

The Mitigation of Primary Blast Injury

Danyal Magnus

Imperial College London

Department of Physics

Thesis submitted for the degree of

Doctor of Philosophy

May 2020

Abstract

While effective against penetrating threats, ballistic armour may not mitigate primary blast injuries inflicted by the pressure wave. It remains poorly understood whether such armour improves blast clinical outcome for the wearer. The aim of this work was to deepen understanding of the nature of modern primary blast injuries, potential materials appropriate for personal blast protection and the influence of hard ballistic plates on expected survivability.

The modern prevalence and severity of blast in the civilian environment was investigated by carrying out a meta-analysis of injured populations. The occurrence of primary blast injuries strongly depends on the ventilation of the attack environment. As the rate of pulmonary injury for Improvised Explosive Device attacks is approximately 9%, it is apparent that the blast attenuating capability of thoracic armour is of importance.

The blast mitigation performance of polyurethane-based foams, hydrogels and a hard ballistic plate were assessed using a shock tube. The foams and hydrogels were manufactured and mechanically characterised across strain rates from $0.01\text{--}1600\text{ s}^{-1}$ covering regimes relevant to both load-bearing and blast loading scenarios. While reticulated polyurethane foam enhanced the peak blast pressure, the hydrogels dissipated blast energy through brittle fracture. A ballistic gelatine human thorax surrogate was used to evaluate the clinical significance of the mitigation, and the blast loading of the gelatine computationally modelled. The hydrogels yielded a 90% reduction of peak pressure compared to the unarmoured case, comparable to values reported for water-based mitigation

systems. By comparison, the addition of a ballistic plate with zero air gap increased lethality from values up to 50% in the unarmoured case to values up to 99%. Under the highest amplitude and duration blast loading, this corresponded to an increase of the delivered peak pressure and impulse to the surrogate of 52% and 27%, respectively, compared to the unarmoured case. Introduction of an air gap between the surrogate and armour further increased the lethality risk to near 99% over the full range of loading conditions due to blunt impact of the plate. However, mitigation could be achieved by combining the ballistic plate with a reticulated foam backing layer greater than a critical thickness.

Acknowledgements

Firstly, I would like to express my sincere gratitude to my supervisors, Dr. William G. Proud and Prof. Mansoor A. Khan, for their invaluable guidance and providing me with the opportunity to pursue this project. I also deeply appreciate the support of Dr. Thuy-Tien N. Nguyen and Dr. David R. Sory throughout the project and, in particular, for training me in many of the scientific techniques required in the early stages of this research. I also thank Dr. Gareth R. Tear and Dr. David J. Chapman for their guidance, useful scientific discussions and encouragement throughout.

The experiments carried out during this research would not have been possible without the support of Mr. Paul Brown and the highly diligent technical staff of the Physics Instrumentation Workshop. I am also grateful to the Royal British Legion Centre for Blast Injury Studies for use of their facilities and for the experimental assistance provided by Dr. Sean Buckland, Dr. Amitay H. Cohen and Mr. James Lee. Much credit must also be given to Dr. Ian G. Cullis for his extremely insightful advice on the computational work.

Finally, I thank my family for being my greatest source of encouragement.

Contents

Abstract	2
Acknowledgements	4
List of Figures	11
List of Tables	37
List of Symbols & Abbreviations	43
1 Introduction	51
1.1 Motivation	51
1.1.1 Overview	51
1.1.2 Improvised Explosive Devices	52
1.1.3 Blast Injury	53
1.2 Objectives	54
1.3 Thesis Outline	54
1.4 Declaration of Originality	56
1.5 Copyright Declaration	56
1.6 Publications	56
2 Background	58

2.1	Shock Waves	58
2.2	Blast Waves	60
2.3	Blast Injury	66
2.3.1	Blast Interaction with the Body	66
2.3.2	Trauma Severity Scales	70
2.3.3	Clinical Observations of Injured Populations	71
2.3.4	Estimates of Blast Lethality to Humans	76
2.4	Blast Mitigation	81
2.4.1	Foams	82
2.4.2	Water	92
2.4.3	Armour	93
2.5	Split-Hopkinson Pressure Bar Fundamentals	99
2.5.1	Operation	99
2.5.2	Data Reduction	102
2.5.3	Experimental Considerations & Sources of Error	105
2.6	Shock Tube Fundamentals	106
2.7	Discussion	110
2.7.1	Review	110
2.7.2	Research Outline	112
3	Epidemiological Study of Blast Injuries Inflicted by Terrorist Bombings in the Civilian Environment	114
3.1	Methodology	115
3.1.1	Definitions	118
3.2	Results	119

3.2.1	Analysis of the Global Terrorism Database	119
3.2.2	Blast Injury Meta-Analysis	125
3.3	Conclusions	132
4	Materials & Preparation	134
4.1	Casting Equipment	134
4.2	Polyurethane Foams	135
4.2.1	Reticulated Foams	135
4.2.2	Syntactic Foams	137
4.3	Hydrogels	141
4.3.1	Agarose Hydrogel	142
4.3.2	Hydrogel Beads & Two-Phase Hydrogel	143
4.4	Ballistic Armour	147
4.5	Gelatine	151
5	Experimental Platforms & Diagnostics	153
5.1	The CBIS Shock Tube System	153
5.1.1	Operation	153
5.1.2	Pressure Diagnostics	157
5.2	Universal Testing Machine	160
5.3	Drop-weight	164
5.3.1	Calibration	167
5.4	The CBIS Split-Hopkinson Pressure Bar Systems	168
5.4.1	Conventional System	168
5.4.2	High-sensitivity System	172

5.5	Digital Image Correlation	175
5.6	Large-scale Shock Tube Rigs	179
5.6.1	Shock Tube External Mount	179
5.6.2	Shock Tube Expansion Adaptor	180
5.7	PVDF Sensors	186
5.7.1	Calibration	187
6	Mechanical Characterisation	189
6.1	Methodology	189
6.2	Stress-strain Calculations	191
6.3	Polyurethane Foams	192
6.4	Hydrogels	200
6.5	Gelatine	205
7	Computational Simulations	213
7.1	Coupled Eulerian Lagrangian Method	214
7.2	Shock Tube Loading	214
7.2.1	Workflow	215
7.2.2	Experimental Validation	219
7.2.3	Mesh Convergence	222
7.3	Blast Loading of Ballistic Gelatine	224
8	Shock Enhancement Studies	228
8.1	Experimental Setups	229
8.1.1	Unmitigated Loading	230
8.2	Polyurethane Foams	231

8.2.1	Reticulated Foam Porosity	231
8.2.2	Reticulated Foam Material Length	235
8.2.3	Dependence on Incident Pressure	237
8.2.4	Dependence on Stand-off Distance	239
8.3	Hydrogels	241
8.3.1	Dependence on Incident Pressure	241
8.3.2	Dependence on Stand-off Distance	246
8.3.3	Comparison to Water Blast Mitigation	247
8.4	Conclusions	248
9	Blast Injury Mitigation Studies	251
9.1	Experimental Setup	252
9.2	Characterisation of Large-scale Blast Loading	253
9.3	Influence of the Pressure Gauges	261
9.4	Ballistic Plate & Hydrogel Blast Injury Mitigation	264
9.4.1	Ballistic Plate Foam Backing Thickness	273
9.5	Conclusions	275
10	Conclusions	277
10.1	Research Summary	277
10.2	Research Impact	280
10.3	Future Work	285
	Bibliography	288
	Appendices	315
Appendix A	Allocation of GTD attack settings	316

Appendix B	Statistical significance of blast injury rates on attack setting .	318
Appendix C	Casting Equipment	320
C.1	Material Characterisation Sample Moulds	320
C.2	Shock Tube Sample Moulds	322
Appendix D	Permissions	323

List of Figures

2.1	As the (one-dimensional) shock front propagates at velocity U_s , it defines the discontinuity between the shocked and unshocked states. The particles in the shocked material move with velocity u_p ($u_p < U_s$). This region is described by the variables pressure (P), internal energy (E) and density (ρ). Note that $\rho = 1/v$ where v is specific volume. Analogous variables are defined for the unshocked material with the subscript ‘0’. Adapted from [30].	58
2.2	Characteristic blast pressure histories for a fixed (Eulerian) point observer in the (a) free environment and the (b) enclosed environment [22].	61
2.3	Different explosive scenarios. (Top) Detonation of the charge on the ground or at a fixed height above the ground result in constructive reinforcement of the blast wave. In the latter case, a Mach stem of increasing height with distance from the detonation centre is formed where the incident and reflected blast waves reinforce each other. (Bottom) Ventilation of the blast environment is an important factor determining the resulting pressure profile. Adapted from [36].	62

2.4	Simulations demonstrating the dependence of blast wavefront geometry on the aspect ratio of the charge's shape, A . The hemispherical and flat charge masses were 14.2 g and 15 g, respectively. In each simulation, the charge was located on the ground in the bottom left corner, and the horizontal and vertical axes extend up to 1 m from the detonation centre. The magnitude of the pressure field is indicated by the scale bars [38].	64
2.5	Characteristic curves expressing the blast pressure-impulse-distance relationship for two Improvised Explosive Devices (IEDs). These curves have been derived from scaling relationships measured by Alonso <i>et al.</i> for hemispherical shaped charges positioned on the ground [40]. The data point labels indicate distances in metres from the charge. Two distance regimes are visible for each curve, corresponding to the near-field and far-field. The pipe bomb and delivery truck bomb have TNT equivalent masses of 2.5 kg and 4500 kg, respectively, defined according to the US Department of Homeland Security in Table 2.1. Note that the recommended safe stand-off distances in Table 2.1 account for all threats from an IED and not only the blast wave (<i>e.g.</i> shrapnel).	66
2.6	Illustration of the 9 body areas in the Abbreviated Injury Scale (AIS) injury scoring system. Posterior view (left): cervical spine, upper back and lower back. Anterior view (right): head, neck, chest, abdomen, arms and legs. This framework for categorising injuries and quantifying severity is widely used in clinical research. Reproduced from [50].	70
2.7	Bowen curves describing lethality for a 70 kg human in the open-field scenario. A unique curve exists for each survival probability, but 1%, 50% and 99% curves are cited conventionally. These curves are referenced as LD_1 , LD_{50} and LD_{99} curves, respectively [53]. The threshold lung damage curve was estimated as $1/5P$ of the LD_{50} curve. Reproduced from [63]. . .	78

2.8	Scaled impulse PI_{50} curve as a function of scaled time t_0 derived by Richmond <i>et al.</i> The plot is populated by canine and hircine data. Reproduced from [69].	81
2.9	Characteristic stress-strain curves for (A) elastomeric, (B) elasto-plastic and (C) elastic-brittle foams with plateau stresses of σ_{el}^* , σ_{pl}^* and σ_{cr}^* , respectively. E^* is the elastic modulus of the foam in its linearly elastic regime, valid for small deformation. Adapted from [75].	84
2.10	Phenomenological model of an ideal foam with plateau stress $\sigma_{plat}^* = \sigma_{el}^*, \sigma_{pl}^*, \sigma_{cr}^*$ (depending on the material type), and plateau region beginning at strain ϵ_0 and ending at the densification strain ϵ_D . The energy absorbed by the foam per unit volume up to the densification strain is denoted W	85
2.11	Syntactic foams are multiphase structures consisting of a matrix material and porosity in the form of hollow inclusions. Additional matrix porosity can also be present. Adapted from [77].	86
2.12	Schematic of a typical shock tube experiment characterising foam response to blast (M_i is the incident shock Mach number). The shock tube is used in a closed ('confined') configuration with x , y and z denoting the pressure sensor postions. Characteristic pressure histories recorded by these sensors are shown in Figure 2.13(b). Adapted from [93].	89
2.13	Profiles of wave position and pressure for the Figure 2.12 shock tube experiment. Sensor positions x , y and z correspond to those shown in Figure 2.12. Adapted from [93].	90
2.14	ζ as a function of the FSI parameter, ψ . ζ is the ratio of the transmitted impulse to that for a perfectly rigid structure, taking values from 0 to 1.	96

2.15	Schematic of the blast mitigation setup used by Wood <i>et al.</i> Peak incident blast overpressure (BOP) ranged from 210–3140 kPa with positive-phase loading durations in the range of 0.5–2.0 ms. The sample vest was attached to a non-deforming steel pipe and the reflected pressure measured behind the vest compared to the loading incident pressure from the shock tube output. Reproduced from [28].	97
2.16	Schematic of a Split-Hopkinson Pressure Bar (SHPB). IB: Input Bar, OB: Output Bar.	99
2.17	Split-Hopkinson Pressure Bar (SHPB) $x - t$ diagram illustrating the stress wave propagation in the system. Wave leading and trailing edges are shown in blue and red, respectively. Time is zeroed at the impact time of the striker with the strain gauge stations at the input and output bar mid-points. The loading duration is computed as $2L/C_{st}$, where L is the striker length and C_{st} is the longitudinal sound speed of the striker material. The striker impact introduces an incident strain signal (ε_I) that is partially reflected at the sample interface (ε_R) and partly transmitted into the output bar (ε_T). The finite thickness of the sample and the associated stress wave reverberations are not displayed. Adapted from [121].	101
2.18	Split-Hopkinson Pressure Bar (SHPB) example strain gauge signals for a 304L stainless steel sample and maraging steel bars. Reproduced from [120].	101
2.19	Parameters and coordinate system for modern Split-Hopkinson Pressure Bar (SHPB) analysis using one-dimensional elastic wave theory. u_1 : Input Bar displacement, u_2 : Output Bar displacement, L_S : Sample initial length, ε_I : Incident wave strain, ε_R : Reflected wave strain, ε_T : Transmitted wave strain. Adapted from [124].	103

2.20	Profiles of idealised shock tube behaviour. The shock is represented in red. (a) The configuration of the shock tube. (b) The pressure profile of the shock tube prior to rupture of the diaphragm (positioned at $x = 0$). (c) Formation of shock, contact surface (interface between driver and driven gas) and rarefaction fan. Region (1) gas (at P_1 and T_1) is at rest ahead of the shock front. Region (2) represents the gas behind the shock (at P_2 and T_2) moving at constant velocity. Region (3) gas (at P_3 and T_3) lies between the contact surface and rarefaction fan tail, while Region (4) is the gas (at P_4 and T_4) at rest ahead of the rarefaction fan head. (d) Pressure profile with position. The rarefaction fan acts to smoothly transition the initial driver pressure P_4 to the shock pressure. The pressure of the fully rarefied gas is P_3 which is equal to the driven gas pressure P_2 at the contact surface. (e) Temperature profile with position. There is a discontinuity at the contact surface where the expanded and cooled driver gas interfaces with the compressed and heated driver gas. (f) Gas velocity profile with position. (g) $x-t$ diagram showing the movement of the shock, contact surface and rarefaction waves (dashed). Region (5) represents the fact that a reflected shock is generated if the shock tube is used in its closed configuration. Adapted from [127].	108
3.1	Metrics of terrorist activity and its relative impact over the period 1970 to 2016.	120
3.2	Regional distribution of fatalities due to bombings.	121
3.3	Heat maps depicting the geographical distribution of incidents and Mass Casualty Incidents (MCIs) in the time periods of 1970-1999 and 2000-2016. A logarithmic scale has been used in each case to reflect the wide range of occurrences across different countries.	122

3.4	Distribution of terrorist activity across attack settings. MCI: Mass Casualty Incident.	123
3.5	Distribution of terrorist activity across attack settings.	124
3.6	Preferred Reporting Items for Systematic Reviews (PRISMA) flow diagram for systematic review of epidemiological data.	125
3.7	Schematic illustrating stratification of the total number of casualties. N : Total casualties across all studies, N_k : Number of casualties reported in k^{th} study, n_k : Sample size of casualty population N_k in k^{th} study.	128
4.1	(a): 40 pores per inch (ppi) Acoustaf foam reticulated foam used in this research [153]. (b): Scanning electron micrograph of 40 ppi polyurethane open-cell foam (Hitachi S-3500-N, 10 kV accelerating voltage) exhibiting the high void fraction relative to the skeletal mass [154].	136
4.2	Illustration of the syntactic foam with a glass microballoon shown embedded in the polyurethane matrix. The single particle is shown with mean microballoon radius R_{mb} and mean microballoon wall thickness δr . This diagram provides a geometric basis for the air void fraction of the microballoon to be estimated. This allows the necessary mass of microballoon additive to achieve a target syntactic foam porosity to be calculated as shown in Eq. (4.1).	138
4.3	(a) Optical micrography and (b) circle finding analysis to determine size distribution of microballoons.	139
4.4	(a) Optical micrographs of bulk polyurethane Shore hardness 30 A (PU30) and (b) with the addition of microballoons. $\bar{d}_{mb}$: Mean microballoon diameter in image.	140

4.5	Preparation of 1.0% (w/v) agarose beads and two-phase hydrogel. A small difference in refractive index between the beads and bulk hydrogel allows the suspension to be visible prior to gelation.	145
4.6	Effect of surfactant concentration on mean agarose bead particle size. The diameters were measured from optical microscopy of approximately 500 particles for each surfactant concentration. In (a), normal number density functions were computed to represent the spread of particle diameters. The mean diameters for each surfactant concentration are shown in (b). The uncertainties were computed as the standard error of the mean for each surfactant concentration. Note that the standard deviation for each data point is approximately equivalent to the half width of the number density function peak in (a) at 60% of the full height.	146
4.7	Particle size measurements of the agarose beads investigated in this work, prepared with a Tween 80 surfactant concentration of 0.075% (w/w). . .	147
4.8	Mark 4a OSPREY armor system (A) carrier made of Cordura® 1000D nylon fabric. This system includes (B) contoured hard front and rear ballistic plates, (C) side ballistic plates and (D) filler soft armour designed to prevent fragmentation impact injuries. Reproduced from [14].	149
4.9	Front (a) and rear (b) OSPREY plates superimposed on anterior and posterior views of areas defined as required essential medical coverage (heart, great vessels, liver and spleen) . Note that the exposed lower region of the aorta would be covered by the soft armour component of the OSPREY system. Reproduced from [14].	149

4.10	Studies of ballistic armour protection. A: computational surface wound location analysis of UK and North Atlantic Treaty Organisation (NATO) casualties in Afghanistan (2012), reproduced from [162]. B: ballistic testing mount of soft and hard armour plates, reproduced from [165].	150
4.11	Monolithic composite Dyneema [®] ballistic plate investigated in this work under blast loading. The plate complies with National Institute of Justice (NIJ) Standard 0101.06. The plate's dimensions were $200 \times 300 \times 20$ mm with a total mass of 1.35 kg.	151
5.1	Schematic of the Centre for Blast Injury Studies (CBIS) shock tube in an open configuration. The blue lines indicate two possible diaphragm positions. The system can also be closed with a head-on sensor mounted in the end flange to measure the reflected pressure wave.	154
5.2	(a) Calibration of Mylar [®] thickness to burst pressure averaged over 5 repeat shots. The errors were computed as the standard error of the mean. (b) Sensor 1 pressure profiles measured 1.22 m from the diaphragm position for varying diaphragm thickness.	155

5.3	Variation of incident wave loading duration by varying the shock tube driver volume. A fixed diaphragm burst pressure of 12 bar was used. In the laboratory, the volume was adjusted by adding polypropylene inserts to the driver section. The driver volume is expressed as a percentage of the maximum volume using the full driver length of 1.22 m (see Figure 5.1). The minimum 32% driver volume corresponds to using the shock tube double breech only as the driver and sealing off the rightmost driver tube, termed ‘blanking volume’. In (a), it can be seen that the positive phase blast duration increases linearly with the driver volume. The values were computed as the average over 5 repeat shots with the errors computed as the standard error of the mean. In (b), the measured Sensor 1 pressure profiles 1.22 m from the diaphragm position are shown.	156
5.4	Signal processing of shock tube pressure diagnostic data. The filter acts to strongly suppress the significance of frequencies above the 70 kHz low-pass cut-off.	158
5.5	Fitting of the Friedlander waveform to the positive phase of a pressure profile from a 10 bar burst pressure and 9 cm driver length open shock tube shot. High frequency noise can be seen in the experimental data due to wave propagation in the shock tube walls and noise introduced by lateral oscillations of the piezoelectric pressure diagnostic. To improve consistency of the calculated peak pressure between shots, this was computed from the value of the fit at the start of the blast wave ($t = 0$ s in this example). . .	159
5.6	Schematic of the electromechanical Instron® 5566 UTM with a self-aligning rig. In compression testing, the crosshead is actuated downwards such that the sample is loaded between the stainless steel platens.	161

5.7	Universal Testing Machine (UTM) compliance with platens only. Errors reported as the standard error of the mean for 5 separate compressions. The uncertainty is reduced for the 10 kN load cell due to the greater magnitude of the load.	162
5.8	Universal Testing Machine (UTM) compliance with self-alignment rig installed. Errors reported as the standard error of the mean for 5 separate compressions. The uncertainty is reduced for the 10 kN load cell due to the greater magnitude of the load.	163
5.9	Schematic of the drop-weight system. mg : Weight of impactor, where m is the impactor mass and g the acceleration due to gravity.	165
5.10	Drop-weight axial loading conditions for varying drop height of a 150 g mass impactor. In (b), each data point represents an average of 5 measurements, with the error computed as the standard error of the mean. This error reduced with increasing drop height due to reducing solid friction with free-fall velocity.	167
5.11	Schematic of the Aluminium 7068 alloy Centre for Blast Injury Studies (CBIS) Split-Hopkinson Pressure Bar (SHPB) system. The sample was held by friction between the Input Bar (IB) and Output Bar (OB). Pre-strain of the sample would lead to systematic underestimation of the sample strain which was mitigated by minimising visible barrelling of the sample.	169

5.12	Data acquired to calibrate the aluminium 7068 alloy Split-Hopkinson Pressure Bar (SHPB) system. (a): Identification of average plateau voltage $\bar{V}$ from the strain gauges for the Input Bar (IB) and Output Bar (OB). (b): Example light-gate acquisition. (c): Calibration of impact velocity to firing pressure. The uncertainty is given by the standard error of the mean for 5 repeat shots at each firing pressure. A reduction of uncertainty at faster impact velocity can be observed due to reduced friction with the gas gun bore. (d): Calibration of strain gauge average plateau voltage to the average corresponding force, $\bar{F}$. Again, the uncertainties are computed as the standard error of the mean over the 5 repeat shots.	171
5.13	Schematic of the quartz-crystal embedded high-sensitivity Split-Hopkinson Pressure (SHPB). IB: Input Bar, OB: Output Bar.	173
5.14	Configuration of Input Bar (IB) and Output Bar (OB) ends for the modified Split-Hopkinson Pressure Bar (SHPB) using quartz crystal embedded aluminium bars. m_s and m_p represent a seismic mass and the platen mass, respectively. The bar, seismic mass and platen are all constructed from aluminium 7068 alloy with the components bonded with conductive epoxy adhesive. Four quartz crystals (grey regions) are utilised. Under mechanical loading, each crystal individually experiences stress and generates a current proportional to rate of change of the applied force. The polarity of transducer 4 is chosen such that its current is subtracted from that of the crystals 1-3. As such, the combination of transducer 4 and the seismic mass act to compensate for the additional unwanted inertia introduced by the stack of crystals 1-3. Adapted from [175].	174

5.15	Digital Image Correlation (DIC) analysis of drop-weight and Split-Hopkinson Pressure Bar (SHPB) high-speed videos using Ncorr [180]. The random speckle patterns utilised features with a minimum size of 5 pixels. In the Ncorr Graphical User Interface (GUI) visible in (b) and (d), Regions of Interest (ROIs) were defined and the subsequent Eulerian displacement (displacement with respect to the start position) of these regions were tracked. Displacements are shown in the direction of the loading axis, vertical (positive downwards) for the drop-weight and horizontal (positive rightwards) for the SHPB. In (d), the displacement is negative as the Input Bar (IB) moves towards the left. The Output Bar (OB) can be observed to remain relatively stationary compared to the IB.	177
5.16	Computation of axial displacement histories for the (a) drop-weight and (b) modified high-sensitivity Split-Hopkinson Pressure Bar (SHPB) using Digital Image Correlation (DIC). In (a), the drop-weight impactor displacement is linear with time up to approximately $600\mu\text{s}$, implying constant sample strain rate. Following this, the impactor displacement is non-linear due to deceleration by the sample. Each data point in (a) and (b) represents a video frame.	178
5.17	Shock tube external mount rig for large-scale shock tube blast mitigation experiments (units in mm).	180
5.18	Illustration of the formation of an expansion fan when a supersonic flow encounters a sharp convex corner at a turn angle of θ . For clarity, the fan is shown for one point in two-dimensions. The maximum angle the flow can turn through is described by the Prandtl-Meyer function as a function of Mach number, M . M_1 : Mach number before turn, M_2 : Mach number after turn, μ_1 : Angle of head of expansion fan, μ_2 : Angle of tail of expansion fan, $\nu(M)$: Prandtl-Meyer function.	181

5.19	Geometrical derivation of the constraints on the angle of the expansion fan head, μ_1 , for no reflection (μ_1^{NR}) or self-interaction (μ_1^{NI}) of the expansion fans produced by the supersonic flow. In (a), it is ensured that the wave does not reflect from the cone wall before exiting the expansion while, in (b), it is ensured that the expansion fans do not interact across the centreline. θ : Adaptor turn angle.	183
5.20	Design spaces for expansion adaptor to enable appropriate selection of the turn angle θ for the CBIS shock tube in terms of (a) leading (μ_1) and trailing (μ_2) edge angle, and (b) input (M_1) and output (M_2) Mach number. The surface of (a) is transformed to (b) by applying Eq. (5.8). Superscripts NR and NI denote non-reflection and non-interaction constraints, respectively. θ_{max} : Maximum supersonic flow turn angle.	184
5.21	Measurement of outlet peak pressure (a) and calculation of corresponding Mach number (b) from application of the Eq. (2.35) Rankine-Hugoniot relation. The outlet pressure and blast Mach number followed a linear dependence on the diaphragm burst pressure. Each data point represents 5 repeated shots with the uncertainty given as the standard error of the mean.	185
5.22	Shock tube expansion adaptor for large-scale shock tube blast mitigation experiments (units in mm).	186
5.23	Piezotech S2501 Polyvinylidene Fluoride (PVDF) sensors used for gelatine pressure measurements in shock tube blast injury mitigation experiments.	187
5.24	Calibration data for the Piezotech S2501 Polyvinylidene Fluoride (PVDF) sensors. Two sensors were calibrated (Sensor 1 and Sensor 2). In (a), the data represents the mean of 6 repeated drop-weight shots with the uncertainties computed as the standard error of the mean.	188

6.1	Conservation of sample volume under compression (positive Poisson's ratio).	191
6.2	Stages of Split-Hopkinson Pressure Bar (SHPB) data reduction for a bulk Shore hardness 30 A polyurethane (PU30) sample at a strain rate of 1600 s^{-1} . In (d), the sample true stress-strain relations are shown under the assumption of conserved sample volume. Computed 1-wave, 2-wave and 3-wave stresses correspond to calculation of the sample stress from the transmitted signal only, incident and reflected signal, and finally the incident, reflected and transmitted signals, respectively (see Section 2.5.2). IB: Input Bar, OB: Output Bar, σ_I : Incident stress signal, σ_R : Reflected stress signal, σ_T : Transmitted stress signal.	192
6.3	Quasi-static and dynamic characterisation of polyurethane reticulated and syntactic cellular materials. ppi: Pores per inch, SHPB: Split-Hopkinson Pressure Bar, PU30: Shore hardness 30 A polyurethane, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.	194
6.4	Repeated quasi-static loading of $\rho^*/\rho_s = 6\%$ relative density syntactic foam. The material undergoes brittle crushing under the first compression. ρ^* : Foam bulk density, ρ_s : Foam skeletal density.	197
6.5	Semi-log plots of polyurethane foam plateau pressure (a) and energy absorption (b) strain rate sensitivity. Each data point represent the mean of 3 independent measurements, with the error computed as the standard error of the mean. UTM: Universal Testing Machine, SHPB: Split-Hopkinson Pressure Bar, ppi: Pores per inch, PU30: Shore hardness 30 A polyurethane, σ_{el}^* : reticulated foam plateau stress, σ_{cr}^* : syntactic foam plateau stress, W : foam energy absorbed per unit volume up to densification, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.	198

6.6	High-sensitivity Split-Hopkinson Pressure Bar (SHPB) Input Bar (IB) and Output Bar (OB) quartz-crystal voltage signals for 1.5% (w/v) agarose hydrogel without (a) and with (b) sample coring. Coring reduces initial radial inertia and reduces the time to force equilibrium.	200
6.7	Quasi-static and dynamic characterisation of homogeneous and two-phase agarose hydrogels. The curves are plotted up to the material's failure strain. SHPB: Split-Hopkinson Pressure Bar.	202
6.8	Log-log plot of hydrogel failure stresses across quasi-static and dynamic strain rates. Filled and unfilled markers denote homogeneous and two-phase materials, respectively. Failure stresses were calculated from the mean of 3 repeat measurements of the stress-strain curve for the materials, with the uncertainty computed as the standard error of the mean. Within these error bounds, the distinction between the homogeneous and two-phase materials for a given matrix concentration remained significant. Due to the logarithmic vertical axis, these error bounds are not displayed here for clarity.	204
6.9	Quasi-static and dynamic characterisation of Type A 300 Bloom North Atlantic Treaty Organisation (NATO) specification 20% (w/w) ballistic gelatine. Each point in the elastic modulus strain rate sensitivity (d) represents the average of 3 independent measurements with the uncertainty computed as the standard error of the mean. SHPB: Split-Hopkinson Pressure Bar.	206
6.10	Human tissue length-scale and elastic property regimes. Reproduced from [195].	208

6.11	Least-squares fitting of Neo-Hookean, Ogden and Mooney-Rivlin hyperelastic constitutive models to gelatine stress-strain experimental data across quasi-static and dynamic strain rates. SHPB: Split-Hopkinson Pressure Bar.	211
7.1	Shock tube initial driver and driven section pressure conditions for a blanking volume 2 bar burst pressure (Mylar [®] 23 μ m) shot. Scale bar units in Pa. SVAVG: Volume-averaged stress components.	218
7.2	Regions for normal end-wall (a) and transverse (b) wall boundary conditions.	218
7.3	Pressure distribution in the simulated closed shock tube 1.8 ms after bursting of the diaphragm for a blanking volume 2 bar burst pressure (Mylar [®] 23 μ m) shot. Scale bar and time units in Pa and seconds, respectively. SVAVG: Volume-averaged stress components.	220
7.4	Comparison of measured and numerical pressure histories for the open and closed shock tube configurations for blanking volume 2 bar burst pressure (Mylar [®] 23 μ m) shots. A 10 mm Eulerian mesh element size has been used.	221
7.5	Variation of Eulerian mesh element sizes from coarse to fine.	222
7.6	Eulerian mesh convergence sensitivity for pressure profiles, peak pressure, rise time and resulting simulation run time.	223

7.7	Schematic of the modelling setup for an Abaqus Coupled Eulerian Lagrangian (CEL) simulation of ballistic gelatine loaded by a shock tube in a closed configuration. A driver burst pressure of 7.2 bar was used (Mylar [®] 100 μm diaphragm). As discussed in Section 7.2, zero transverse velocity, v , (magenta) boundary conditions were applied to the Eulerian region along the shock tube walls. For the Lagrangian domain, zero displacement (u) boundary conditions were defined to confine the gelatine laterally (green) and axially (red) to replicate the containment of the material in the shock tube.	224
7.8	Comparison of measured and numerical pressure histories with gelatine sample in a closed shock tube. Sensor 1 measured the incident wave and Sensor 2 was positioned head-on behind the sample. In (b), both purely hyperelastic (Ogden) and visco-hyperelastic (Ogden and Prony) constitutive models are shown.	225
7.9	Gelatine stress wave propagation under shock tube blast loading. Scale bar and time units in Pa and seconds, respectively.	226
8.1	Confined (a) and unconfined (b) shock tube setups used for shock enhancement investigations. The stand-off between the mitigation material and rear boundary is denoted d . Sensor 1 (farther downstream as shown in Figure 5.1) measured the incident wave profile while Sensor 2 measured the reflected profile at the rigid end-wall. Sample diameters were equal to the shock tube diameter of 60 mm. This setup is analogous to that used by Seitz & Skews illustrated in Figure 2.12.	229
8.2	Unmitigated baseline pressure profiles for the (a) confined and (b) unconfined setups (Mylar [®] 75 μm diaphragm).	230

- 8.3 The effect of reticulated foam porosity on peak pressure and impulse mitigation for both covered (a) and uncovered (b) blastward faces when loaded using a Mylar[®] 75 μm diaphragm ($P_s = 3.6 \pm 0.2 \text{ bar}$, $I_s = 3.2 \pm 0.3 \text{ bar ms}$). The foam sample length is 20 mm and there is zero stand-off between the sample and closed shock tube end wall. The pressure profiles show the reflected pressure behind the samples measured by Sensor 2. Second-order polynomials were fitted by least-squares regression to the peak pressure attenuation plot (c). The uncertainties represent the standard error of the mean over 3 repeat independent measurements. ppi: Pores per inch, P_s : Blast peak incident overpressure pressure, I_s : Blast positive phase impulse, τ_P : Peak pressure mitigation ratio, τ_I : Impulse mitigation ratio. 232
- 8.4 Schlieren shadowography of the reflected shock wave from a reticulated foam under shock tube loading at 100 μs , 200 μs and 300 μs (from left to right) after reflection from the front face of the foam. The foam is visible as the dark region on the right side of each frame. Reproduced from [87]. 234

- 8.5 The effect of initial reticulated foam length on end-wall pressure amplification for foam porosities in the range of 20-80 pores per inch (ppi). The materials were loaded using a Mylar[®] 75 μm diaphragm (peak incident overpressure $P_s = 3.6 \pm 0.2$ bar). In (b), smoothing spline curves were fitted. It can be observed that the peak pressure mitigation ratios approached a plateau, becoming less dependent on the initial foam length. The rate at which the materials approached this mitigation independence from the initial foam length decreased with increasing foam ppi. For example, the 20 ppi foam peak pressure mitigation plateaued above an initial foam length of 80 mm within errors. Conversely, the 80 ppi foam did not reach this plateau over the investigated range, though a decreasing dependence on the initial foam length can be observed. This behaviour can be related to the stress plateau characteristic of foams under compression. The uncertainties represent the standard error of the mean over 3 repeat independent measurements. 235
- 8.6 The effect of incident loading pressure amplitude on peak pressure (τ_P) and impulse (τ_I) mitigation ratios for reticulated and syntactic polyurethane foam materials. These mitigation ratios express the ratio of the parameter (peak pressure or impulse) with the mitigation material to without the mitigation material. The uncertainties represent the standard error of the mean over 3 repeat independent measurements. ppi: Pores per inch, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density. . 237
- 8.7 Comparison of end-wall pressure histories for 20 pores per inch (ppi) reticulated foam positioned at stand-off distance d from the shock tube end-wall, demonstrating the contribution of elastic inertia. The samples were loaded using a Mylar[®] 75 μm diaphragm (peak incident overpressure $P_s = 3.6 \pm 0.2$ bar). For visual comparison, the starts of the signals have been shifted to the same time. 239

- 8.8 The effect of stand-off distance, d , from the shock tube end-wall for the polyurethane syntactic foams. The pressure profiles in (a) and the peak pressure mitigation in (b) are shown for the example of a Mylar[®] 75 μm diaphragm (peak incident overpressure $P_s = 3.6 \pm 0.2$ bar). The uncertainties represent the standard error of the mean over 3 repeat independent measurements. For visual comparison, the beginnings of the signals have been shifted to the same time. τ_P : Peak pressure mitigation ratio, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density. 240
- 8.9 Homogeneous (a) and two-phase (b) hydrogel end-wall pressure profiles for the confined shock tube setup. The materials were loaded using a Mylar[®] 75 μm diaphragm (peak incident overpressure $P_s = 3.6 \pm 0.2$ bar). 241
- 8.10 The effect of incident loading pressure amplitude on peak pressure (τ_I) and impulse (τ_I) mitigation ratios for homogeneous and two-phase hydrogels. The uncertainties represent the standard error of the mean over 3 repeat independent measurements. The dependence of the mitigation ratios on incident peak pressure was well-described by linear fits where the samples underwent fracture. The exception to this was the case of the 2.5% (w/v) homogeneous hydrogel which remained in its elastic regime and did not fragment. Visual evidence of this is shown in Figure 8.14. As such, this data point was excluded from the linear fits as the deformation mechanisms were fundamentally different to those under higher amplitude blast loading where the material failed. 243
- 8.11 Recovered 2.5% (w/v) homogeneous hydrogel following shock tube loading at varying incident peak pressure. From left to right, the incident peak pressures were 0.9 bar, 2.1 bar, 3.0 bar and 3.8 bar. The leftmost sample was undamaged and remained in its elastic regime, resulting in shock enhancement. 244

8.12	Unconfined shock tube loading of 2.0% (w/v) hydrogel under 1.8 bar incident peak pressure loading. The shock tube end-wall is visible on the right edge. The dark material (left) is the Velcro [®] releasing from the sample. Lateral material loss is permitted in the setup compared to the conventional confined shock tube shock enhancement configuration	245
8.13	The effect of incident loading pressure amplitude on peak pressure and impulse mitigation ratios for two-phase hydrogels under unconfined loading conditions. The uncertainties represent the standard error of the mean over 3 repeat independent measurements. τ_P : Peak pressure mitigation ratio, τ_I : Impulse mitigation ratio.	245
8.14	Unconfined impact of 2.5% (w/v) two-phase hydrogel against rear end-wall at an initial stand-off distance of 30 mm, loaded by a 2.1 bar peak pressure blast wave. The blast wave propagates from left to right with the (a) unloaded sample, (b) sample contact with the blast wave and Velcro [®] release, (c) free-flight of the sample towards the end-wall, and (d) initial impact with the end-wall.	246
8.15	The effect of stand-off distance, d , from the rigid rear boundary for two-phase hydrogels in an unconfined configuration. The samples were loaded with a Mylar [®] 75 μm diaphragm ($P_s = 3.0 \pm 0.2$ bar, $I_s = 3.2 \pm 0.3$ bar ms). The uncertainties represent the standard error of the mean over 3 repeat independent measurements. P_s : Blast peak incident overpressure, I_s : Blast incident positive phase impulse, τ_P : Peak pressure mitigation ratio, τ_I : Impulse mitigation ratio.	247

- 9.1 Shock tube and armour-surrogate experimental setup (not to scale). External mount (Figure 5.17) and high-speed camera not shown for clarity. The bull-nose mount for Sensor 2 utilises a round front surface to minimise its interaction with the blast wave. It was secured with three M6 bolts to an optical table which is a rigid surface with a rectangular (25 mm spaced) grid of tapped M6 holes). The gelatine was instrumented with the surface Polyvinylidene Fluoride (PVDF) sensor and internal piezoelectric Sensor 3. 252
- 9.2 Setup (a) and simulation (b) for shock tube flow through an expansion region of diameter w . Scale bar and time units in Pa and seconds, respectively. SVAVG: Volume-averaged stress components. 254
- 9.3 Comparison of experimental and numerical pressure histories measured centrally in the expanded region. w : Diameter of expansion region (60 mm corresponds to no expansion). 255
- 9.4 Simulation of expansion adaptor pressure output. Scale bar units in Pa. M_1 : Incident flow Mach number, SVAVG: Volume-averaged stress components. 256
- 9.5 Simulation of lateral pressure distribution at a 20 cm stand-off distance from the expansion adaptor outlet. This approximates the loading that the large-scale samples would be exposed to. Note that gauge pressure is shown here. In (a), the red box overlay shows the dimensions of the target mitigation materials. In (b), the pressure profiles measured at the 5 sensor locations are shown. It can be seen that the pressure distribution is non-uniform, with a maximal pressure in the centre. This is emphasised in the (c) horizontal pressure line-out where the pressure drops rapidly with radial distance from the centre to below atmospheric pressure. Scale bar units in Pa. SVAVG: Volume-averaged stress components. 258

- 9.6 Comparison of shock tube blast output with and without the expansion adaptor (full driver volume). In (c), the unfilled and dashed data represents the standard shock tube setup and the filled and solid data represents the output with the adaptor. Additional data points are present for the non-adaptor data due to the restricted working range of the adaptor. As for the shock tube peak pressure measured at the mid-point of the driven section (see Figure 5.2(a)), the peak pressure measured at the muzzle without the adaptor was also found to scale linearly with the diaphragm burst pressure. With the addition of the adaptor, higher peak pressure and duration was achieved due to the reduction of gas losses to the surroundings. The adaptor peak pressure converged to that of the system without the adaptor with increasing burst pressure due to the flow Mach number approaching that where there was no longer interaction with the adaptor. This relationship with the burst pressure was well-described by a second order polynomial. The blast positive phase duration linear fits highlight that this parameter is manipulated only by changes to the driver volume and is approximately independent of burst pressure. 260
- 9.7 Comparison of computational and experimental gelatine pressure diagnostics. 263
- 9.8 High-speed photography of shock tube loading of mitigation materials. The blast is right-propagating with the gelatine surrogate visible as the right-hand region. All frames are at the same time 10 ms after triggering of the camera from Sensor 1. 265
- 9.9 Time sequence of ballistic plate impacting the gelatine surrogate from a 30 mm stand-off, corresponding to Figure 9.8(b). The plate was observed to accelerate towards the surrogate (left), impact the gelatine (center) and finally rebound (right) towards the left, losing contact with the gelatine. . 266

- 9.10 Comparison of blast performance of the mitigation materials to published lethality and pulmonary injury risk curves. The standard 1% (LD_1) and 50% (LD_{50}) survival curves are shown in black, with the Table 2.3 injury criteria shown in red. The scaled duration t_+ represents scaling of the iso-injury curves to the case of a 70 kg mass human target. The data was obtained by scanning through increasing peak incident pressure (by increasing the driver pressure) and increasing loading duration (by increasing the driver volume). The data points are computed as the mean of two repeats. LD_1 : Iso-injury curve for 1% survival probability, LD_{50} : Iso-injury curve for 50% survival probability, CI_{50} : Iso-injury curve for 50% probability of becoming Combat Ineffective (CI). 267
- 9.11 Comparison of blast performance of the mitigation materials to published pressure impulse injury criteria. The 50% (PI_{50}) survival line is shown in black, with the Table 2.3 injury criteria shown in red. The scaled duration t_+ represents scaling of the injury lines to the case of a 70 kg mass human target. The curves are obtained by scanning through increasing peak incident impulse (by increasing the driver pressure) and increasing loading duration (by increasing the driver volume). The data points are computed as the mean of two repeats. PI_{50} : Impulse iso-injury curve for 50% survival probability. 269
- 9.12 Dependence of the mitigation of peak pressure and impulse at the midsection of the gelatine surrogate on incident blast pressure. These measurements were for a loading duration of 0.24 ms. τ_P : Peak pressure mitigation ratio. τ_I : Impulse mitigation ratio. 270

9.13	Abaqus Coupled Eulerian Lagrangian (CEL) simulation of ballistic plate blast loading with and without entrapment of the air occupying the stand-off region between the surrogate and rear surface of the plate. In (a) and (b), absolute pressure of the Eulerian air region is shown in units of Pa. In (c) and (d), the pressure field for the gelatine Lagrangian region is shown in units of Pa. Time steps are in units of seconds. A peak incident pressure of 1.28 bar and loading duration of 0.24 ms was used. SVAVG: Volume-averaged stress components.	272
9.14	Variation of the gelatine surface peak reflected pressure with ballistic plate foam backing layer thickness. The foam was 20 pores per inch (ppi) reticulated polyurethane foam with an incident peak pressure of 0.85 bar and loading duration of 0.24 ms. T_{min} : Analytically calculated minimum foam thickness for densification.	274
C.1	CAD drawing of mould back plate used for manufacturing samples for material characterisation. This was bolted to the front plate in Figure C.2 with M4 bolts. Parts were made of acetal for ease of demoulding polyurethane and hydrogel materials. All units in mm.	320
C.2	CAD drawing of mould front plate used for manufacturing samples for material characterisation. Two moulds, 1.6 mm and 2.0 mm thick, were made to manufacture samples appropriate for both drop-weight and SHPB testing. This part was bolted to the back plate in Figure C.1 with M4 bolts. Parts were made of acetal for ease of demoulding polyurethane and hydrogel materials. Following vacuum degassing, polycarbonate covers were place on the top of the assembly to seal the material during curing. All units in mm.	321

- C.3 CAD drawing of mould front plate used for manufacturing samples for shock tube shock enhancement experiments. This part was bolted to a back plate plate analogous to Figure C.1 and a polycarbonate front plate with M10 bolts, sealing the assembly for sample curing. All units in mm. 322

List of Tables

2.1	Example Improvised Explosive Device (IED) TNT equivalences and recommended safe stand-off distances defined by the US Department of Homeland Security. The TNT equivalent mass denotes the mass of TNT required to yield the same energy release as the IED. The indoor distance assumes that buildings provide a degree of protection to the occupants and is calculated based on the threshold to avoid building collapse. The outdoor safe stand-off distance is farther due to the shrapnel throw distance. This distance is limited by the need to avoid fragment impact injuries from shrapnel or glass breakage as this is considered a greater risk than overpressure at large distances due to rapid attenuation of the blast wave. Adapted from [39]	65
2.2	Outcomes for military populations of size n sustaining blast loading at varying incident peak overpressure (BOP). TMP: Tympanic Membrane Perforation, BLI: Blast Lung Injury. Adapted from [55].	74
2.3	Summary of common casualty criteria for Blast Lung Injury (BLI). LD_{50} : 50% survival probability pressure-time iso-injury curve, PI_{50} : 50% survival probability impulse-time iso-injury curve, t_+ : scaled time for animal mass and atmospheric pressure, t_{crit} : characteristic time for oscillation of the human thorax, LD_{99} : 99% survival probability pressure-time iso-injury curve, CI_{50} : 50% probability of becoming Combat Ineffective (CI).	82

3.1	Global Terrorism Database (GTD) search parameters for the epidemiological study. The terminology is defined in accordance with the syntax outlined by the GTD codebook [133]. ‘Successful’ attacks were those where the Improvised Explosive Device (IED) functioned as intended by the perpetrator(s).	116
3.2	Classification of ten blast injuries for systematic review of epidemiological data. BLI: Blast Lung Injury, TMP: Tympanic Membrane Perforation. .	117
3.3	Attack settings defined for the epidemiological study. The examples are as defined by the Global Terrorism Database (GTD) codebook [133].	118
3.4	Studies included in the epidemiological meta-analysis. The study period refers to the time during which the incident(s) took place, given as a range for the studies that examined events over multiple years. SCS: Semi-confined Space, OS: Open Space.	127
3.5	Number of casualties presenting each injury out of the total number of casualties studied in each attack setting. These numbers are computed as a percentage in Table 3.6. For each setting, the total casualties studied can vary due to some studies focussing on certain injuries. For example, in the train setting, one of the two studies comprising the data considered only TMP and traumatic amputation, resulting in a larger total casualty population for these injuries compared to other injuries. BLI: Blast Lung Injury, TMP: Tympanic Membrane Perforation, SCS: Semi-confined Space, OS: Open Space.	127

3.6	Proportions of casualties presenting different blast injuries by attack setting. These values provide the expected percentage of casualties with each injury according to attack setting. The percentage is computed from the ratio of the number of casualties reported to have the injury to the total number of casualties in the attack setting (shown in Table 3.5). Where this total number of casualties was higher for a given attack setting and injury, the confidence in the calculated proportion was higher, resulting in a narrower Confidence Interval (CI). As this total number of casualties was contributed to by multiple studies for a given setting, this confidence interval was computed as the weighted average. For example, for the train TMP data, two studies contributed total casualty populations of 512 and 105, leading to smaller and larger CIs, respectively due to the difference of sample sizes. In this example, the overall confidence interval was computed as the average of the CIs for the two studies. BLI: Blast Lung Injury, TMP: Tympanic Membrane Perforation, SCS: Semi-confined Space, OS: Open Space.	130
4.1	Reticulated polyurethane foams used in this work and their measured porosities using a helium pycnometer. The porosities were computed as the mean of 5 repeat measurements (5 separate foam samples), with the uncertainty calculated as the standard error of the mean. Increasing relative density, ρ^*/ρ_s , corresponds to a greater solid phase proportion in the foam. ρ^* : Foam bulk density, ρ_s : Foam skeletal density.	136
4.2	Polyurethane syntactic foam porosities (ϕ) estimated from optical micrography. For each target porosity, 10 independent optical micrographs were collected from separate syntactic foam samples. The measured porosities were computed as the mean of the estimated porosity for each of the 10 samples. The errors were computed as the standard error of the mean. ρ^* : Foam bulk density, ρ_s : Foam skeletal density.	141

5.1	Key material and calibration parameters of the aluminium 7068 alloy Split-Hopkinson Pressure Bar (SHPB). The subscripts <i>IB</i> and <i>OB</i> refer to the Input Bar and Output Bar, respectively.	172
6.1	Summary of reticulated polyurethane foam quasi-static and dynamic compression moduli (E^*), elastic plateau stresses (σ_{el}^*) and energy absorbed per unit volume (W). These material parameters were calculated as the mean value from the stress-strain curves of 3 repeat experiments, with the uncertainty expressed as the standard error of the mean. ppi: Pores per inch, $\dot{\epsilon}$: strain rate, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.	195
6.2	Summary of syntactic polyurethane foam quasi-static and dynamic compression moduli (E^*), brittle plateau stresses (σ_{cr}^*) and energy absorbed per unit volume (W). These material parameters were calculated as the mean value from the stress-strain curves of 3 repeat experiments, with the uncertainty expressed as the standard error of the mean. ppi: Pores per inch, $\dot{\epsilon}$: strain rate, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.	195
6.3	Summary of quasi-static homogeneous and two-phase hydrogel compression elastic moduli (E). These moduli were calculated as the mean value from the stress-strain curves of 3 repeat experiments, with the uncertainty expressed as the standard error of the mean. $\dot{\epsilon}$: strain rate.	203
6.4	Summary of gelatine quasi-static and dynamic compression moduli (E). These moduli were calculated as the mean value from the stress-strain curves of 3 repeat experiments, with the uncertainty expressed as the standard error of the mean. $\dot{\epsilon}$: strain rate.	207

6.5	Human elastic moduli (E) and densities of anterior-facing thoracic tissues tested under quasi-static compression.	208
6.6	Hyperelastic model parameters for fitting to experimental quasi-static and dynamic compression data. The model goodness-of-fit is quantified as the coefficient of determination.	211
7.1	Ideal gas Equation of State (EoS) properties for the Eulerian air domain in Coupled Eulerian Lagrangian (CEL) simulations.	216
7.2	Comparison of experimental and computational pressure history parameters measured behind blast-loaded ballistic gelatine.	226
8.1	Polyurethane foam peak pressure (τ_P) and positive phase impulse (τ_I) mitigation ratios for confined shock tube loading with a peak incident blast pressure of 3 bar. The uncertainties represent the standard error of the mean over 3 repeat independent measurements. ppi: Pores per inch, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.	248
8.2	Hydrogel peak pressure (τ_P) and positive phase impulse (τ_I) mitigation ratios for confined shock tube loading with a peak incident blast pressure of 3 bar. The uncertainties represent the standard error of the mean over 3 repeat independent measurements.	249

10.1	Recommended uses of the materials considered in this research for a variety of explosive scenarios. The threat nomenclature and TNT equivalent masses are defined by the US Department of Homeland Security in Table 2.1. The overpressure and impulse values are estimates and are derived from TNT characteristic curves for hemispherical explosive charges located on the ground [40]. Examples of such curves are shown in Figure 2.5. TMP: Tympanic Membrane Perforation, BLI: Blast Lung Injury.	282
A.1	Allocation of GTD attack environment for epidemiological study (Chapter 2).	317
B.1	Statistical significance tests for injury rates across different attack settings.	319
D.1	Copyright license details	323
D.2	Copyright licenses for reproduction of figures.	323

List of Symbols & Abbreviations

A	Ratio of explosive charge volume to cross-sectional area (m)
AIS	Abbreviated Injury Scale
APS	Anatomic Profile Score
A_B	SHPB bar cross-sectional area (m ²)
BABT	Behind Armour Blunt Trauma
BLI	Blast Lung Injury
BOP	Peak blast overpressure (Pa)
b_{IB}	SHPB input bar voltage-force calibration parameter
b_{OB}	SHPB output bar voltage-force calibration parameter
b_s	Blast wave time constant
CBIS	Centre for Blast Injury Studies
CEL	Coupled Eulerian Lagrangian
CFD	Computational Fluid Dynamics
CI	Combat Ineffective
C-J	Chapman-Jouguet
CPNI	Centre for Protection of National Infrastructure

CVR	Current Viewing Resistor
c_0	Ambient sound speed (m s^{-1})
C_B	SHPB bar material longitudinal sound speed (m s^{-1})
c_p	Specific heat capacity at constant pressure (J K^{-1})
C_{st}	SHPB striker longitudinal sound speed (m s^{-1})
c_v	Specific heat capacity at constant volume (J K^{-1})
DIC	Digital Image Correlation
DoP	Depth of Penetration (m)
DPV	Directional Probability Variations
d	Stand-off distance between armour and protected structure (m)
d_{10}	Particle diameter where 90% of the particles in the distribution are larger (m)
d_{50}	Particle diameter where 50% of the particles in the distribution are larger or smaller (m)
d_{90}	Particle diameter where 10% of the particles in the distribution are larger (m)
D_B	SHPB bar diameter (m)
ΔE	Energy dissipated by material duration shock wave propagation (J)
ΔE_s	Change of energy during shock compression (J)
ΔE_u	Change of energy during unloading (J)
d_{mb}	Mean microballoon diameter (m)
δr	Mean microballoon wall thickness (m)
D_S	Sample diameter (m)

EOD	Explosive Ordnance Disposal
EoS	Equation of State
E	Shocked specific internal energy (J kg^{-1})
E_0	Ambient specific internal energy (J kg^{-1})
E_B	SHPB bar elastic modulus (Pa)
E_s	Energy absorbed by drop-weight sample (J)
E^*	Foam elastic modulus (Pa)
ϵ_0	Foam stress plateau strain
ϵ_D	Foam densification strain
ϵ_I	SHPB incident strain signal
ϵ_R	SHPB reflected strain signal
ϵ_T	SHPB transmitted strain signal
FBI	Federal Bureau of Investigation
FSI	Fluid Structure Interaction
FSO	Full Scale Output
GSW	Gunshot Wound
GTD	Global Terrorism Database
GTI	Global Terrorism Index
γ	Adiabatic index
IB	Input Bar
IED	Improvised Explosive Device
INIBA	Improved Northern Ireland Body Armour
ISS	Injury Severity Score

I_0	Blast incident impulse (Pa s)
I_s	Blast positive phase transmitted impulse (Pa s)
I_t	Blast transmitted impulse (Pa s)
I_t^{Max}	Maximum transmitted blast impulse (bars)
J_j	Number of casualties with j^{th} injury
k_B	Boltzmann constant (J K^{-1})
K	Number of studies included in epidemiological analysis
k_{IB}	SHPB input bar voltage-force calibration parameter
k_{OB}	SHPB output bar voltage-force calibration parameter
LIVM	Low Impedance Voltage Mode
LD_n	Iso-injury curve for $n\%$ survival probability
L_S	Sample length (m)
λ	Stretch ratio
MCI	Mass Casualty Incident
MoD	Ministry of Defence
MSC	Mild Steel Core
M	Mach number
m_{animal}	Animal mass (kg)
m_f	Areal density (kg m^{-2})
M_p	Ballistic plate momentum per unit area (Pa s)
m_{ref}	Target animal reference mass (kg)
m_{tot}	Total mass of additive microballoons (kg)
μ_1	Expansion fan head angle (rad)

μ_1^{NI}	Expansion fan head angle non-interaction constraint (rad)
μ_1^{NR}	Expansion fan head angle non-reflection constraint (rad)
μ_2	Expansion fan tail angle (rad)
NATO	North Atlantic Treaty Organisation
NIJ	National Institute of Justice
NISS	New Injury Severity Score
N	Total casualties across all studies in epidemiological analysis
N_k	Number of casualties reported in k^{th} study (sum of hospital admissions and pre-hospital deaths)
n	Sample of total casualties (N) across all studies in epidemiological analysis
n_k	Sample size of casualty population (N_k) in k^{th} study
$\nu(M)$	Prandtl-Meyer function
OB	Output Bar
OS	Open Space
PBI	Primary Blast Injury
PC	Pochhammer-Chree
PPE	Personal Protective Equipment
ppi	Pores per inch
PRISMA	Preferred Reporting Items for Systematic Reviews and Meta-analysis
PU	Polyurethane
PU30	Shore hardness 30 A polyurethane
PVDF	Polyvinylidene Fluoride
PZT	Lead Zirconate Titanate

P	Shocked material pressure (Pa)
P_0	Unshocked material pressure (Pa)
P_{atm}	Atmospheric pressure (Pa)
p_j	Proportion of total casualty population (N) presenting j^{th} injury
$p_{k,j}$	Proportion of casualties presenting j^{th} injury in k^{th} study
$\hat{p}_{k,j}$	Unbiased estimator of $p_{k,j}$
P_s	Blast peak gauge pressure (Pa)
P_{sea}	Atmospheric pressure at sea level (Pa)
ϕ	Foam porosity
PI_{50}	Impulse iso-injury curve for 50% lethality probability
R	Radial distance from detonation centre (m)
R_{mb}	Mean microballoon radius (m)
ρ^*/ρ_s	Foam relative density
ρ	Shocked material density (kg m^{-3})
ρ_0	Unshocked material density (kg m^{-3})
ρ_B	SHPB bar density (kg m^{-3})
ρ_{mb}	Microballoon true density (kg m^{-3})
ρ_s	Foam skeletal density (kg m^{-3})
ρ^*	Foam bulk density (kg m^{-3})
SCS	Semi-Confined Space
SEM	Scanning Electron Microscope
SHPB	Split-Hopkinson Pressure Bar
SVAVG	Finite element volume-averaged stress components

SRS	Simple Random Sampling
SSD	Sum of Squared Differences
START	National Consortium for the Study of Terrorism and Responses to Terrorism
σ_{hyd}	Hydrostatic stress (Pa)
σ_i	Incident stress (Pa)
σ_{cr}^*	Elastic-brittle foam plateau stress (Pa)
σ_{el}^*	Elastomeric foam plateau stress (Pa)
σ_f^*	Hydrogel failure stress (Pa)
σ_{pl}^*	Elastic-plastic foam plateau stress (Pa)
σ_{plat}^*	General foam plateau stress (Pa)
σ_r	Reflected stress (Pa)
σ_t	Transmitted stress (Pa)
TBI	Traumatic Brain Injury
TMP	Tympanic Membrane Perforation
t_0	Time scaled for animal mass (s)
t_+	Blast wave positive phase duration (s)
t_{crit}	Characteristic timescale for thoracic oscillation (s)
T_L	SHPB striker loading duration (s)
T_{min}	Minimum foam thickness for densification (m)
τ_I	Impulse mitigation ratio
τ_P	Peak pressure mitigation ratio
θ	Expansion fan turn angle (rad)

θ_{max}	Maximum supersonic flow turn angle (rad)
v	Specific volume ($\text{m}^3 \text{kg}^{-1}$)
V_{bulk}	Volume of syntactic foam polyurethane matrix material (m^3)
V_s	Foam skeletal volume (m^3)
V_{tot}	Total volume of sample optical microscope region (m^3)
V^*	Bulk foam volume (m^3)
$\hat{V}(\hat{p}_j)$	Pooled variance estimator of proportion of casualty population with j^{th} injury
UTM	Universal Testing Machine
u_p	Shocked material particle velocity (m s^{-1})
u_{rig}	UTM non-sample displacement (m)
U_s	Shock velocity (m s^{-1})
u_{sample}	UTM sample displacement (m)
u_{tot}	UTM total actuator displacement (m)
w	Diameter of shock tube expansion (m)
W	Foam energy absorption per unit volume up to densification strain ϵ_D (J m^{-3})
W_c	Explosive charge mass (kg)
W_k	Weight assigned to k^{th} study
$y_{k,j}$	Casualty outcome parameter for k^{th} study and j^{th} injury (0 if injured, 1 if not)
Z	Acoustic impedance (Pa s m^{-3})
ζ	FSI parameter

Chapter 1

Introduction

1.1 Motivation

1.1.1 Overview

In a geopolitically unstable modern world, blast remains prevalent in both military and civilian contexts. This necessitates the development of methods for effectively mitigating the damaging effects of an explosion. Unfortunately, examples of such devastation are abundant. In the civilian environment, recent occurrences of blast in Western Europe include the Madrid (2004), London (2005), Norway (2011), Paris (2015), Brussels (2016) and Manchester (2017) bombings. Regarding military personnel, a review of US injury rates during the Iraq and Afghanistan conflicts revealed that approximately 75% of hostile injuries were inflicted by Improvised Explosive Devices (IEDs) [1].

These circumstances have given impetus to comprehensively understanding modern IED injury patterns, biological response to blast and the requirements for materials that can effectively attenuate the incident shock wave. It remains that blast injuries are highly complex and relatively poorly understood. This is primarily due to the multiphase nature

of the insult, the inherent heterogeneity of the body's tissues and the role of the blast environment. Nonetheless, armour technology has advanced dramatically in the last 30-40 years, with thicker, more massive and impractical armour no longer automatically implying added protection on the modern battlefield. Rather, advances in materials, diagnostic equipment and modelling have driven progress [2]. However, such armour is conventionally designed to defeat ballistic threats. While penetrating trauma is clearly an important component of blast loading, particularly from IEDs, the mitigation of the preceding blast wave has received relatively little attention. In the field of blast mitigation, a body of research exists primarily focussed on civil infrastructure applications with worthwhile experimental and numerical research having been devoted to, for example, reinforced concrete [3] and sandwich composites [4–12] under blast loading.

However, for personal applications, where there is an emphasis on lightweight solutions that do not unduly compromise the wearer's mobility [13–17], there is a need to better understand the role of body armour in protecting the wearer from the damaging effects of the blast wave. This is known as primary blast injury. This situation is complicated by the close proximity of the protected structure (*i.e.* the body). As such, armour must take into consideration human tolerance to blast as well as the significance of ergonomic factors (*e.g.* fit, moisture level *etc.*) in the armour's performance and expectation for human survivability.

1.1.2 Improvised Explosive Devices

The US Department of Defence defines IEDs as 'devices placed or fabricated in an improvised manner incorporating destructive, lethal, noxious, pyrotechnic or incendiary chemicals, designed to destroy, disfigure, distract or harass and often incorporating military stores' [18]. The proliferation of IEDs in modern warfare can be attributed to their ease of manufacture and the fact that they can be triggered by the target. The target's close proximity is devastating while affording the distant attacker protection [19, 20]. In particular, roadside IEDs are highly damaging due to efficient momentum transfer of the

detonation products to the vehicle [21]. Thus, IEDs have been predominantly responsible for loss of military vehicles since World War II [20]. Since IEDs are often buried, the injury pattern can be characteristic in the trauma inflicted upon the lower limbs, lungs and brain [22, 23]. It should be noted that the term ‘IED’, given its broad definition, encompasses devices ranging from crude and home-made to sophisticated high-explosives [20]. Hence, differing IEDs may inflict vastly contrasting injury profiles, posing a significant challenge to the effective treatment and rehabilitation of victims. This task can be ameliorated by intermediary protective structures designed to attenuate the blast wave. This is not confined to the military sphere. In civilian contexts, such needs can arise in Explosive Ordnance Disposal (EOD) and protection of law enforcement personnel [13, 24, 25].

1.1.3 Blast Injury

The advent of World War I may mark the beginning of concerted scientific efforts to understanding blast injury [26]. Injuries sustained on the battlefield were distinctly different in nature to that observed in the civilian environment. As such, early rudimentary diagnoses of blast injuries attributed common causes of death to blanket terms of the unexplained effects on the body or nervous system such as ‘shell shock’ [26].

Since then, a more sophisticated understanding of blast injury mechanisms has been achieved through studies of injuries sustained during more recent conflicts [20, 22, 26]. It remains that much of our current understanding of blast injury is derived from the battlefield due to the frequency of explosive incidents. Wounds inflicted by IEDs in Iraq and Afghanistan were complex and distinct from that seen from conventional weapons. There are several reasons for the unique pattern of injury inflicted by blast. Firstly, the mechanical response of biological tissues is strongly strain rate dependent, and strain rates experienced under blast loading are significantly higher than ordinary life (such as falling from a height or road traffic accidents) [19]. Secondly, blast typically subjects the body to multiple injury mechanisms simultaneously (‘polytrauma’) [27]. Finally, the loading depends upon the attack environment and may include further stages following

the blast wave such as fragment impact. There is thus a concerted effort to understand the pathophysiology of blast injury.

1.2 Objectives

There is present lack of understanding of the effects that are significant in protecting the body against blast using armour materials appropriate for personal use (see Chapter 2). This includes the effectiveness of conventional ballistic body armour (soft or hard) for mitigating blast injury, as well as developing an understanding of how alternative materials might dissipate blast energy. Prior studies have neglected the intermediate strength nature of the protected structure [28].

To address this research problem, it was important that blast mitigation was informed first by clinical observations of modern blast injury patterns. As a result, a holistic approach was taken by using findings from previous bombings to motivate not only the need for blast injury mitigation but also the most vulnerable anatomical regions.

The key objectives of the project were to:

1. Motivate the modern problem of blast and the need for mitigation of primary blast injury.
2. Investigate the effect of material properties and their positioning relative to the protected structure on their blast mitigation performance.
3. Assess the injury mitigation performance of the mitigation materials and ballistic armour, estimating survivability.

1.3 Thesis Outline

This thesis is comprised of 10 chapters, outlined as follows:

- Chapter 1** Introductory discussion summarising the motivations, central themes and objectives of this work, as well as declarations and publications.
- Chapter 2** Fundamentals of blast loading, blast injury, casualty criteria and relevant experimental platforms. Prior clinical studies and blast mitigation research are reviewed and emerging research questions this work aims to address are identified.
- Chapter 3** Epidemiological study analysing the occurrence of terrorist bombings in the civilian environment and resulting blast injury patterns, as well as the dependence of these injuries on the attack setting.
- Chapter 4** Overview of the mitigation and surrogate materials used in the experimental component of this work. The rationale for these materials and the preparation methods are discussed.
- Chapter 5** Overview of the experimental platforms, techniques and diagnostics used for mechanical characterisation and blast loading studies.
- Chapter 6** Mechanical characterisation results for the mitigation and surrogate materials over a range of strain rates.
- Chapter 7** Setup and methodology for computational blast loading simulations of the shock tube flow. Simulations of sample-loaded experiments are presented in Chapters 8 and 9 with the corresponding experimental data.
- Chapter 8** Results of shock tube blast mitigation experiments for the mitigation materials introduced in Chapter 4, investigating effects such as incident blast loading pressure and duration, rear boundary condition, and material confinement.
- Chapter 9** Results of large-scale blast injury mitigation experiments for ballistic armour and hydrogel mitigation materials.
- Chapter 10** Conclusions of the research, covering the main outcomes of this work and areas for future research.

1.4 Declaration of Originality

I declare that, to the best of my knowledge, this thesis is my own work and has not been submitted for any degree or other purposes. I certify that the intellectual content of this thesis is the product of my own work and that all assistance from others in the preparation of this work has been properly acknowledged.

1.5 Copyright Declaration

The copyright of this thesis rests with the author. Unless otherwise indicated, its contents are licensed under a Creative Commons Attribution-Non Commercial-No Derivatives 4.0 International Licence (CC BY-NC-ND). Under this licence, you may copy and redistribute the material in any medium or format on the condition that; you credit the author, do not use it for commercial purposes and do not distribute modified versions of the work. When reusing or sharing this work, ensure you make the licence terms clear to others by naming the licence and linking to the licence text. Please seek permission from the copyright holder for uses of this work that are not included in this licence or permitted under UK Copyright Law.

1.6 Publications

- D Magnus, DR Sory, J Lee, MA Khan, and WG Proud. Study of soft material blast mitigation effects using a shock tube. In *AIP Conference Proceedings*. AIP Publishing, 2019. (Accepted).
- D Magnus, MA Khan, and WG Proud. Epidemiology of civilian blast injuries inflicted by terrorist bombings from 1970-2016. *Defence Technology*, 14(5):469–476, 2018. Available from: doi:10.1016/j.dt.2018.07.014.
- D Magnus, A Haller, A Jouffroy, and WG Proud. Tensile characterisation of

the aorta across quasi-static to blast loading strain rates. In *AIP Conference Proceedings*, volume 1979, page 090010. AIP Publishing, 2018. Available from: doi:10.1063/1.5044867.

- D Magnus, LC Smith, and WG Proud. Multiple wave compression of granular materials using the split-Hopkinson pressure bar. In *AIP Conference Proceedings*, volume 1979, page 110009. AIP Publishing, 2018. Available from: doi:10.1063/1.5044928.
- GR Tear, AH Cohen, D Magnus, DR Sory, and WG Proud. Damage characterisation for cement and concrete using microwave induced damage. In *Multidisciplinary Digital Publishing Institute Proceedings*, volume 2, page 525, 2018. Available from: doi:10.3390/ICEM18-05418.

Chapter 2

Background

2.1 Shock Waves

A characteristic component of a blast wave is the existence of a shock front. The theory of shocks was developed from the 19th Century with significant contributions from individuals including Rayleigh, Rankine and Hugoniot [29]. A shock wave is supersonic with respect to the medium it propagates in and is defined by a leading shock front where there is a discontinuity of pressure, temperature and density. Thus, there is a sharp adiabatic transition between the thermodynamic state of the shocked and unshocked material. This is represented in Figure 2.1.

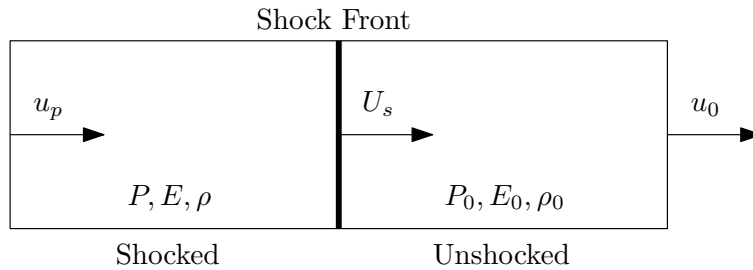


Figure 2.1: As the (one-dimensional) shock front propagates at velocity U_s , it defines the discontinuity between the shocked and unshocked states. The particles in the shocked material move with velocity u_p ($u_p < U_s$). This region is described by the variables pressure (P), internal energy (E) and density (ρ). Note that $\rho = 1/v$ where v is specific volume. Analogous variables are defined for the unshocked material with the subscript '0'. Adapted from [30].

The Figure 2.1 representation provides a basis for deriving the conservation relations of one-dimensional shock propagation. These are known as the Rankine-Hugoniot equations and represent conservation of mass, momentum and energy (Eq. (2.1)). It is implicit in these equations that the shock is steady with zero rise-time and thickness [31]. Moreover, the material is treated hydrodynamically with zero strength, viscosity and heat conduction. It should be noted that real shock waves exhibit three-dimensionality, finite rise-time and energy dissipation, but these effects are often negligible on the rapid timescale of a shock wave [30, 32].

$$\rho(U_s - u_p) = \rho_0(U_s - u_0), \quad \text{Conservation of Mass} \quad (2.1a)$$

$$P - P_0 = \rho_0(U_s - u_0)(u_p - u_0), \quad \text{Conservation of Momentum} \quad (2.1b)$$

$$E - E_0 = \frac{1}{2}(P + P_0) \left(\frac{1}{\rho_0} - \frac{1}{\rho} \right). \quad \text{Conservation of Energy} \quad (2.1c)$$

A cornerstone of modern shock physics, the Rankine-Hugoniot equations relate kinematic (u_p, u_0, U_s) and thermodynamic $(P, P_0, E, E_0, \rho, \rho_0)$ variables across the shock. Assuming the parameters of the unshocked state are well-known, a relationship between only two shocked state variables must be established in order to determine all remaining variables and the material's Equation-of-State (EoS) [30, 31]. This empirically determined relationship is known as a Hugoniot [30]. For the special case of a shock propagating in an ideal gas, this is simplified by the fact that only one parameter, U_s , is required to compute all remaining shock variables. The shock front velocity is often represented by the Mach number (M), defined as:

$$M = \frac{U_s}{c_0}, \quad (2.2)$$

where c_0 is the local sound speed of the unshocked gas under ambient conditions. This is given in an unshocked ideal gas by: $c_0 = \sqrt{\gamma k_B T / m}$, where k_B is the Boltzmann constant, T is absolute temperature and m is the mass of a molecule. The adiabatic index γ is given by $\gamma = c_p / c_v$, where c_p and c_v are the specific heat capacities at constant pressure and volume, respectively. For an ideal monatomic gas, $\gamma = 5/3$. The application of the

Rankine-Hugoniot equations to an ideal gas forms the theoretical basis of the shock tube which is further discussed in Section 2.6.

Finally, Eq. (2.1) describes the shock loading of a material from initial specific volume v_0 to a compressed specific volume, termed here as v_1 . A secondary process is the release of the material as it unloads back to the P_0 unshocked pressure and a final specific volume of v_2 . Assuming this is an adiabatic process ($dS = 0$), the change of energy during unloading (ΔE_u) from v_1 to v_2 is given by:

$$\Delta E_u = - \int_{v_1}^{v_2} P dv. \quad (2.3)$$

Defining the change of energy during shocking of the material as $\Delta E_s = E - E_0$ (given by the Eq. (2.1c) Rankine-Hugoniot relation), the total energy dissipated by the material during shock propagation is:

$$\Delta E = \Delta E_s + \Delta E_u = \frac{1}{2}(P + P_0) \left(\frac{1}{v_0} - \frac{1}{v_1} \right) - \int_{v_1}^{v_2} P dv. \quad (2.4)$$

If the objective of the material is to mitigate energy transmission, Eq. (2.4) highlights the properties of a good impact absorber. Energy dissipation ΔE is maximised for large initial v_0 and small v_1 and v_2 after shock compression and unloading. Alternatively, as density is the inverse of specific volume, this can be considered as a material with much higher compressed and final density than its initial density. This describes a highly compressible material. Cellular materials are a commonly applied example of such materials that are engineered for high compressibility and are discussed in Section 2.4.1.

2.2 Blast Waves

A blast wave is an example of a shock wave generated by the fast (microsecond timescale) deposition of a large amount of energy in a small volume. Causes may include rupture of high-pressure vessels, uncontrolled nuclear reactions or detonation of explosives. In the

latter case, an initial stimulus triggers a hierarchical explosive train such that a shock propagates through the explosive (‘detonation wave’) [21]. This shock acts to convert unreacted material to higher temperature and pressure detonation products over the width of the reaction zone [20, 21, 33]. Characteristic pressures may be of the order of 1 GPa to 10 GPa [20]. The interface at which all material has transitioned to detonation products is known as the Chapman-Jouguet (C-J) interface [2]. For an open-air blast, the arrival of the detonation wave at the charge surface generates a radially expanding shock that compresses the surrounding air to a peak pressure P_s near instantaneously [21, 26, 33, 34]. Behind the shock front, gas expands rapidly at speeds of the order of km s^{-1} , known as the ‘blast wind’ [21, 26]. This rapid expansion rarefies the air, generating a release wave that moves counter to the shock front and drops the pressure back to atmospheric level (P_0). Further expansion of the gas drops the local pressure below atmospheric (‘negative pressure’) [21, 26, 33, 34]. Finally, the surrounding air flows back into the partial vacuum in the wake of the blast wind, returning the pressure back to ambient level on a timescale of the order of 10 ms (see Figure 2.2(a)).

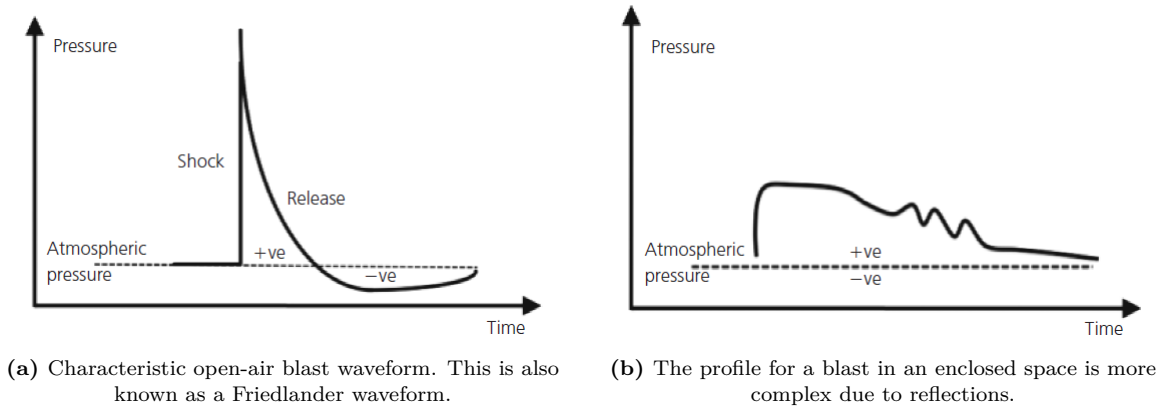


Figure 2.2: Characteristic blast pressure histories for a fixed (Eulerian) point observer in the (a) free environment and the (b) enclosed environment [22].

In this scenario, the Friedlander waveform is the analytical representation of the pressure-time profile, $P(t)$, for a blast wave expanding freely in the atmosphere with no reflections (see Figure 2.2(a))[19, 21, 22, 26, 35]. It is given by:

$$P(t) = P_{atm} + P_s \left(1 - \frac{t}{t_+}\right) e^{-b_s t/t_+}, \quad (2.5)$$

where t is time, t_+ is the positive phase time duration and b_s is a constant describing the rate of pressure release. The peak blast overpressure, commonly abbreviated to ‘BOP’, is given by $BOP = P_{atm} + P_s$. These parameters are dependent upon the proximity and nature of the explosive. For this free-field case, the isotropic expansion of the blast wave in three dimensions means that the shock can be described by classical one-dimensional theory in the far-field.

By contrast to this idealised scenario, the blast pressure profile in a confined or partially confined environment results in greater complexity due to reflections of the pressure wave. In addition, the positioning of the explosive relative to the ground is also significant, as illustrated in Figure 2.3 [36].

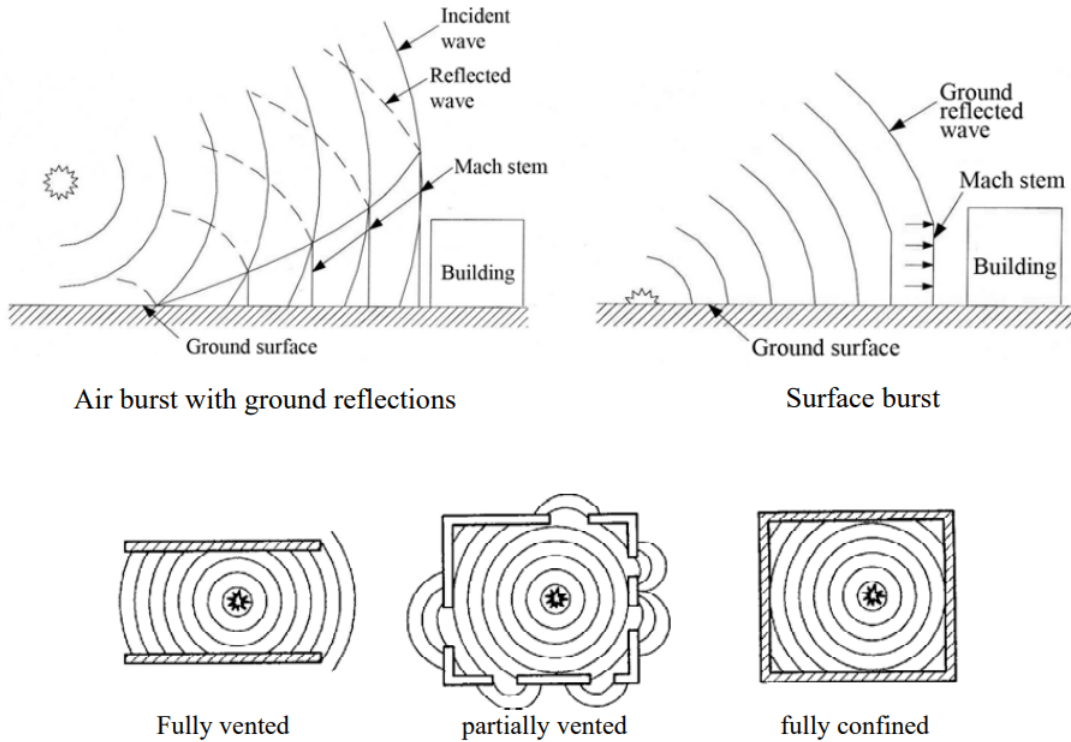


Figure 2.3: Different explosive scenarios. (Top) Detonation of the charge on the ground or at a fixed height above the ground result in constructive reinforcement of the blast wave. In the latter case, a Mach stem of increasing height with distance from the detonation centre is formed where the incident and reflected blast waves reinforce each other. (Bottom) Ventilation of the blast environment is an important factor determining the resulting pressure profile. Adapted from [36].

For the air burst explosion at a stand-off height from the ground, a reflected wave is generated from the ground that reinforces the incident wave to form a Mach stem

that has an increasing height with distance. By contrast, for the ground explosion, the incident and reflected waves merge at the detonation point to form a single wavefront. It is noteworthy that in a terrorist attack, the IED is often detonated on or close to the ground in a confined or built area. In such an enclosed or cluttered environment, the loading is rendered more complex still with the loading duration and accumulation of gaseous explosive products being highly dependent on the level of venting. In a fully vented scenario (*e.g.* a building-lined street), the detonation products are vented and no gas pressure is built up. A partially vented explosion is analogous to an environment with doors and windows, and will be associated with greater reflections and confinement of gases dependent on the failure of the confining objects. Finally, a fully confined scenario is associated with high gas confinement and loading duration where the subsequent pressure drop also depends on heat conduction through the confining walls (see Figure 2.2(b)). Taking a vehicular scenario as an example, additional internal reflections within the vehicle are particularly devastating to the occupants as the blast is slower to dissipate, prolonging the blast duration and increasing the severity of injury [22, 26, 33].

Many IEDs produce a more complex waveform still with loading profiles that are a function of the threat type, amount, distance, casing and deployment tactics. For buried devices, the energy release is mediated by the surrounding material (*e.g.* soil). The energy release causes initial compaction of the soil and creates a transiently impermeable region of material that weakly confines the product gases [22]. Eventually, this region fractures and the product gases accelerate through the voids, simultaneously ejecting soil material outwards at supersonic speed [20]. Applied to a vehicle, the blast wave is strengthened as it undergoes multiple reflections between the ground and vehicle floor, while the soil ejecta impacting the vehicle add further peaks to the pressure profile [22, 26].

The effect of distance is taken into account by blast scaling laws, the most common of which was introduced by Hopkinson-Cranz [37]. The scaling is formulated such that two explosives of the same type but differing mass are positioned at the same scaled distance such that the resulting pressure profile at the same target surface is identical (assuming

other variables such as atmospheric conditions are controlled). The Hopkinson-Cranz law predicts that the blast peak pressure relates to this scaled distance (Z) according to the following function:

$$P_s = f\left(\frac{R}{\sqrt[3]{W_c}}\right) = f(Z), \quad (2.6)$$

where R is the distance between the detonation centre and the point of interest, W_c is the mass of the charge and f is an arbitrary function that can be empirically determined for a given charge type. For the idealised case of a spherical point charge detonated in free space, the function is simply a constant in the far-field. One of the important factors here in determining the dynamics of the blast wavefront is the geometry of the charge. Indeed, a numerical study by Artero-Guerrero *et al.* showed that as the aspect ratio A increases, equivalent to the ratio of the volume to cross-sectional area of the charge, the blast wavefront flattens [38].

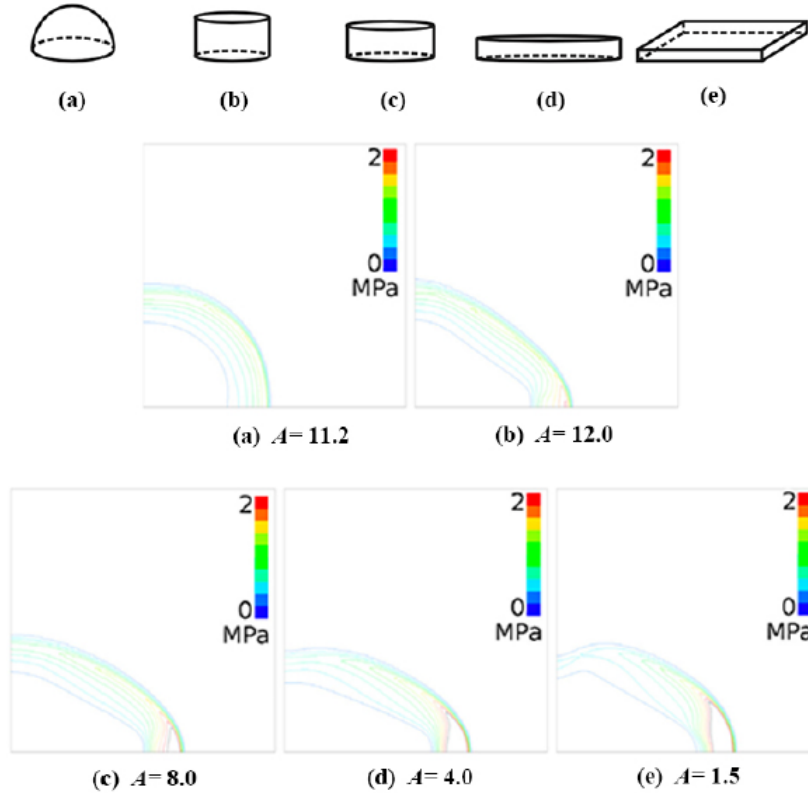


Figure 2.4: Simulations demonstrating the dependence of blast wavefront geometry on the aspect ratio of the charge's shape, A . The hemispherical and flat charge masses were 14.2 g and 15 g, respectively. In each simulation, the charge was located on the ground in the bottom left corner, and the horizontal and vertical axes extend up to 1 m from the detonation centre. The magnitude of the pressure field is indicated by the scale bars [38].

The distribution of the expanding shock front over increasing volume implies that the peak pressure diminishes with distance. The case of differing ambient conditions is addressed by the analogous Sach's Law [37]. The charge mass W is often taken as the TNT equivalent, which is the equivalent mass of TNT required to yield the same energy release. A variety of relationships between TNT equivalent mass and experimental charge mass exist, commonly using the ratio of detonation heats. A summary of common IEDs with corresponding TNT equivalent values and safe stand-off distances as recommended by the US Department of Homeland Security is shown in Table 2.1.

IED	TNT Equivalent Mass (kg)	Indoor Evacuation Distance (m)	Outdoor Evacuation Distance (m)
Small Package/Letter	0.5	12	274
Pipe Bomb	2.5	21	366
Vest/Container Bombs	9.0	34	518
Parcel Package	23	46	564
Compact Car	230	98	579
Full Size Car/Minivan	450	122	732
Van/SUV/Pickup Truck	1800	195	1158
Delivery Truck	4500	262	1554

Table 2.1: Example Improvised Explosive Device (IED) TNT equivalences and recommended safe stand-off distances defined by the US Department of Homeland Security. The TNT equivalent mass denotes the mass of TNT required to yield the same energy release as the IED. The indoor distance assumes that buildings provide a degree of protection to the occupants and is calculated based on the threshold to avoid building collapse. The outdoor safe stand-off distance is farther due to the shrapnel throw distance. This distance is limited by the need to avoid fragment impact injuries from shrapnel or glass breakage as this is considered a greater risk than overpressure at large distances due to rapid attenuation of the blast wave. Adapted from [39]

As an example, the blast scaling of peak overpressure and impulse has been empirically determined for hemispherical TNT charges placed on the ground [40]. In this case, the Eq. (2.6) Hopkinson-Cranz scaling takes a different form in the near-field ($1 \geq Z \geq 10$) and far-field ($10 \geq Z \geq 200$). Using the TNT equivalent masses defined in Table 2.1 for the different IEDs, such a scaling allows the pressure-impulse-distance relationship, known as the characteristic curve, for each threat to be calculated. For comparison, these characteristic curves are shown for a pipe bomb and delivery truck bomb in Figure 2.5.

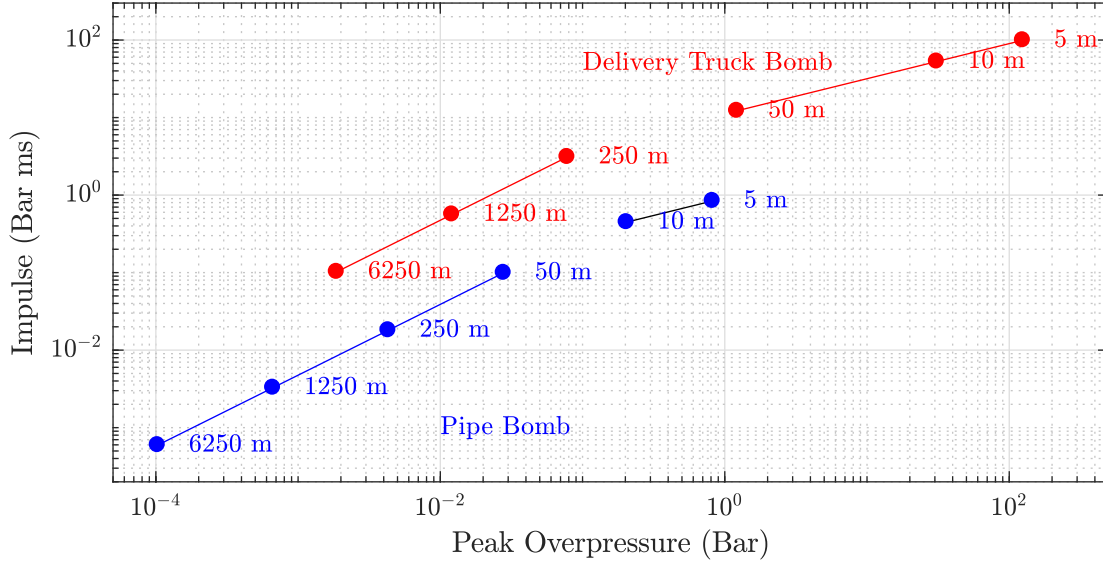


Figure 2.5: Characteristic curves expressing the blast pressure-impulse-distance relationship for two Improvised Explosive Devices (IEDs). These curves have been derived from scaling relationships measured by Alonso *et al.* for hemispherical shaped charges positioned on the ground [40]. The data point labels indicate distances in metres from the charge. Two distance regimes are visible for each curve, corresponding to the near-field and far-field. The pipe bomb and delivery truck bomb have TNT equivalent masses of 2.5 kg and 4500 kg, respectively, defined according to the US Department of Homeland Security in Table 2.1. Note that the recommended safe stand-off distances in Table 2.1 account for all threats from an IED and not only the blast wave (*e.g.* shrapnel).

2.3 Blast Injury

2.3.1 Blast Interaction with the Body

The interaction of the blast wave with the body is complex and has many dependencies including whether the body is directly exposed to the blast (*e.g.* directly triggering an IED on foot), termed ‘unmounted’, or exposed while occupying a vehicle, termed ‘mounted’ [21]. Initially, the blast wave causes a local compression of the body since the first part of the blast wave to reach the body is relatively directional. The higher sound speed in the body causes this compression to propagate into thus far unaffected regions of the body. This causes differential acceleration of regions of the body. Softer tissues are compressed and may tear or rupture. Subsequently, the body is completely exposed to the blast wave and experiences a general compression. Finally, there is a push-pull effect caused by the release wave. The parts of the body most adversely affected by these processes are those

with the greatest gas content *i.e.* the larynx, lungs, ears and gastrointestinal tract [21]. In an enclosed space, the waveform becomes significantly more complex, with reflections occurring from the surrounding surfaces. In approximate terms, an increasing number of reflections is correlated with the extent of damage sustained [21]. For example, in a vehicle, the peak overpressure is reduced in magnitude as the blast wave is transmitted through the floor, but subsequent reflections from the interior of the vehicle cause the blast loading to become more complex and occur over a longer duration [22]. An additional effect is that confinement of the blast between the ground and vehicle underside results in an efficient transfer of momentum to the vehicle and hence more severe tertiary injuries (see Section 2.3 for injury terminology). Another example is the case of the body being situated close to a wall or relatively immobile surface; the body is subjected to further high-pressure pulses due to reflections [21]. For the same reason, lying prone to the ground has been found to result in a different pattern of injury where the abdominal viscera are injured more severely [41].

2.3.1.1 Pathophysiology

Blast injuries can be inflicted by a wide variety of mechanisms and they are typically classified according to four different categories: primary, secondary, tertiary and quaternary [33, 42–44].

Primary Injuries

Primary Blast Injury (PBI) is caused by interactions of the blast wave with tissues as it propagates through the body as stress, shock and shear waves. A stress wave propagates at the tissue's sound speed, while shock waves are at higher velocity, pressure and amplitude. Shear waves propagate transverse to the shock front and have lower velocity, longer duration and may be responsible for strong distortion of soft tissues [26]. The mechanisms for many primary blast injuries lie in the fact that the body is composed of tissues with different mechanical properties. They fall into three broad categories:

- Spall: A compression wave propagating through higher density tissue is strongly reflected at an interface with much lower impedance as a tensile pulse. This may reach the ultimate tensile strength of the local tissue. This causes failure such as fragmentation of the higher density tissue or forcible movement of fluid into another region *e.g.* blood displaced from a capillary to an alveoli to cause alveolar haemorrhage [26].
- Implosion (barotrauma): The blast pressure wave rapidly compresses gas-containing parts of the body (more compressible than liquids) which subsequently re-expand. This process is associated with strong shear and distortion, and is the likely cause of prevalent PBIs such as Blast Lung Injury (BLI) and Tympanic Membrane Perforation (TMP).
- Inertia: The heterogeneous nature of the body's constituent tissues cause different regions to accelerate differentially, resulting in tensile failure between adjacent parts.

Given these mechanisms, the lungs, gastrointestinal tract and middle ear are the most susceptible to PBI on account of their high air content [20]. An example case for the prevalence of TMP is the 2004 Madrid bombing where 41% of victims sustained this injury [33]. For vehicles, the increased distance between the occupants and explosion, as well as the interaction of the vehicle structure with the blast wave, will mitigate primary blast injuries that are sensitive to peak pressure [20]. Indeed, Champion *et al.* showed that the overpressure outside an armoured vehicle was 28 times greater than that inside for a charge detonated 3 metres away [45]. However, the longer duration of loading within a vehicle may cause a different pattern of injuries to manifest. For BLI, the coupling of the pressure wave into the thorax causes pathologies observable to both the macroscopic and microscopic level through optical and electron microscopy. BLI will typically manifest through blood-filled alveoli and airways, alveolar wall disruption and bleeding capillaries [46]. At smaller length scales it has been observed that even regions without bleeding may also present histopathological changes [47].

Secondary Injuries

Secondary injuries are caused by debris or fragments impacting the body and inflicting penetrating or blunt trauma [20, 33, 43]. The lethality of these objects can vary considerably, with some that can be counteracted by light personal armour [27] and others that may penetrate vehicles and pose threat to occupants, necessitating armouring of the vehicle underside [20]. It is noteworthy that armour may often provide effective ballistic protection but does not necessarily protect the target from PBI. For example, a study of the 216 British military deaths due to IRA activities from 1970 to 1984 found that 90% of those killed were equipped with body armour and had not suffered penetrative trauma [26].

Tertiary Injuries

Tertiary injuries are caused by acceleration of the body against mobile or fixed objects [33]. Such injuries may include further penetrating and blunt trauma, traumatic amputation, fractures and brain injury. In enclosed environments, such as vehicles, the possibility for tertiary injury is exacerbated by the possibility of being struck by the deforming crew compartment [20, 22]. For example, the floor plate of a vehicle struck by an anti-vehicle mine will exert axial load on the occupants which is associated with lower limb, pelvic and spinal injury patterns [20]. In addition, the whole vehicle may be accelerated as soil ejecta transfers its largely upward momentum to the vehicle. For the same explosive threat, a lighter vehicle will experience greater acceleration and its occupants are thus more vulnerable to a vertical acceleration injury such as lumbar spine injury [20]. An unrestrained occupant may suffer head, neck and upper limb injuries from striking the roof, while unrestrained equipment may also strike the crew [20]. For these reasons, tertiary effects are considered to be the most prevalent mechanism of injury for vehicle occupants struck by blast [20].

Quaternary Injuries

Injuries that are not defined as primary, secondary or tertiary are categorised as quaternary [33]. These may include injuries caused by thermal effects (*e.g.* burns), chemicals, post-incident events (*e.g.* drowning) or complications of pre-existing conditions [20, 33].

2.3.2 Trauma Severity Scales

A method for quantifying trauma severity is critical for mitigation strategy, injury research, prognosis evaluation and informing triage. It also allows for a comparison of the outcomes of trauma care. Modern methods are typically anatomical-based scoring systems, and are widely utilised in clinical studies [48, 49]. Introduced in 1969, the Abbreviated Injury Scale (AIS) was the first widely implemented measure. The AIS encodes the type, location and severity of a single injury as a 7-digit number. The body is divided into 9 separate anatomical regions, within which the specific type of biological structure and injury are identified (see Figure 2.6).

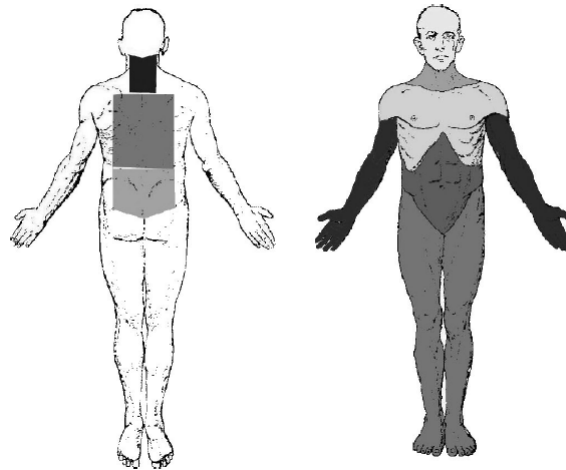


Figure 2.6: Illustration of the 9 body areas in the Abbreviated Injury Scale (AIS) injury scoring system. Posterior view (left): cervical spine, upper back and lower back. Anterior view (right): head, neck, chest, abdomen, arms and legs. This framework for categorising injuries and quantifying severity is widely used in clinical research. Reproduced from [50].

The severity of injury is ranked on a scale of 1-6 where 1 represents a minor injury and 6 is an unsurvivable injury. This severity scale was determined by a group of experts

on the basis of four criteria: threat to life, permanent impairment, treatment period and energy dissipation. Given the subjective nature of this assessment, it is challenging to ‘standardise’ injury classifications due to differences in judgement. As such, clinical studies of traumatically injured populations often consider patients admitted to a particular health care centre [49, 50], as discussed in Section 2.3.3. The primary criticism of the AIS is the poor correlation between the severity score and survival probability [51].

A variety of injury scales have been developed to address this shortcoming, including the Injury Severity Score (ISS) and New Injury Severity Score (NISS), both derived from the AIS framework. These measures are often more appropriate for blast injury since they allow for polytrauma. The ISS is the sum of the squares of the AIS scores for the 3 most severely injured body regions. No region can be represented more than once in this calculation. The rationale for the summing of squares was that it yielded greater predictability of mortality [51]. The ISS score ranges from 1-75 where 1 is a single minor injury and 75 represents three distinct body regions with an AIS injury severity of 5. Any AIS score of 6 (fatal) automatically sets the ISS score to the maximum of 75. The primary criticisms of the ISS are that it does not account for multiple injuries to an individual anatomical region, and that equal weight is assigned to differing injuries. In the case of, for example, severe head injuries, this results in an underestimation of the probability of mortality. The NISS is a variation of the ISS that allows for the AIS scores to be summed regardless of body region. However, both the ISS and NISS inherit some of the drawbacks of the AIS including the subjective nature of the severity scoring. Note that further scoring systems exist that attempt to weight more severe injuries, such as the Anatomic Profile Score (APS), but such systems are less frequently employed [52].

2.3.3 Clinical Observations of Injured Populations

Blast mitigation for personal applications must consider both the mitigation material and also the nature of the body. Given the complexity of the human body and its constituent tissues, comprehensive and validated mechanical and numerical models of

the body remain elusive. As such, it is necessary to appeal to empirical evidence of past injuries to understand the nature of modern threats and particularly vulnerable areas of the body. This is crucial for informing mitigation strategy.

Clinical studies of injured military and civilian populations following explosions have been carried out by a number of investigators [48, 49] with the intention of identifying markers for more severe injuries, as well as associating observed injury patterns with the nature of the explosive, environment and target proximity. A caveat that must be acknowledged is that the nature of such clinical data depends strongly on the recording methods of different trauma registries. For example, there are a broad range of definitions for a ‘casualty’. These can range from a temporary hearing injury to those injuries with 99% mortality [53].

In the civilian case, studies of casualties admitted to hospitals in the aftermath of terrorist bombings exist. Almogy *et al.* [48] and Bala *et al.* [49] studied the trauma database records of those admitted to Hadassah Hospital (Jerusalem, Israel) from August 2001 to August 2004 (154 patients) and October 2000 to December 2005 (181 patients), respectively. Almogy *et al.* found that BLI and intra-abdominal injury were the two leading causes of in-hospital mortality with an incidence of 18.2% and 8.4%, respectively. These patients often exhibited injuries to greater than four AIS body regions, characteristic of severe blast polytrauma. Considering attack setting, Almogy *et al.* considered buses (closed space), semi-confined spaces (*e.g.* restaurants) and open spaces. No significant difference in the average number of severely injured ($ISS \geq 16$) casualties per attack was reported, though patients injured in open spaces exhibited more penetrating trauma than PBI. This is likely due to the fact the blast wave decays rapidly in open air. Nonetheless, incidence of BLI did not differ significantly between the attack settings. Bala *et al.* concentrated upon the pattern of abdominal trauma observed in three categories of trauma: blunt, gunshot wound (GSW) and blast. It was found that median ISS values for the blast group were 17% and 89% higher than the blunt and GSW groups, respectively. Bowel injury was identified in 71.4% of blast victims, showing that blast causes a unique pat-

tern of injury, especially to hollow viscera. Bala *et al.* observe that the intra-abdominal injury pattern is similar to that of the GSW group, which is characterised by localised but severe trauma. As a result, it is concluded that shrapnel impact is the leading cause of abdominal injury.

Conversely, a study of the injury patterns from the Madrid train bombings in March 2004 by Turégano-Fuentes *et al.* suggested that 67% of bowel perforations were primary blast injuries due to lack of penetrating trauma. Though abdominal injury affected only 5% of survivors, these injuries were the most life-threatening and required major surgical intervention. A similarly high incidence of PBI was found by Katz *et al.* for a bus explosion in Israel (1988), where 38% and 14% of survivors exhibited blast lung and abdominal injuries, respectively [54]. Katz *et al.* attribute the prevalence of PBI in this incident to the particularly high pressure wave amplitude (3.8–5.2 bar) and prolonged duration (2–3 ms), each due to the confined environment. Bala *et al.* also suggest that the differences in cause of abdominal injury between the modern Israel experience and these other experiences may be due to the augmentation of IEDs in Israel with substantial shrapnel [49].

The findings of the Madrid experience agree with a review of clinical reports of abdominal PBI by Owers *et al.*. Over nine clinical reports reporting abdominal PBI to 1040 hospitalised survivors, PBI incidence varied from 1.3% to 33%. Average incidence in open and closed environments was 5.6% and 6.7%, respectively. The most commonly affected abdominal organs were the hollow viscera of the terminal ileum and caecum, agreeing with the findings of the Israel experience. Owers *et al.* note that abdominal PBI is uncommon but also incompletely reported in the literature, largely due to lack of reliable external markers of injury. In the civilian setting, where there is little personal protection, secondary abdominal trauma and pulmonary PBI are more prevalent clinical problems.

In the military case, Mellor & Cooper provided early evidence to suggest that ballistic

vests do not offer protection against PBI [55]. They carried out a study of blast injuries inflicted upon 828 personnel in Northern Ireland during 1970-84. Due to the nature of the conflict, the magnitude of the explosions and distance to the targets could be estimated, enabling the BOP to be estimated from empirical data. Resulting observations for four victim groups exposed to differing blast amplitudes are summarised in Table 2.2.

Group	n	BOP (bar)	Outcome
1	255	<1.4	Sufficient to cause TMP
2	135	1.4-3.4	Minority of casualties with BLI
3	200	3.4-5.5	Majority of casualties with BLI
4	238	>5.5	Severe BLI with majority dead

Table 2.2: Outcomes for military populations of size n sustaining blast loading at varying incident peak overpressure (BOP). TMP: Tympanic Membrane Perforation, BLI: Blast Lung Injury. Adapted from [55].

Of the 216 killed, 90% were equipped with body armour while PBI was found to be a significant cause of morbidity and mortality. Indeed, 17% of those with significant pulmonary damage exhibited no external signs of trauma, enabling secondary or tertiary trauma to be eliminated as possible causes. The implication was that the ballistic vests were not protecting the wearer from the blast wave. As is consistent with the civilian case, TMP was the most common injury, occurring in approximately 50% of survivors and likely associated with the fact that fewer than 20% were equipped with helmets. It is also noteworthy that severe intra-abdominal trauma requiring laparotomy, while occurring for only 5 casualties, all took place in vehicles, suggestive of the enhanced peak pressures arising in enclosed environments due to reflections of the blast wave. Similarly, targets in a cluttered urban environment or in close proximity to a wall are, in general, associated with greater injury severity due to reflected pressure waves and waves impinging on the target from multiple directions prolonging the loading duration [21].

Intra-abdominal rupture does not occur in isolation and is seen as part of a poly-trauma along with BLI and tertiary injuries. A similar pattern of injury was observed by Gondunsky & Reiter in 2004 during the Iraq War for vehicular environment injuries inflicted by roadside IEDs [56]. The importance of mitigating PBI was further highlighted in a review by Smith of patients presenting BLI to UK military field hospitals during

2003-2009 [57]. Due to the fact that in an open environment, the peak BOP decays rapidly as the inverse cube of radial distance (see Eq. (2.6)), in such a context, secondary missiles (*e.g.* shrapnel) would be expected to be the principal cause of injuries. However, due to the increasing effectiveness of body armour for defeating fragment impact, Smith observed that casualties were increasingly surviving closer to the source of the explosion and this was correlated with an increased incidence of PBI. This can be speculated to be a consequence of increased diagnosis and reporting of PBI for casualties that may no longer exhibit life-threatening penetrating trauma. As is characteristic of blast, in the case of casualties presenting both primary and secondary polytrauma, penetrating injuries are more readily diagnosed and triaged. While clinicians are trained to exercise high suspicion of primary injury, casualties succumbing to secondary trauma may negate a subsequent investigation of PBI where there is a likelihood of such injuries existing. An additional factor that can be related to increased reporting of PBI is the fact that in recent conflicts, the ratio of gunshot wounds to blast injuries reduced, reflecting increased use of IEDs. Of the 1678 casualties examined by Smith's investigation, 14% sustained thoracic injuries, with 50% of those presenting BLI with an average ISS of 52.6. Only 34% of these victims ultimately survived. Over the time period, it was found that the clinical burden of blast lung increased from 7.3% in Iraq to 11% in Afghanistan.

An analogous review of this casualty population carried out by Stevenson instead focussed on head-related injuries found that there was no correlation between TMP and blast lung, suggesting that TMP was not an indicator of underlying blast lung [58]. This was reinforced by Scott *et al.* who found that 6-11% of casualties presented BLI while over 50% presented TMP [59]. These results may have a physical basis in the fact that BLI occurs at a higher pressure threshold than TMP. While TMP has been observed to occur at 1 bar overpressure [60], BLI occurs above approximately 5 bar [50]. Thus, BLI would not be expected to be correlated with TMP. Moreover, it can be speculated that this observation is fundamentally due to the mechanisms for the two injuries being distinct. While TMP occurs on the basis of tensile failure of a membrane, BLI has been considered to occur as a result of haemorrhage at the air interfaces within the lung or,

potentially, oscillation of the thorax on a longer timescale [61, 62]. Conversely, Traumatic Brain Injury (TBI) occurred in 50% of casualties and was found to be correlated with the occurrence of TMP. Given that almost all casualties were equipped with combat helmets, Stevenson concludes that such protective equipment is ineffective against BOP. Given the ubiquity of combat helmets for all personnel in the field [2], this finding is robust due to the fact that the targets will have been exposed to a wide range of loading conditions in a variety of environments. In the context of similar findings for the thorax with respect to ballistic plates, this finding reinforces the observation that the addition of a high impedance layer to the protected structure may act to couple the blast wave more effectively, increasing damage. Finally, while the occurrence of intra-abdominal PBI was low at <5% it must be noted that such statistics may be unreliable due to the difficulty of comprehensively diagnosing and recording such injuries.

2.3.4 Estimates of Blast Lethality to Humans

The performance requirements of blast mitigation materials must be informed by the thresholds of human tolerance to blast. The question of whether probability of survival for a given blast peak pressure and duration can be predicted for a human is complex. Such studies of blast casualty criteria occurred in the post-1950 era in which German and British blast research entered open literature and testing with animal species began [53, 59]. It remains that the mechanisms for PBI at the cellular level are poorly understood, necessitating this empirical approach.

Extensive experiments were carried out by Bowen *et al.* in the 1960s on 2097 animals (13 different species) to examine the lethality of exposure to primary blast [63]. The blast waves were generated either by shock tube or explosive (TNT) detonation, replicating the Friedlander loading of the open-field scenario, with the target positioned next to a rigid reflecting surface. The outcome of an experiment was binary and based upon whether the animal survived or died within 24 h of blast exposure without medical intervention. They calculated curves expressing the probability of survival for a 70 kg human as a function

of maximum pressure and duration of the positive pressure phase. It is now common to calculate ‘iso-damage’ curves by this multivariate approach [61]. These ‘Bowen curves’ are widely used for estimating lethality in the open-field blast scenario. Variations of ambient atmospheric pressure and animal mass (m_{animal}) were scaled according to dimensional analysis as:

$$\bar{t}_+ = t_+ \left(\frac{m_{ref}}{m_{animal}} \right)^{1/3} \left(\frac{P_{atm}}{P_{sea}} \right)^{1/2}, \quad (2.7)$$

$$\bar{P} = P \left(\frac{P_{sea}}{P_{atm}} \right), \quad (2.8)$$

where the bars indicate scaled quantities, m_{ref} is the reference mass (chosen as 70 kg for a human) and P_{sea} is the atmospheric pressure at sea level. It was assumed that the lethality curves could be expressed by the functional form:

$$P = P^*(1 + at_+^{-b}), \quad (2.9)$$

where P^* , a and b are constants determined by statistical analysis. It is crucial to note that P in Eq. (2.8) is the measured peak reflected pressure. This was converted by Bowen *et al.* to the incident wave pressure using a simple analytical relationship for the case of a plane wave reflected by a wall of infinite strength. To extend results to the free-stream case, Bowen *et al.* introduced the ‘pressure dose’ concept. They assumed that for a person near a wall, the pressure dose is the reflected pressure. By contrast, for an open-field, it was taken as the sum of the incident pressure and dynamic pressure. The Bowen curves for this scenario are shown in Figure 2.7.

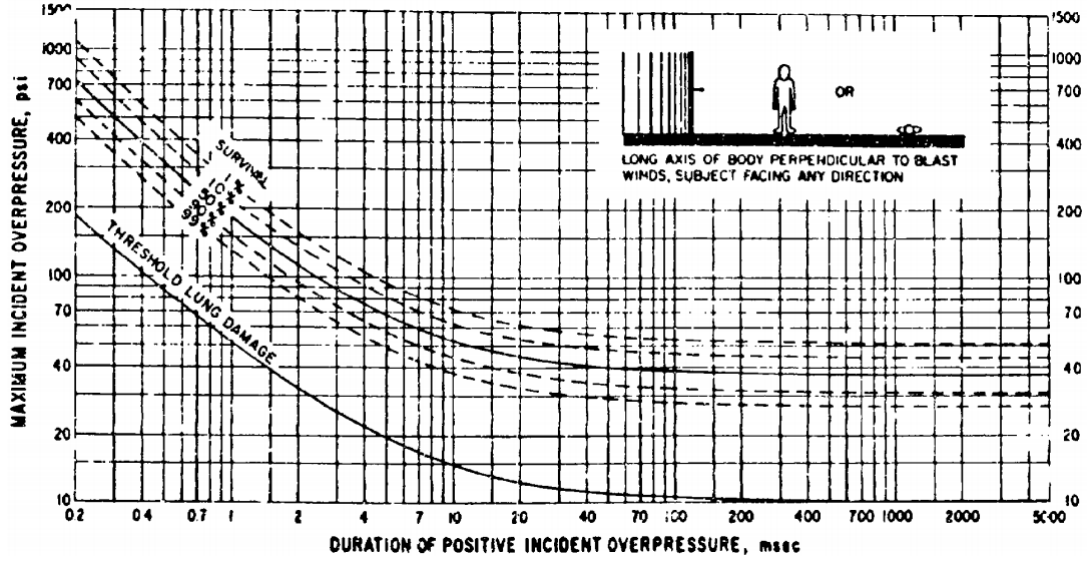


Figure 2.7: Bowen curves describing lethality for a 70 kg human in the open-field scenario. A unique curve exists for each survival probability, but 1%, 50% and 99% curves are cited conventionally. These curves are referenced as LD_1 , LD_{50} and LD_{99} curves, respectively [53]. The threshold lung damage curve was estimated as $1/5P$ of the LD_{50} curve. Reproduced from [63].

The Bowen curves were derived entirely empirically and it is noted by Bowen *et al.* that the purely mathematical extension to the free-field scenario is predicated upon tentative assumptions. As such, Bowen *et al.* quote significant uncertainty in the lethality curves. As the experiments utilised no medical intervention, this factor may contribute to an overestimation of lethality as surviving casualties on the battlefield will, in most cases, be transited to a medical centre and treated within the 24 h period, typically within 2 h [33]. For the LD_{50} case, the curve underestimates lethality by 0-30% for $t_+ < 2.8$ ms, overestimates by 0-15% for $2.8 \text{ ms} < t_+ < 100$ ms and is correct to within 1% for $t_+ > 100$ ms. An analysis by Richmond & Cooper of the pressure dose concept in the free-stream case found that for $t_+ > 6 - 10$ ms the data is in good agreement [64]. Conversely, in the $t_+ < 2$ ms regime, Bowen *et al.* underestimate the lethality in the free-field. Data has been gathered more recently in an attempt to improve upon the lethality criteria derived by Bowen *et al.*. Bass *et al.* [65] and Rafaels *et al.* [66] conducted analogous experiments on a total of 2550 large animals in both the open-field and near-wall scenarios. Significantly more large animals were considered in these studies than Bowen *et al.* and Richmond (1100 compared with 350). The major distinction from the Bowen curves is that Bass *et al.* use a different pressure dose for extending to the

free-stream case in the $t_+ < 30$ ms regime. They considered the pressure dose to be the reflected pressure from an imaginary wall behind the target (calculated from the measured incident pressure). The rationale for this was that Bass *et al.* did not find a statistical difference in lethality for animals in an open field or near a wall in the short loading duration regime. As such, the Bass curves are lower than the Bowen curves with lethality occurring at lower values of incident pressure.

An additional caveat to the Bowen curves is that the positive duration t_+ was not measured by Bowen *et al.*, but rather calculated empirically from prior experiments using Pentolite HE [67]. As a result, the Bowen *et al.* calculations are predicated upon the assumption that 1 kg Pentolite is equivalent to 1.1 kg TNT. Gruss notes that there is a statistically significant difference between the data used by Bowen *et al.* and modern TNT blast data. As such, Gruss provides a correction to the time-dependence of the Bowen curves [61].

Nonetheless, it is common for vulnerability analysts to infer incapacitation from the Bowen curves [68]. In the surveys of military casualties (see Section 2.3.3), while ears are universally recognised to be the most vulnerable biological system, they are not critical for survivability. As such, most casualty criteria are defined by the threshold for pulmonary injury, and so the Bowen curves remain the basis for many modern primary blast injury criteria [68]. In estimating the pulmonary injury criterion, there is no generally accepted quantification of military incapacitation.

A number of casualty criteria have been suggested by investigators. It is tentatively suggested by Kokinakis *et al.* that LD_{50} and LD_1 based casualty criteria underestimate the extent of casualties and the damaging effect of the threat. It is proposed that the threshold should be derived from the LD_{99} curve [53]. By contrast, another key approach was to consider instead impulse rather than BOP in the short loading duration regime. Richmond *et al.* performed shock tube and HE tests on animals analogous to Bowen *et al.* and Bass *et al.*, considering loading durations in the range of 1.5–400 ms [69]. In

canine and hircine tests, it was found that the resistance to fast loading was a factor of 4-5 times greater than for long loading duration. They identified a ‘critical duration’ (t_{crit}) of 15–20 ms that separated the short ($t_+ < t_{crit}$) and long ($t_+ > t_{crit}$) duration regimes. For short durations, the damage was dependent upon the delivered impulse primarily, which represents the momentum transfer to the body. A generally accepted interpretation of the significance of t_{crit} is that it represents the characteristic timescale for oscillation of the thorax [61, 62]. For pulses of long duration, it was found that the reflected peak pressure alone provided the best fit for the lethality data [69]. As such, in the short duration regime, Richmond *et al.* proposed a scaling of impulse corresponding to 50% lethality to generate an iso-damage curve analogous to the LD_{50} Bowen curve known as the partial impulse criteria (PI_{50}). In this case, rather than using the peak blast overpressure, the scaled impulse of the blast wave is used as the metric:

$$\bar{I} = \frac{1}{m_{animal}^{1/3}} \int_0^{t_0} P(t) dt \left(\frac{12}{P_{atm}} \right)^{1/2}, \quad (2.10)$$

where the impulse has been scaled for animal mass and atmospheric pressure (in psi). The impulse is computed up to an empirically-derived time (t_0) that is scaled by animal mass:

$$t_0 = 0.6 m_{animal}^{1/3} \left(\frac{12}{P_{atm}} \right)^{1/2}. \quad (2.11)$$

Over the range of loading durations considered by Richmond *et al.*, the scaled impulse took the near-constant value of 30 psi ms kg^{-1/3}. This is shown in the Figure 2.8 plot of the scaled impulse against time. This plot can be considered as an ‘impulse version’ of the LD_{50} Bowen curve valid for loading duration shorter than 15 ms to 20 ms.

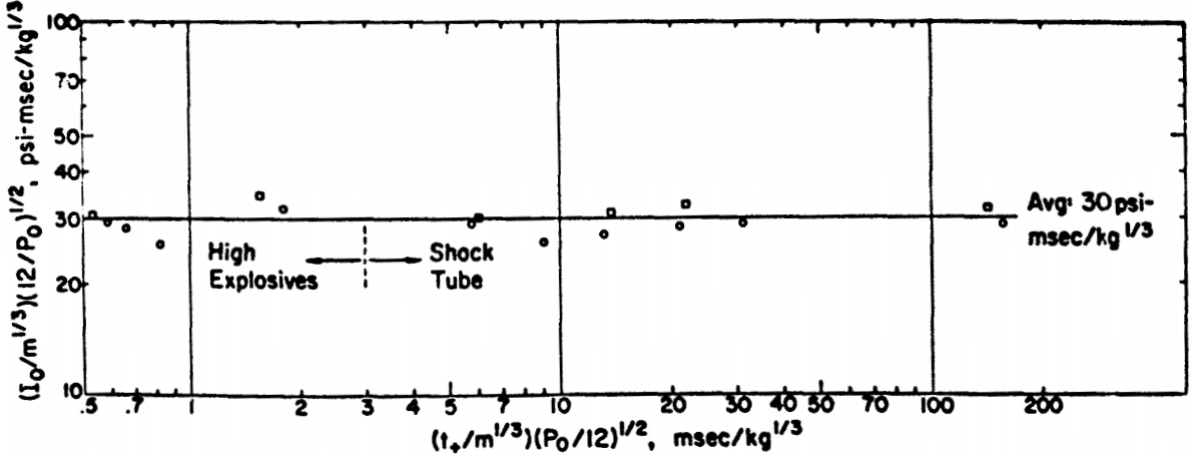


Figure 2.8: Scaled impulse PI_{50} curve as a function of scaled time t_0 derived by Richmond *et al.* The plot is populated by canine and hircine data. Reproduced from [69].

Using this scaled impulse function, Richmond proposed a $1/4 PI_{50}$ threshold for lung damage in the short duration regime. A close variation of this criterion was also proposed by Clemedson & Jonsson [70]. In a later study, Richmond *et al.* proposed an alternative criteria for military personnel tolerance to blast [71]. They no longer considered threshold blast lung injury to be necessarily incapacitating as it is often characterised by small non-fatal haemorrhages. Rather they defined incapacitation as being Combat Ineffective (CI), defined by one's inability to perform a designated task within 5 minutes. The 99% incidence of CI curve was equated to the LD_1 curve, while the CI_{50} curve was set as $1/2 LD_{50}$.

A summary of pulmonary injury threshold criteria proposed by different investigators is shown in Table 2.3. In considering blast mitigation for personal applications, these criteria offer a means of approximately quantifying whether armour materials can sufficiently attenuate the incident blast wave below critical human damage thresholds over a range of peak pressure and loading duration scenarios.

2.4 Blast Mitigation

The ongoing use of explosives worldwide through terrorism and military campaigns has led to a strong interest in blast mitigation research. There are broadly two aspects to

Pulmonary Injury (BLI) Threshold	Study
$1/4 LD_{50}$	Bowen <i>et al.</i> [63]
$1/4 PI_{50}; t_+ < t_{crit}$	Richmond <i>et al.</i> [69]
$1/5 PI_{50}; t_+ < t_{crit}$	Clemedson & Jonsson [70]
LD_{99}	Kokinakis <i>et al.</i> [53]
$2/5 CI_{50} (= 1/5 LD_{50})$	Richmond <i>et al.</i> [71]

Table 2.3: Summary of common casualty criteria for Blast Lung Injury (BLI). LD_{50} : 50% survival probability pressure-time iso-injury curve, PI_{50} : 50% survival probability impulse-time iso-injury curve, t_+ : scaled time for animal mass and atmospheric pressure, t_{crit} : characteristic time for oscillation of the human thorax, LD_{99} : 99% survival probability pressure-time iso-injury curve, CI_{50} : 50% probability of becoming Combat Ineffective (CI).

this research. The first is to understand blast loading and its interaction with complex materials and geometries. The second is it develop mitigation strategies for diminishing the damage from the shock. Much worthwhile effort has been devoted to mitigation research relevant to civil infrastructure and vehicles. In these cases, polyurea based coatings [4, 72], reinforced concrete [3], perforated steel panels [44, 73], and sandwich composites [4–12] have been widely studied analytically, computationally and experimentally.

Research relevant to personal use has been strongly influenced by clinical need and observations on the battlefield. As such, several studies have considered materials used in ballistic helmets for mitigating TBI [58, 74], and EOD suits [46], with little consideration of the role of ballistic plates in mitigating blast [28]. Widely considered mitigation materials include cellular materials, water-based materials and composites.

2.4.1 Foams

2.4.1.1 Fundamentals

Foams are cellular materials that are commonplace in nature. Early identifications of such materials were cork and biological tissue structures by Hooke [75]. Man-made materials also exist with modern techniques realising cellular forms of polymers, metals and ceramics. These foams are often light and inexpensive. On an everyday basis, these foams have broad applications in thermal insulation, acoustic insulation and structural protection. However, foams may also fulfil more critical uses for mitigation of injury in

high strain rate events such as automotive crashes and blast.

A foam is a three-dimensional interconnected network of trusses which form the edges and faces of cells [75, 76]. If the solid material the foam is comprised from (the ‘solid phase’) joins to adjacent cells at the cell edges only, leaving the cell faces open, the foam is termed ‘open-celled’. Conversely, if the cells are entirely separated with closed faces, the foam is ‘closed-celled’. An important parameter describing foams is their relative density ρ^*/ρ_s . Here, ρ^* is the bulk foam’s density (‘apparent density’) and ρ_s is the solid phase’s density. The foam’s porosity (ϕ), the ratio of the void volume to bulk volume (V^*), is then given by:

$$\phi = 1 - \frac{\rho^*}{\rho_s} = 1 - \frac{V_s}{V^*}, \quad (2.12)$$

where V_s is the solid phase volume. By convention, the asterisk denotes foam parameters. It can be seen that increasing relative density corresponds to a greater solid phase proportion in the foam and smaller pore volume [75]. Foams may be further categorised according to their mechanical properties as elastomeric, elastic-plastic or elastic-brittle, as shown in Figure 2.9.

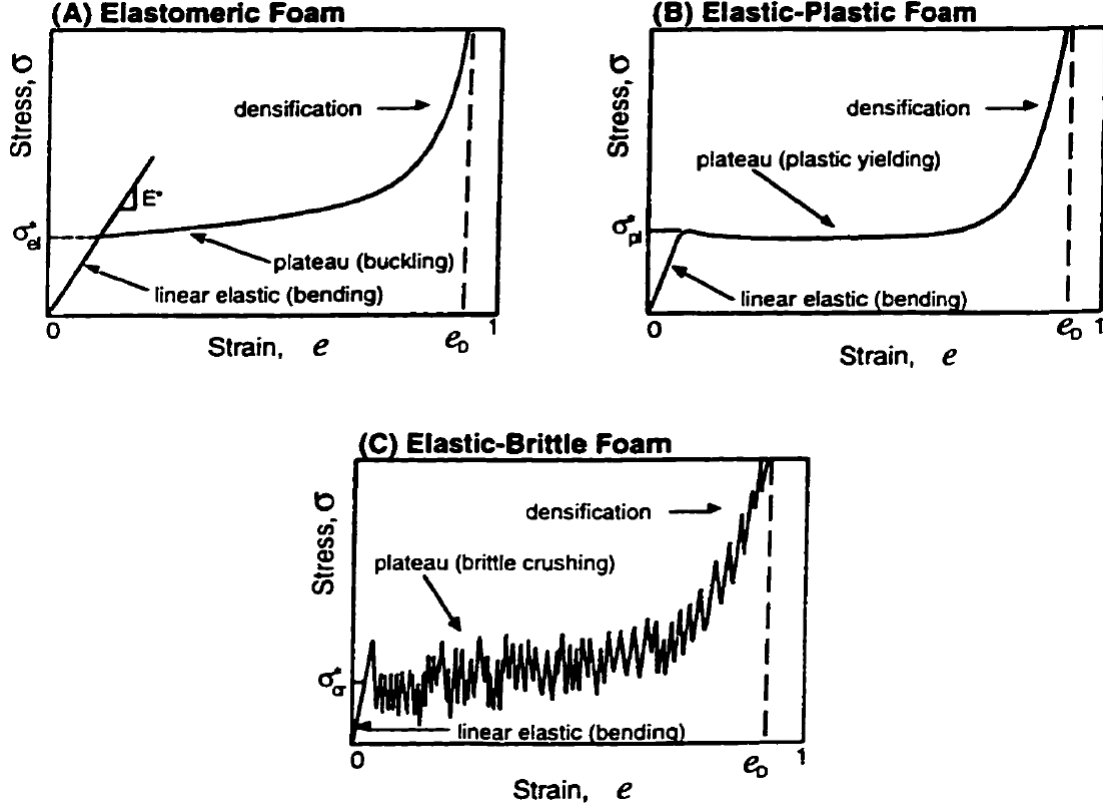


Figure 2.9: Characteristic stress-strain curves for (A) elastomeric, (B) elasto-plastic and (C) elastic-brittle foams with plateau stresses of σ_{el}^* , σ_{pl}^* and σ_{cr}^* , respectively. E^* is the elastic modulus of the foam in its linearly elastic regime, valid for small deformation. Adapted from [75].

The unique mechanical properties of foams are readily identifiable from their stress-strain relationships. Under compression, the deformation of all foams is approximately linear for small strains. Subsequently, the foam enters a plateau regime at strain ϵ_0 where the material sustains a large strain under near-constant load. This corresponds to collapse of the foam's cells and is the reason for the energy absorption capability of foams. The presence of the plateau region means that, for a given stress level, the energy absorbed per unit volume (the area under the stress-strain curve) will be higher for the foamed counterpart of the fully dense solid phase material. Upon complete collapse of the pores at strain ϵ_D , densification is achieved and, thereafter, the stress rises sharply with strain as the solid phase material is compressed. The Figure 2.10 model for an idealised foam illustrates this characteristic response of a foam under loading.

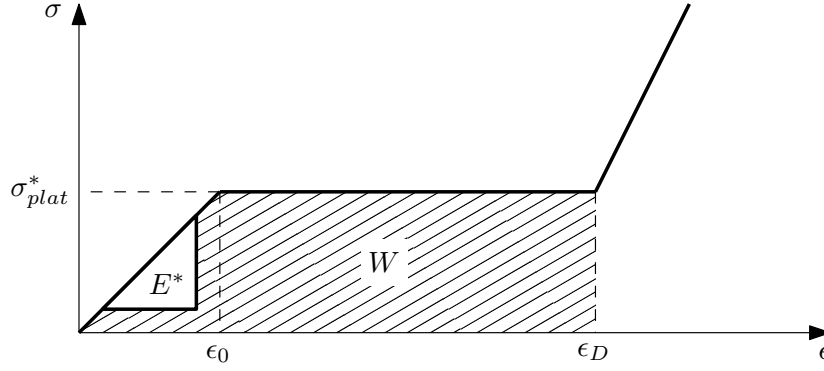


Figure 2.10: Phenomenological model of an ideal foam with plateau stress $\sigma_{plat}^* = \sigma_{el}^*, \sigma_{pl}^*, \sigma_{cr}^*$ (depending on the material type), and plateau region beginning at strain ϵ_0 and ending at the densification strain ϵ_D . The energy absorbed by the foam per unit volume up to the densification strain is denoted W .

As is apparent from Figure 2.9, the differing foam types are distinguished by the nature of the cell wall buckling *i.e.* the plateau region. For the elastomeric foam, deformation remains purely elastic and is fully recovered upon unloading. By contrast, elastic-plastic foams exhibit plastic yielding of the cell walls while elastic-brittle foams undergo brittle crushing [75]. For each of these cases, the plateau stress and energy absorbed per unit volume up to densification can be estimated from energy equivalence according to:

$$\sigma_{plat}^* = \frac{\int_{\epsilon_0}^{\epsilon_D} \sigma(\epsilon) d\epsilon}{\epsilon_D - \epsilon_0}, \quad (2.13)$$

$$W = \int_0^{\epsilon_D} \sigma(\epsilon) d\epsilon. \quad (2.14)$$

It should be noted that the plateau level is dependent upon strain rate [75]. While major foam applications include packaging and auto design, where strain rates of up to about 40 s^{-1} may be experienced, blast loading scenarios may involve strain rates of the order of 100 s^{-1} to 1000 s^{-1} [75]. Under such high-rate loading, the dynamic behaviour of foam deforming faster than its sound speed is more poorly understood and remains a highly active area of research (see Chapter 6).

While foams are widely used where lightweight structures are necessary, their application can be limited by their low strength and modulus. Syntactic foams are a class of multi-phase cellular material that can address this issue by allowing tuning of the

stress plateau necessary for energy absorption applications. Such foams consist of matrix material, in which hollow particulate are dispersed to introduce controlled porosity (see Figure 2.11).

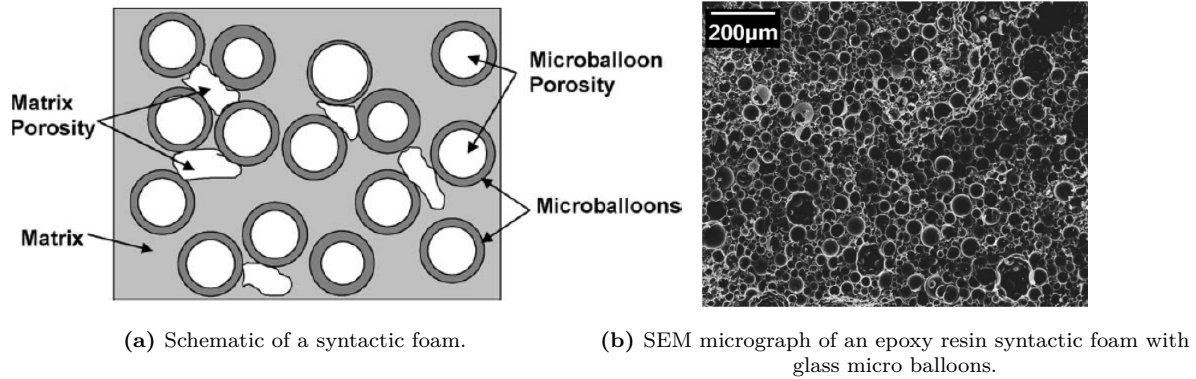


Figure 2.11: Syntactic foams are multiphase structures consisting of a matrix material and porosity in the form of hollow inclusions. Additional matrix porosity can also be present. Adapted from [77].

The inclusion of hollow particles reduces the overall density, resulting in higher specific strength that is appropriate for lightweight applications requiring high damage tolerance. Under impact, these particles enhance energy dissipation through cracking mechanisms [78]. Common matrix materials include polyurethane, silicone, rubber latex, epoxy resin and metals. Matrix materials have typical densities in the range of $1000\text{--}3000\text{ kg m}^{-3}$ [79]. Thin-walled, hollow particles may typically be carbon, polymer, ceramic or, most commonly, glass ('microballoons'). Such particles are often have diameters in the range of $10\text{--}200\text{ }\mu\text{m}$ with corresponding true particle densities in the range of $150\text{--}800\text{ kg m}^{-3}$ [79]. Most applications consider microballoon volume fraction in the range of $0.30\text{--}0.65$ [77]. A subset of microballoons that are also used in syntactic foams are 'cenospheres'. These are hollow glass microspheres that are produced as a waste product from coal burning and are comprised of aluminosilicates rather than the sodium silica of engineered microballoons [80]. In general, microballoons are found to be chemically inert and stable up to temperatures of $1100\text{ }^{\circ}\text{C}$ [80]. While syntactic foams were first manufactured in the 1960s, most research effort has taken place in the last 15 years with a focus on understanding how their mechanical, thermal and electrical properties can be effectively tailored [79]. Indeed, these properties can be effectively modified by varying parameters including the material choice, microballoon volume or mass fraction, microballoon diameter and mi-

croballoon wall thickness. As a result of this flexibility, syntactic foams are appropriate for a wide range modern applications, including acoustic attenuation, thermal insulation, structural engineering and in blast mitigation as a sandwich composite core [79].

The quasi-static and dynamic behaviour of syntactic foams have been investigated in detail by a large number of investigators both experimentally [77, 78, 81–84] and computationally [80]. Li & Muthyla studied microballoon-embedded syntactic foams with a rubber-cement matrix under low-velocity ($< 4 \text{ m s}^{-1}$) impact, considered to have structural applications as a mitigation layer. While the addition of rubber to the cement matrix enhanced the material’s capacity to store elastic strain energy, a unique aspect of their study was an attempt to enhance the micro length-scale energy absorption by incorporating glass microfibres as a further additive. This was found to improve energy dissipation under impact through mechanisms such as fibre pull-out from the matrix material. Fahr investigated the change of mechanical properties to silicone resin following the addition of microballoons in different mass percentages (10%, 20% and 30%), reducing the overall silicone density by up to 50% [84]. Under quasi-static compression, microballoons were found to increase the plateau stress, and hence energy dissipation, compared to the monolithic material. A greater mass or volume fraction of particles also corresponded to a longer duration plateau. Fahr also conducted dynamic experiments with sandwich structures under shock tube blast loading. Using steel facesheets with a corrugated steel core, Fahr considered samples both with and without the silicone syntactic foam filler. With the foam, rear facesheet deflection was decreased by an average of 27% over different incident pressures, indicative of a stiffer response. Studies carried out by Goel *et al.* concentrated upon the sensitivity of aluminium syntactic foam behaviour to strain rate (from 0.001 s^{-1} up to $> 500 \text{ s}^{-1}$), accessing the high-rate regime with an SHPB [81]. At higher rate, the plateau region became less well-defined with a smoother transition from the elastic to densification regimes. It was also observed that the plateau stress and peak stress (immediately prior to the plateau region) increased with strain rate. Similarly, Pellegrino *et al.* investigated compression of polymeric (polyurethane) syntactic foams across strain rates in the range of $0.001\text{--}1100 \text{ s}^{-1}$ [83]. The syntactic

foam was found to exhibit greater strain rate dependence than monolithic polyurethane or conventional polyurethane foams with equivalent density. It is concluded that microballoons must trigger deformation mechanisms that enhance response to strain rate. Finally, unconventional syntactic foams featuring a gradient of hollow particulate density across their thickness have been investigated by Shankar *et al.* [82] and Gupta & Ricci [77]. These studies address the practical consideration that particles may settle under gravity within the matrix. Under quasi-static compression, Shankar *et al.* studied an epoxy resin syntactic foam with a variation of 44% to 26% particle volume fraction across a 25 mm thickness sample. Over that volume fraction range, the elastic modulus was found to increase linearly from 2.00 GPa to 2.47 GPa. However, Gupta & Ricci report that such graded density foams have a greater risk of premature fracture due to non-uniform stress distribution along the volume fraction gradient [77], and that there was also an abrupt drop in stress of 40-60% immediately after the peak stress, reducing energy absorption. Achieving graded density by the alternative method of varying the microballoon wall thickness rather than volume fraction, Gupta & Ricci find that these detrimental effects can be avoided [77].

2.4.1.2 Foam Shock Enhancement

While the mitigation of stress transmission by low density media is well-established for low impact velocities of the order of 10 m s^{-1} (such as in cashworthiness studies), their resistance to blast is more complex [85]. Blast interaction studies often consider the case of the foam placed against or near a rigid surface, pertinent to the use of such materials for the protection of buildings or vehicles. Typically, the sample is placed against the rigid end-wall of a shock tube in a closed or confined configuration, with pressure sensors positioned before and after the mitigation material [76, 86–94]. The principles of the shock tube are discussed in Section 2.6. An example of such a setup used by Seitz is shown in Figure 2.12 [93].

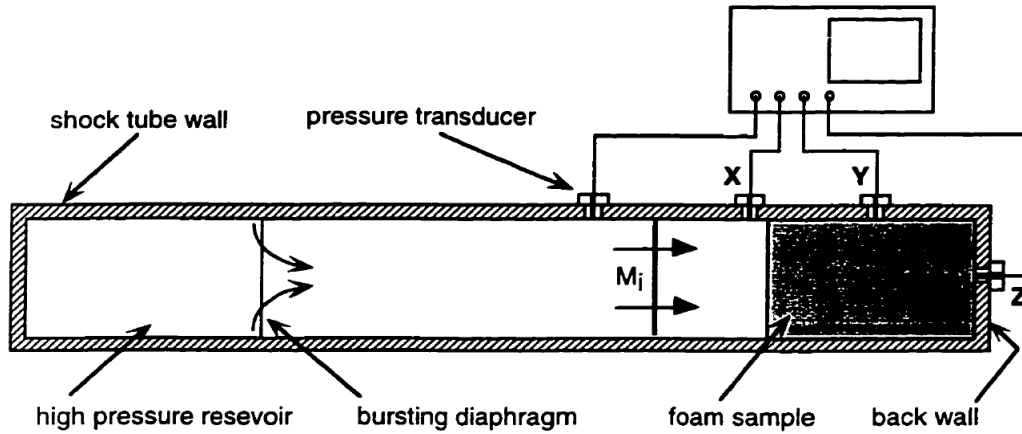
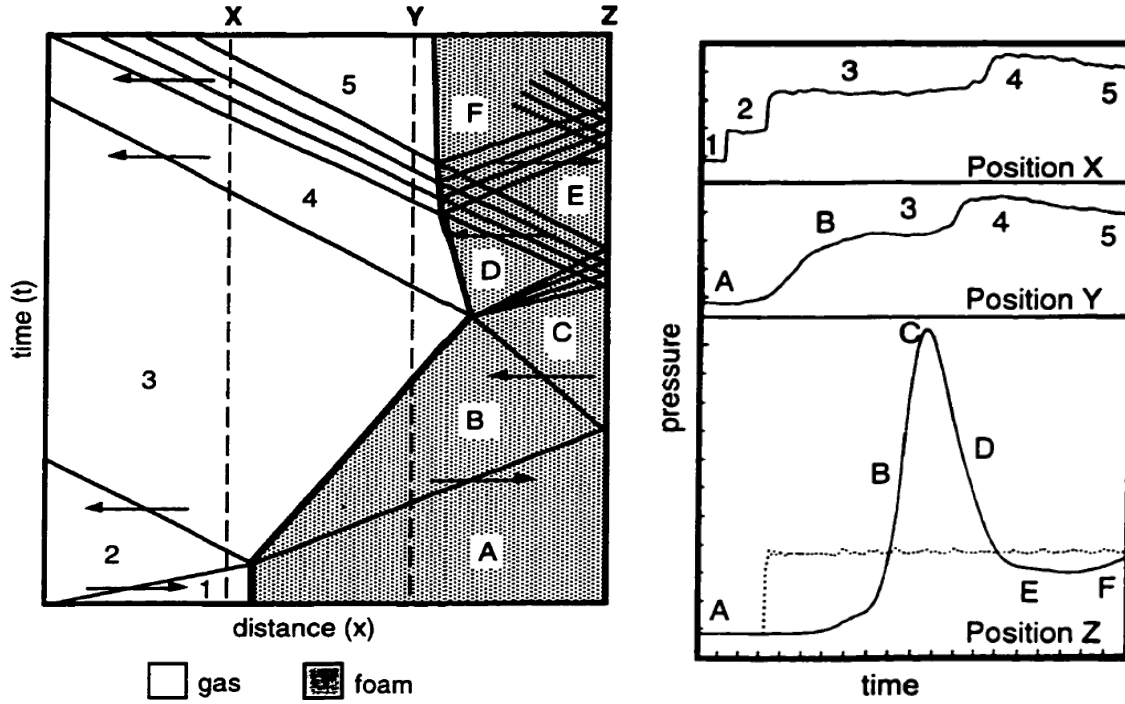


Figure 2.12: Schematic of a typical shock tube experiment characterising foam response to blast (M_i is the incident shock Mach number). The shock tube is used in a closed (‘confined’) configuration with x , y and z denoting the pressure sensor positions. Characteristic pressure histories recorded by these sensors are shown in Figure 2.13(b). Adapted from [93].

This experiment was first performed in 1970 by Monti [86]. The outcome was an early suggestion that the transmitted pressure was, in fact, amplified, challenging the intuitive notion that intervening porous matter automatically leads to shock mitigation. This ‘shock enhancement’ phenomenon has since been reproduced by many studies of blast interaction with low-density materials [76, 87–94]. On the other hand, attenuation of the blast overpressure has also been reported [95, 96].

Seitz & Skews conducted an extensive study of compressible foam interaction with shock waves [87, 97]. An often cited [98, 99] rationale offered by Seitz & Skews for the amplification effect was based upon treating the foam as a homogeneous material (solid and interstitial gas combined into a single phase) [93]. The resulting expected shock wave interaction with the foam is then represented in the Figure 2.13(a) $x-t$ diagram.



(a) $x-t$ diagram for interaction of a shock wave with foam. (b) Measured front (x), side-wall (y) and rear (z) pressure profiles. For sensor z , the solid and dashed lines represent the recorded pressure with and without foam, respectively.

Figure 2.13: Profiles of wave position and pressure for the Figure 2.12 shock tube experiment. Sensor positions x , y and z correspond to those shown in Figure 2.12. Adapted from [93].

In Figure 2.13(a), the shock propagates through the undisturbed Region 1 and impacts the front surface of the foam, compressing it while generating a reflected wave in Regions 2-3. The transmitted wave in Regions A-B reflects from the rigid rear interface in Regions B-C while the front surface moves towards the rear. Upon arrival of the reflected wave at the front free surface, a wave is transmitted back into the gas in Regions 3-4 with a rarefaction wave also being reflected back into the foam in Regions C-D. This defines the end of the compression phase as the foam begins to expand again. This rarefaction wave is continually reflected within the foam until equilibrium is reached [93].

Complementary pressure histories at the three sensor positions are shown in Figure 2.13(b). At position x , the increases of pressure due to the incident (1-2), reflected (2-3) and transmitted (3-4) waves are visible. The gradual decrease in pressure (4-5) due to the transmission of the rarefaction fan is also visible. Sensor y presents a smoothed version of this profile. The pressure amplification effect is manifest in the sensor z data.

Experimenters including Lee *et al.* [94], Gelfand *et al.* [88] and Gvozdeva *et al.* [100] attribute this phenomenon to the foam's momentum requiring a greater impulse from the rigid wall to arrest the foam's motion. Gvozdeva *et al.* also noted that increasing initial length of the foam resulted in densification being attained later and also greater peak pressure amplification [100]. Thus, for long sample length (>100 mm), the significance of this is that the plateau stress level will influence the pressure transmission through the foam. In this case, Gvozdeva *et al.* observed that the transmitted pressure profile was approximately square, indicative of densification having not taken place [100]. This is supported by the work of Ashby *et al.* [101] and Tan *et al.* [102] where foam collapse at near-constant pressure was observed as a result of the plateau region. In the case of metal foams, this plateau pressure was observed to increase in magnitude from the order of 1–100 MPa when moving from quasi-static to high-rate loading. In addition to this strain rate dependence, the collapse pressure is also a function of the foam's density. Hence, density and thickness are the major design parameters for foams when applied to specific applications.

An additional rationale for the amplification effect has been offered by experimenters on the basis of simple impedance considerations [98, 99]. When the pressure wave encounters an interface of greater impedance, the transmitted stress is enhanced (see Section 2.4.3). As such for the air-foam-steel case of the shock tube, there are two interfaces that act to amplify the pressure, rather than just one interface (air-steel) when there is no intervening foam. In support of this, Seitz & Skews report no amplification effect following the introduction of a small air gap between the foam and shock tube end-wall [87, 97].

The nature of shock enhancement by cellular materials remains poorly understood. It is known that the impedance rationale alone does not fully describe the mechanism. Indeed, it has been shown that the gas percolation dynamics in the foam are significant, and hence the single-phase homogeneous treatment of foams may be an oversimplification [99, 103, 104]. Skews hypothesised that early in the compression, a near-impenetrable,

piston-like, region of foam was established that drove the gas in front of it [104]. The pressure amplification arises from the hot high-pressure gas, constrained between the rigid wall and advancing ‘piston’, being forcibly ejected through the compressed foam. These experimental hypotheses have been supported by two-phase computational simulations carried out by Baer & Olim [105], as well as Kitagawa *et al.* [106], though Seitz & Skews note significant *ad hoc* modifications to the parameters of their models were required to obtain experimental agreement [87]. Analytical work related to the shock enhancement phenomenon has been carried out by Li & Jones [10], Jones [11], Karagiozova & Jones [12] and Jiang & Olson [13]. Several investigators have examined the effect using finite-element modelling: [1, 6-8, 14]. These modelling efforts largely assume that the pressure acting on the foam surface is equivalent to the loading profile, though the actual pressure will also depend upon the dynamic response of the surface. Mazor *et al.* [15] and Ben-Dor *et al.* [16] developed an analytical model to account for this in the case of a planar shock front colliding head-on with an open-cell polyurethane foam. It was predicted by this model that the pressure developed at the end-wall would be amplified. However, the model did not reproduce the empirical observations of the peak pressure amplification increasing with initial foam thickness.

2.4.2 Water

As an alternative to cellular material solutions, water has also demonstrated effectiveness for mitigating the effects of an explosion with experimental studies concentrating on water sprays [107] and water sheets [108–110]. Sprays attenuate the pressure wave by dissipating the shock energy through break up of larger droplets into a finer mist (‘atomisation’) while also offering the advantage of quenching fire and diluting the concentration of detonation products in an enclosed environment. In considering personal applications, bulk water has been shown to effectively reduce BOP. A study by Chen *et al.* showed that body armour containing water-filled tubes could reduce BOP by 97% with rapid evacuation of the water lateral to the blast loading axis effectively redirecting the shock energy [109],

though the impracticality of such a solution was acknowledged. Similarly high values were found by Son *et al.* for unconfined 3 mm thick water sheets loaded by a shock tube, with BOP and impulse reduced by 82% and 75% respectively [110].

2.4.3 Armour

2.4.3.1 Fundamentals

Armour can be broadly categorised as passive or reactive [2]. Passive armour defeats the impinging blast wave or projectile with the material properties alone. In contrast, reactive armour has a kinetic response such that it can enhance its disruption of the external stimulus. A further distinction is whether the armour is absorbing or disrupting. An absorber is intended to effectively absorb kinetic energy by plastic deformation, while a disruptor is typically made of a high-strength material (high-hardness steel or ceramic) such that impinging projectiles are blunted and eroded, ejecting fragments radially. In blast, both absorption capability for the primary blast wave and disruption capability for subsequent fragment impact are required.

Lightweight armour is desirable. Often, complete protective coverage of the body or a vehicle is not possible since the armour system becomes impractically bulky and hinders mobility. As such, armour distribution must be considered with the most vital areas of the body, such as the thorax, being high-priority while the extremities are relatively lower priority. The second consideration in the disposition of armour is a probabilistic one; Directional Probability Variations (DPVs) are used to assess the areas of the protected system that are most likely to be attacked [2].

The principles of wave transmission through materials must be considered in the design of armour. A blast wave impinging upon a target will rapidly deposit a large amount of energy in the structure. The objective of armour is to protect the underlying structure by ensuring the transmitted stress, impulse, peak pressure and loading duration

remain below critical damage thresholds for the target (see Section 2.3.4) [111]. It is rarely possible to fully attenuate a blast wave. This transforms the nature of blast mitigation into an optimisation problem where the armour must be tailored to the structure it is intended to protect. A crucial caveat to this is that the magnitude, duration and direction of the loading cannot be predicted precisely.

The impulse (I_0) of an incident blast wave is responsible for the kinetic energy transferred to the structure and is given by:

$$I_0 = \int_0^\infty P(t) dt. \quad (2.15)$$

Whether it is more important to attenuate the peak blast pressure or reduce the impulse depends upon the characteristics of the protected structure. An early realisation of this differentiation was in 1943 by Holbourn in application to head injuries [112].

A fundamental principle of wave interaction with materials is the effect of acoustic impedance (Z) mismatch upon the balance of wave transmission and reflection. For a normally incident stress wave propagating from medium A to B with impedances Z_A and Z_B , respectively, the transmitted stress (σ_t) is related to the incident stress (σ_i) by:

$$\sigma_t = \frac{2Z_B}{Z_A + Z_B} \sigma_i. \quad (2.16)$$

Thus, the contrast in impedance of the materials dictates the transmission characteristics. The impedance of a material is given by $Z = \sqrt{E\rho}$ where E is the elastic modulus and ρ is the density. If the two materials are considered to be armour and protected system, respectively, it can be seen from (2.16) that employing armour of higher impedance will act to limit the transmitted stress.

2.4.3.2 Monolithic Plates

Early armour design, such as during World War II, focussed upon monolithic armour where added blast resistance was achieved by simply employing a thicker plate. This principle can be seen as a consequence of Eq. (2.16), where a simplistic rigid-body analysis with zero energy dissipation implies that the transmitted impulse (I_t^{Max}) to material B is given by:

$$I_t^{Max} = \frac{2(m_B/m_A)}{(m_B/m_A) + 1} I_0, \quad (2.17)$$

where m_A and m_B are the masses of the two materials. Hence, massive armour ($m_A \gg m_B$) will mitigate the impulse [111]. In this rigid-body approximation, I_t^{Max} represents the maximum possible transmitted impulse to the material. The applicability of this rigid-body treatment must be assessed by accounting for the nature of the fluid. Thus, the interaction between the material and fluid medium must be more accurately described to determine the transmitted impulse (see Section 2.4.3.3).

2.4.3.3 Fluid Structure Interaction

The coupling of the blast wave into the structure is described by Taylor's Fluid Structure Interaction (FSI) theory [113]. Central to the one-dimensional FSI is that the uniform pressure exerted upon the front of the material is a superposition of the contributions from the incident free-field wave (P_I), reflected wave if the panel were rigid (P_{R1}), and rarefaction wave (P_{R2}) due to fact that the panel is accelerated by the fluid in reality [5]. Thus, $P(t) = P_I(t) + P_{R1}(t) + P_{R2}(t)$, where the incident pressure wave P_I may be given by the Eq. (2.5) Friedlander waveform for an open-air blast. Applying the initial conditions that the material has zero displacement and velocity, it can be shown that the transmitted impulse is given by:

$$I_t = \zeta I_t^{Max}, \quad (2.18)$$

where ζ is defined by:

$$\zeta = \psi^{-\psi/(\psi-1)}. \quad (2.19)$$

Here, the non-dimensional FSI parameter is defined as: $\psi = \rho_0 c_0 b_s / m_f$, where ρ_0 is the density of the fluid and m_f is the areal density of the material facing the incident wave [5]. Eq. (2.19) is plotted in Figure 2.14.

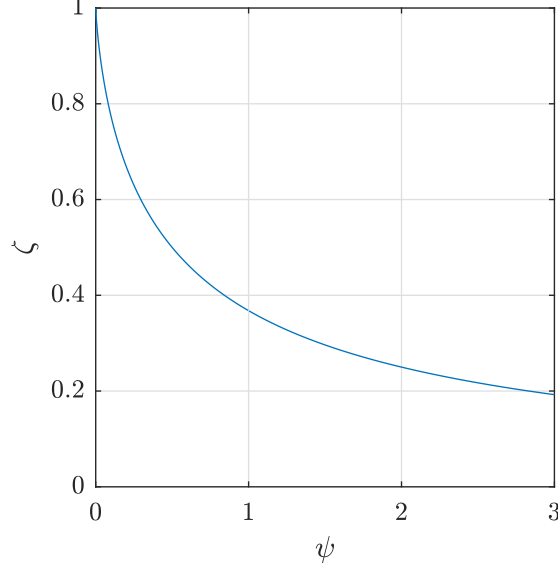


Figure 2.14: ζ as a function of the FSI parameter, ψ . ζ is the ratio of the transmitted impulse to that for a perfectly rigid structure, taking values from 0 to 1.

For an example air-blast where $\rho_0 = 1.24 \text{ kg m}^{-3}$, $c_0 = 330 \text{ m s}^{-1}$, $b_s = 0.1 \text{ ms}$ and $m_f = 8.03 \text{ kg m}^{-2}$ for 1 mm thick stainless steel, ψ takes the small value of $\psi = 0.0052$. The interpretation of this is that the significant change in impedance from air to the material results in close to complete transmission of the incident impulse *i.e.* $\zeta \approx 1$ [5]. Thus, mitigation of air-blast must rely upon efficient dissipation of the deposited energy by plastic deformation of the structure or redirection of the energy.

2.4.3.4 Ballistic Plates

Advances in ballistic protection have enabled modern armour plates, commonly utilising Kevlar® or Dyneema® fibres, to effectively protect the wearer from penetrating threats. However, there is a scarcity of research directly addressing the ability of such plates to attenuate blast loading. Wood *et al.* investigated this by considering the blast mitigation performance of both soft National Institute of Justice (NIJ) Level-2 and hard NIJ Level-4 ballistic vests attached to a rigid human surrogate and loaded with a shock tube [28].

Their setup is shown in Figure 2.15.

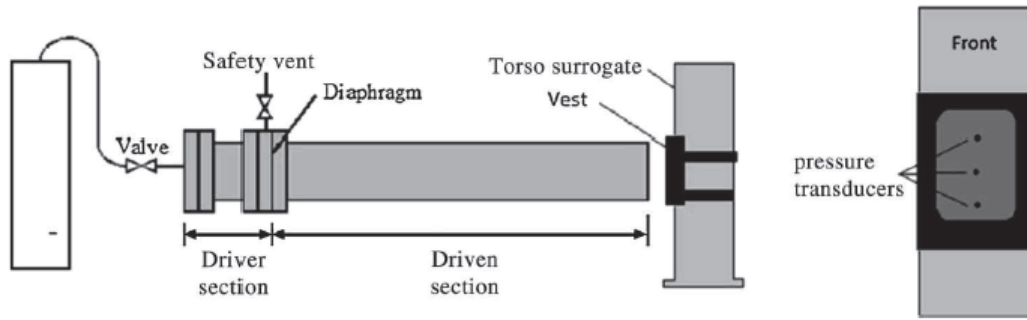


Figure 2.15: Schematic of the blast mitigation setup used by Wood *et al.* Peak incident blast overpressure (BOP) ranged from 210–3140 kPa with positive-phase loading durations in the range of 0.5–2.0 ms. The sample vest was attached to a non-deforming steel pipe and the reflected pressure measured behind the vest compared to the loading incident pressure from the shock tube output. Reproduced from [28].

Wood *et al.* reported that at the highest incident pressure, significant BOP reductions by factors of 14.2 and 56.8 were observed for the soft and hard vests respectively, concluding that personal body armour provide substantial protection against PBI and particularly pulmonary injury. However, it should be noted that, from impedance considerations, the attachment of the plate to a surrogate with substantially greater impedance than a human (steel compared to the thoracic wall), will lead to a significant underestimation of the reflected pressure measured by the pressure transducers behind the ballistic plate. Indeed, there is evidence from clinical observations that they are ineffective against the direct effects of a blast wave, as discussed in Section 2.3.3. In addition to the observations that casualties equipped with body armour and no external signs of penetrating trauma suffered lethal PBI [55], laboratory studies have also been carried out. An animal model study by Cooper *et al.* aimed to investigate this by exposing murine models to blast loading (*in vivo*) and investigating the mitigation effect of polyurethane foams and Kevlar[®] applied to the thorax [114]. All tested foams acted to increase the severity of lung injury with mortality increasing from 0 to 33% compared to the unprotected baseline. No significant ($p < 0.05$) reduction of injury severity was found for Kevlar[®] thoracic coverings. However, the combination of the two materials, with Kevlar[®] backed by the foam was highly effective, eliminating the occurrence of BLI. From impedance considerations (see Eq. (2.16)), it can be seen that the combination of low impedance foam with a high

impedance facesheet effectively decouples the stress wave at the facesheet-foam interface.

This effect was realised by adapting EOD suits with the addition of foam backing to the ballistic plates in an effort to maximise the impedance difference ($Z_A \gg Z_B$) [46]. However, this simple acoustic transmission model does not account for the thickness of the materials or their mass, but only the interface. Indeed, it has been observed that increasing the mass of the high impedance layer and the foam thickness led to higher survivability [115]. Without the foam layer, the plate would be accelerated by the blast wave and result in an impact injury to the wearer. Conversely, without the plate, Cooper had shown that foam alone increases injury severity, while the effect of foam shock enhancement can further amplify this (see Section 2.4.1.2) [92, 114]. The foam-backed plate solution will ideally bring the plate to rest before it is fully compressed. In this scenario, the transmitted stress to the thorax is approximately the dynamic crush strength of the foam, its plateau stress, which can be engineered to be below critical damage thresholds (see Section 2.3.4). In order to modify a ballistic plate in this way to provide blast protection, the desire to satisfy $Z_A \gg Z_B$ must be balanced with the need to avoid densification, where the transmitted stress to the body will rapidly increase (see Figure 2.9). As such, Wadley *et al.* developed an analytical model that relates the minimum foam thickness to the ballistic plate's momentum [116]:

$$T_{min} = \frac{M_p^2}{2m_f\sigma_{pl}^*\epsilon_D}, \quad (2.20)$$

where T_{min} is the minimum foam thickness below which densification occurs, M_p is the momentum per unit area of the ballistic plate and m_f is the areal density of the ballistic plate. The foam is idealised with a step function between the plateau stress and densification at the densification strain ϵ_D . Wadley *et al.* note that the cellular material can be of any type provided that a plateau stress exists. As such, accurate determination of the mechanical properties of the foam relevant to the loading conditions under blast may allow appropriate foam backing material and layer length to be predicted.

2.5 Split-Hopkinson Pressure Bar

Fundamentals

The Split-Hopkinson Pressure Bar (SHPB) is a widely used platform for carrying out high strain rate mechanical characterisation of materials under compression tension and shear (conventionally compression). The foundation of contemporary advanced SHPB methods is the development of the Hopkinson bar technique as a result of experiments carried out by John [117] and Bertram [118] Hopkinson in 1872 and 1905, respectively. Later in 1949, Kolsky applied the technique to mechanically characterise engineering materials [119]. The modern SHPB method demands careful consideration of factors including the sample's geometry, inertia and friction, as well as the bars' dimensions, material and calibration [120].

2.5.1 Operation

The conventional setup for a compression SHPB is shown in Figure 2.16.

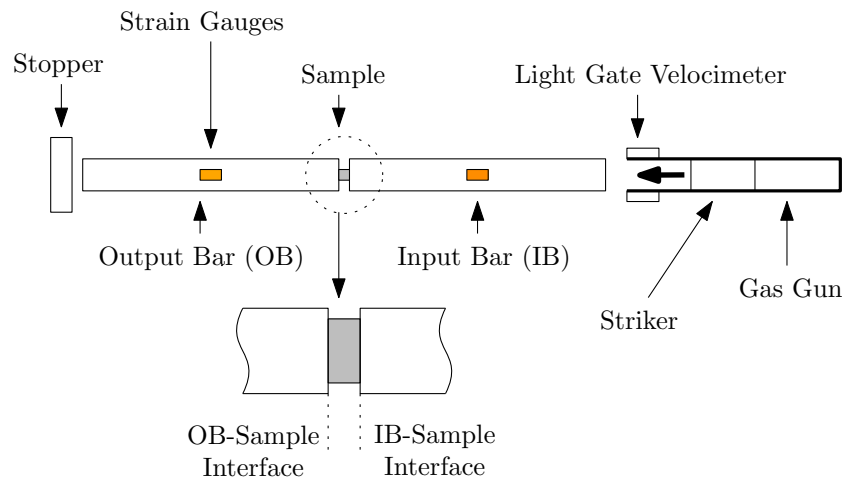


Figure 2.16: Schematic of a Split-Hopkinson Pressure Bar (SHPB). IB: Input Bar, OB: Output Bar.

The system is comprised of long, thin bars of equal diameter. In a traditional experiment, the sample is positioned between the Input Bar (IB) and Output Bar (OB). The bar ends are machined flat to ensure planar and parallel boundary conditions. Both

bars are typically instrumented with strain gauges, often stationed at the mid-point of each bar. A striker is propelled towards the input bar (typically by a gas gun system), launching a compressive stress wave on impact (‘incident wave’) that propagates towards the sample interface. The loading duration associated with this pulse (T_L) is related to the striker length (L) and elastic wave speed of the striker material (C_{st}) by:

$$T_L = \frac{2L}{C_{st}}. \quad (2.21)$$

The waveform of such a loading pulse is approximately rectangular in the stress-time domain while the amplitude scales with the impact velocity of the striker (see Figure 2.18) [120]. At the IB-Sample interface, the stress wave is partially reflected back through the input bar as a longitudinal tensile wave (‘reflected wave’), and partly transmitted through the sample. The balance of this reflection and transmission is dependent upon the acoustic impedance mismatch between the sample and bar material. The reflected tensile wave is detected by the IB strain gauge as a pulse of opposite sign to the incident pulse. This phenomenon also generates the requirement that the input bar must be sufficiently long that there is no temporal overlap of the incident and reflected waves, as well as that the IB gauge cannot be stationed close to the sample. The portion of the incident wave transmitted into the sample reverberates between the IB-Sample and OB-Sample interfaces (‘ringing-up’), causing a complex superposition of compression waves to propagate along the OB [120]. This signal manifests in the OB strain gauge output as the ‘transmitted wave’. During this period, the sample undergoes strain where it may deform beyond its dynamic limit. The strain signals obtained from the gauges for the incident, reflected and transmitted waves are denoted by ε_I , ε_R and ε_T , respectively. The Lagrangian wave profiles for the system are shown in the Figure 2.17 $x-t$ diagram. Note that an additional bar following the output bar can be utilised to ensure single wave loading of the sample if sample recovery is required. Such a ‘momentum trap’ bar acts to transfer the striker energy from the system following the first compression cycle.

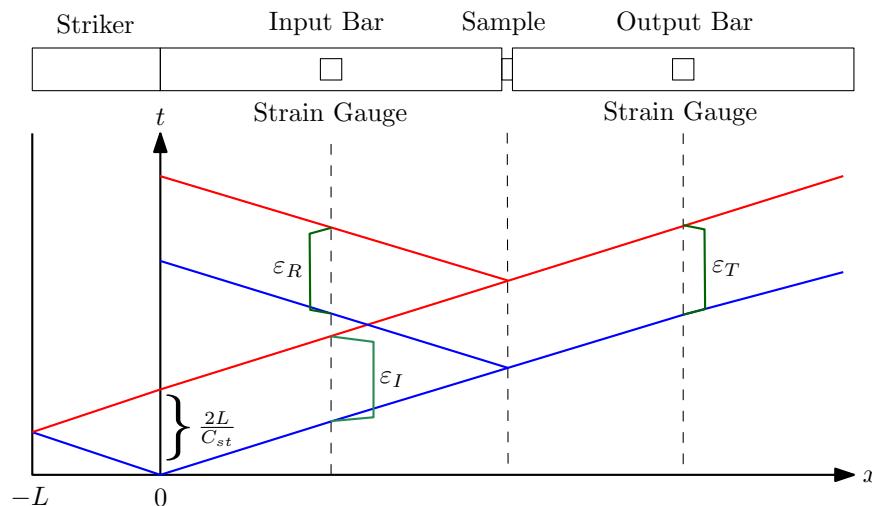


Figure 2.17: Split-Hopkinson Pressure Bar (SHPB) $x - t$ diagram illustrating the stress wave propagation in the system. Wave leading and trailing edges are shown in blue and red, respectively. Time is zeroed at the impact time of the striker with the strain gauge stations at the input and output bar mid-points. The loading duration is computed as $2L/C_{st}$, where L is the striker length and C_{st} is the longitudinal sound speed of the striker material. The striker impact introduces an incident strain signal (ε_I) that is partially reflected at the sample interface (ε_R) and partly transmitted into the output bar (ε_T). The finite thickness of the sample and the associated stress wave reverberations are not displayed. Adapted from [121].

Conventionally, the bar materials in the SHPB are identical. Thus, for an elastically deforming sample, and following calibration of the strain gauge signals, the superposition of the modulus of the transmitted and reflected signals will approximate the incident wave. An example SHPB experiment data acquisition is shown in Figure 2.18.

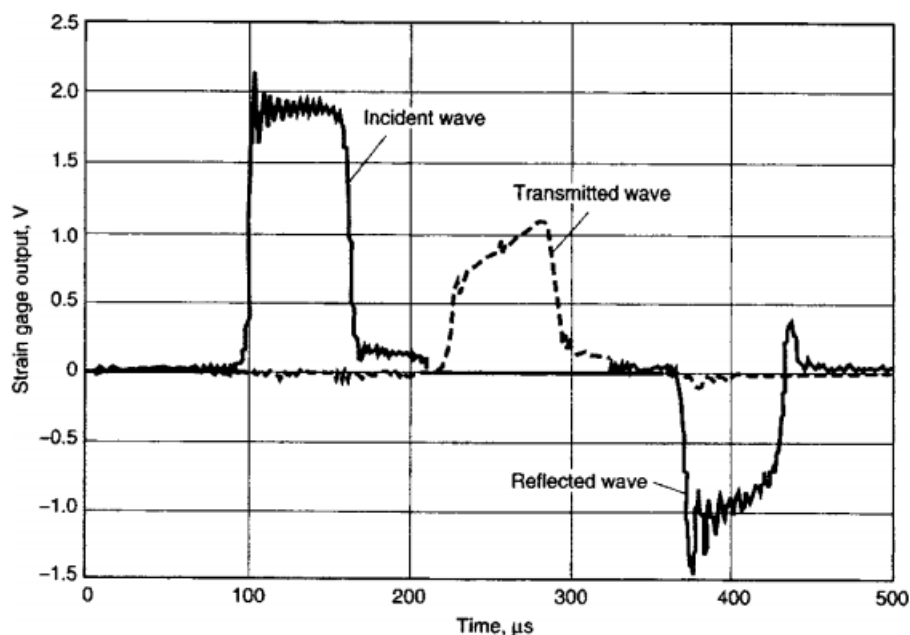


Figure 2.18: Split-Hopkinson Pressure Bar (SHPB) example strain gauge signals for a 304L stainless steel sample and maraging steel bars. Reproduced from [120].

From Figure 2.18, it can be observed that the IB waves (incident and reflected) contain high frequency components. This is attributed to Pochhammer-Chree (PC) modes, named after Pochhammer and Chree who independently provided formulations for the equation of motion of waves propagating in infinitely long, homogeneous, isotropic cylinders. A detailed review of these modes is provided by Rigby *et al.* [122]. The longitudinal stress wave generated by the striker can be represented as an infinite sum of sinusoids with differing amplitude and frequency. The phase velocity solutions to the PC equation are dependent on frequency such that low frequency components travel at near the bar elastic wave velocity while high frequency components are relatively slower. This results in dispersion of the waves that is most visible in the sharp rising and falling edges of the incident wave (see Figure 2.18) due to the high frequency components lagging behind the lower frequency components comprising the main body of the signal [122]. The transmitted wave does not exhibit such oscillations due to deformation of the sample damping out the high frequency components of the transmitted signal [120].

2.5.2 Data Reduction

Data analysis for an SHPB experiment requires that a number of bar and sample material parameters are known. These parameters are the bar Young's modulus (E_B), density (ρ_B), elastic sound speed (C_B) and diameter (D_B), while the sample dimensions must also be known. The bar longitudinal sound speed is given by the following expression:

$$C_B = \sqrt{\frac{E_B}{\rho_B}}, \quad (2.22)$$

assuming the bar response remains in the elastic regime. Given this and that the ratio of the bar length to diameter is high, the dynamic stress-strain properties of the sample can be obtained by appealing to the theory of one-dimensional elastic wave propagation. It has been shown by Davies that the assumption of a uniform uniaxial stress state across the bar cross-section is valid when the bar length exceeds the bar diameter by a factor of at least 20 [123]. Following the conventions used by Gray III [124], the IB-Sample and OB-

Sample interfaces are denoted by the subscripts 1 and 2, respectively. For simplicity, it is assumed that the bars are the same material and of uniform diameter, with a cylindrical sample of instantaneous length (L_S) in the axial (loading) direction and diameter (D_S). A schematic for this traditional analysis is shown in Figure 2.19 where u represents the displacement of a given sample-bar interface.

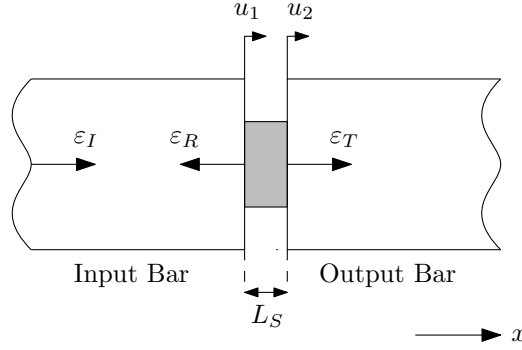


Figure 2.19: Parameters and coordinate system for modern Split-Hopkinson Pressure Bar (SHPB) analysis using one-dimensional elastic wave theory. u_1 : Input Bar displacement, u_2 : Output Bar displacement, L_S : Sample initial length, ε_I : Incident wave strain, ε_R : Reflected wave strain, ε_T : Transmitted wave strain. Adapted from [124].

The wave equation for each interface is then given by:

$$\frac{\partial^2 u}{\partial x^2} = \frac{1}{C_B^2} \frac{\partial^2 u}{\partial t^2}, \quad (2.23)$$

which can be solved by D'Alembert's method to yield solutions of the form:

$$u_1 = f(x - C_B t) + g(x + C_B t), \quad (2.24a)$$

$$u_2 = h(x - C_B t), \quad (2.24b)$$

where the functions f , g and h are arbitrary functions. Given that the one-dimensional strain is given by the derivative of u with respect to x , $\varepsilon = \partial u / \partial x \equiv u'$, the input and output bar strain functions can be expressed as:

$$\varepsilon_1 = f' + g' = \varepsilon_I + \varepsilon_R, \quad (2.25a)$$

$$\varepsilon_2 = h' = \varepsilon_T. \quad (2.25b)$$

Conversely, the average strain rate of the sample is obtained by differentiating ε with respect to time:

$$\dot{\varepsilon} = \frac{(u_1 - u_2)}{L_s}, \quad (2.26)$$

where u_1 and u_2 are the particle velocities at the IB-Sample and OB-Sample interfaces, respectively, and the effect of the sample on the stress wave propagation is neglected. Also differentiating Eq. (2.24a) and Eq. (2.24b) with respect to time:

$$\dot{u}_1 = C_B(-f' + g') = C_B(-\varepsilon_I + \varepsilon_R), \quad (2.27a)$$

$$\dot{u}_2 = C_B(-h') = -C_B\varepsilon_T, \quad (2.27b)$$

and substituting into Eq. (2.26) leads to the result that:

$$\dot{\varepsilon}(t) = \frac{C_B}{L_S}[-\varepsilon_I(t) + \varepsilon_R(t) + \varepsilon_T(t)]. \quad (2.28)$$

Thus, the sample strain is estimated by integration of Eq. (2.28):

$$\varepsilon(t) = -\frac{C_B}{L_S} \int_0^t [\varepsilon_I(t) - \varepsilon_R(t) - \varepsilon_T(t)] dt. \quad (2.29)$$

Computation of the sample stress can be achieved by consideration of the forces, F , seen by the bar ends. These are given by definition as:

$$F_1 = A_B E_B (\varepsilon_I + \varepsilon_R), \quad (2.30a)$$

$$F_2 = A_B E_B \varepsilon_T, \quad (2.30b)$$

where A_B is the bar cross-sectional area. A critical assumption in the data analysis of SHPB signals is that force equilibrium applies such that $F_1 = F_2$. Physically, there is a transient period where the sample rings up as the transmitted wave reverberates between the bar ends. Following this, it is assumed that stress equilibrium in the sample is reached. This also implies that sample-bar interface friction, as well as sample inertia

are negligible. Under these assumptions, Eq. (2.30a) and Eq. (2.30b) can be combined, yielding the result that:

$$\varepsilon_I + \varepsilon_R = \varepsilon_T. \quad (2.31)$$

This relation simplifies the system of equations for the sample average strain rate, strain and stress to the following:

$$\dot{\varepsilon}(t) = \frac{2C_B}{L_S} \varepsilon_R(t), \quad (2.32)$$

$$\varepsilon(t) = \frac{2C_B}{L_S} \int_0^t \varepsilon_R(t) dt, \quad (2.33)$$

$$\sigma(t) = \frac{A_B E_B}{A_S} \varepsilon_T(t), \quad (2.34)$$

where A_S is the sample cross-sectional area and $\sigma(t)$ is the average true stress of the specimen. The Eqs. (2.32) to (2.34) system of equations constitute a 1-wave analysis as they are derived from the Eq. (2.30b) relation for the force. A 2-wave analysis instead employs the Eq. (2.30a) force for the input bar end such that $\sigma(t) = (A_B E_B / A_S) [\varepsilon_I(t) + \varepsilon_R(t)]$. Finally, a 3-wave analysis is defined by the average of the 1- and 2-wave analyses *i.e.* $\sigma(t) = (A_B E_B / 2A_S) [\varepsilon_I(t) + \varepsilon_R(t) + \varepsilon_T(t)]$. Conventionally, a 1-wave analysis is computed.

2.5.3 Experimental Considerations & Sources of Error

This derivation of the theoretical framework for the SHPB technique informs requirements for sample design and experimental setup. Many assumptions are trivially satisfied, such as choice of a homogeneous and isotropic bar material, as well as controlling the striker impact velocity such that the bar material remains below its elastic limit.

Sample design presents greater challenges. Softer samples require low acoustic impedance (*e.g.* aluminium) bars to ensure that the transmitted wave exhibits satisfactory signal-to-noise. In such a case, the slow elastic sound speed of the sample necessitates a short sample length to enable the ringing-up period to be minimised and stress equilibrium to

be achieved. The sample characteristic time is the transit time for the compression wave, given by $t_0 = L_s/C_0$ where C_0 is the sample sound speed. A guideline for the ringing-up time required to achieve stress equilibrium is 3 times this characteristic time [120]. Practically, the force equilibrium can be directly checked by observing the equivalence of the IB-Sample and OB-Sample interface stresses *i.e.* $F_1 = F_2$.

Sample inertia may be diminished by utilising a hollow sample while lubrication of the bar ends can minimise any frictional contribution. It should be noted that the effect of bar friction becomes more significant as the sample length decreases. Guidelines suggested by Gray III [124] for minimising inertia and friction effects are to choose a sample diameter approximately 80% the bar diameter (the sample diameter cannot exceed that of the bar) and a sample length-to-diameter ratio (L_s/D_s) in the range of 0.5-1. Satisfying such requirements for soft samples, stress equilibrium may remain challenging to obtain. An additional technique to achieve this is pulse shaping of the incident wave. The time for stress equilibrium depends upon the rise time of the sample to the uniaxial stress state as it undergoes the ringing-up period. By introducing a thin layer of material on the impacted end of the input bar, the rise time of the incident wave can be extended such that it represents a ramp with a rise time more comparable to the sample. This enables stress equilibrium to be reached earlier in the experiment and also the strain rate to be more constant. In additional advantage is that the pulse shaping material acts to dampen the high frequency PC oscillations present in the incident wave's rising and falling edges (see Figure 2.18).

2.6 Shock Tube Fundamentals

The shock tube is a widely used platform for generating one-dimensional shocks of controlled amplitude and duration in the laboratory. As such, characterisation of blast mitigant performance has frequently employed shock tubes [4, 92, 125].

A conventional air-driven shock tube consists of a low-pressure section (driven gas) and

high-pressure section (driver gas) separated by a diaphragm [126]. An idealised treatment of the wave propagation in a shock tube is well-established [44, 126]. A schematic of the system with pressure, temperature and velocity profiles, as well as an x - t diagram, are shown in Figure 2.20. The pressure of the driver gas is increased to the diaphragm's burst pressure (P_4), with a corresponding increase of temperature to T_4 . The rupture of the diaphragm generates a compression wave that propagates into the low-pressure region and rapidly steepens into a shock front. Ahead of this shock in Region (1), the gas is at rest. Behind the shock in Region (2), the driver gas moves with uniform velocity. As well as generating the compression wave, the diaphragm rupture causes the driven gas to expand such that a spectrum of rarefaction waves (a rarefaction fan) propagates [126]. The head of this rarefaction fan travels at the speed of sound of the high-pressure environment while the gas ahead of it in Region (4) remains at rest. The rarefaction fan represents a gradual transition from the initial pressure P_4 to the shock pressure P_2 [44]. The tail of the rarefaction fan travels at the sound speed of the driver gas flowing into the low-pressure section [127]. Since this gas flow is conventionally supersonic, the tail extends to the right such that the fan spreads out over time [127]. The contact surface represents the interface between the driver gas and the driven gas. Since the gas behind the shock front in Region (2) has been compressed while the gas behind the rarefaction fan tail has expanded, a discontinuity of temperature exists at this contact surface [127]. Pressure and particle velocity are continuous across this interface. The driven gas between the rarefaction fan tail and contact surface is denoted by Region (3). Note that if the shock tube is used in a closed configuration, the shock is reflected from the end of the driven section with a pressure P_5 and temperature T_5 . Finally, an important experimental consideration is that the hot gas behind the shock front in Region (2) is not affected by the reflected rarefaction fan. This is achieved by careful choice of the driver section length and distance from the diaphragm to the test specimen.

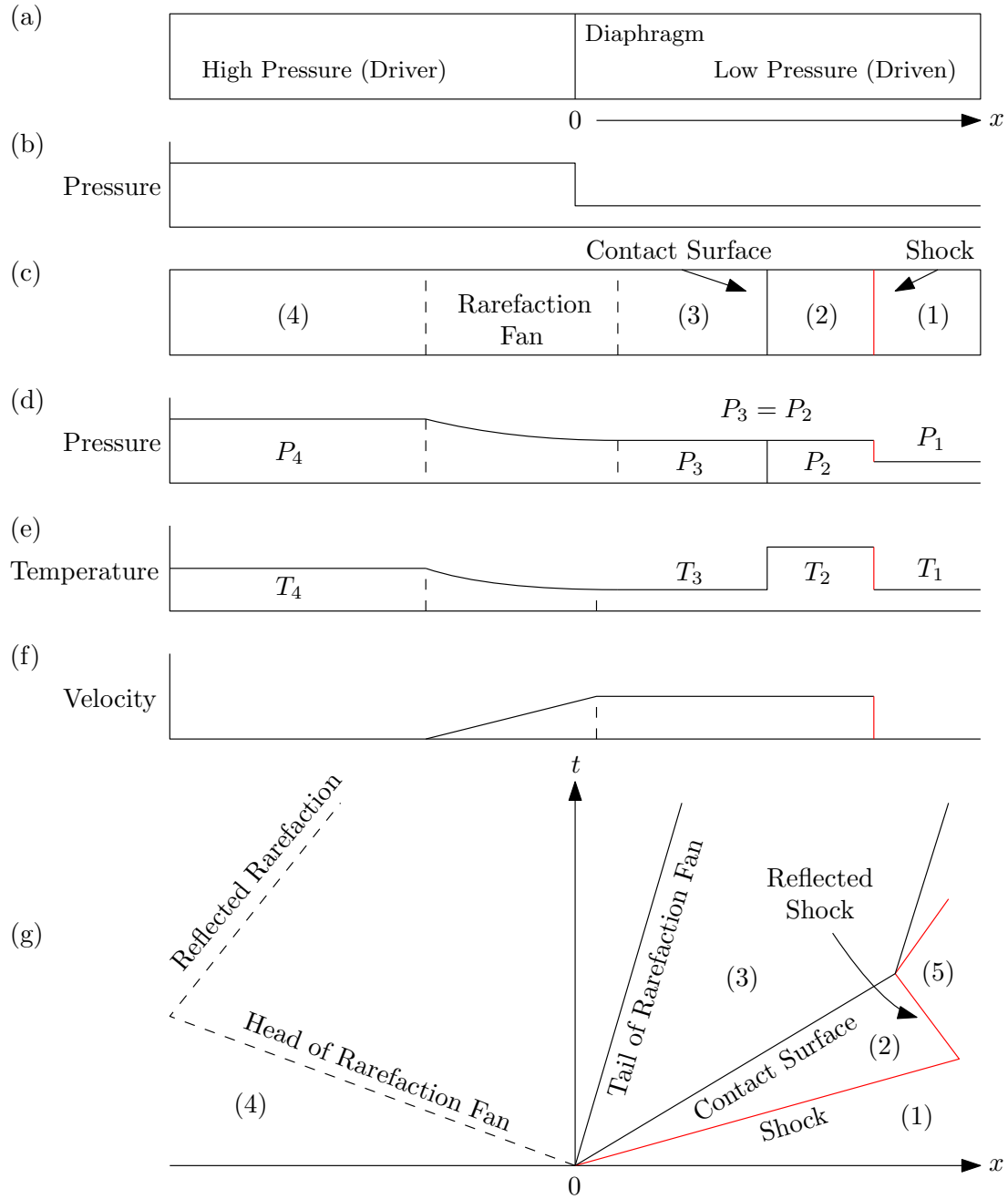


Figure 2.20: Profiles of idealised shock tube behaviour. The shock is represented in red. (a) The configuration of the shock tube. (b) The pressure profile of the shock tube prior to rupture of the diaphragm (positioned at $x = 0$). (c) Formation of shock, contact surface (interface between driver and driven gas) and rarefaction fan. Region (1) gas (at P_1 and T_1) is at rest ahead of the shock front. Region (2) represents the gas behind the shock (at P_2 and T_2) moving at constant velocity. Region (3) gas (at P_3 and T_3) lies between the contact surface and rarefaction fan tail, while Region (4) is the gas (at P_4 and T_4) at rest ahead of the rarefaction fan head. (d) Pressure profile with position. The rarefaction fan acts to smoothly transition the initial driver pressure P_4 to the shock pressure. The pressure of the fully rarefied gas is P_3 which is equal to the driven gas pressure P_2 at the contact surface. (e) Temperature profile with position. There is a discontinuity at the contact surface where the expanded and cooled driver gas interfaces with the compressed and heated driver gas. (f) Gas velocity profile with position. (g) $x-t$ diagram showing the movement of the shock, contact surface and rarefaction waves (dashed). Region (5) represents the fact that a reflected shock is generated if the shock tube is used in its closed configuration. Adapted from [127].

The Figure 2.20 construction of gas regions provides a basis for an ideal gas description of the shock tube. Developed by Gaydon and Hurle in 1963 [128], their theory provides relations between the pressure, density and temperature of the gases in the system. Applying the Rankine-Hugoniot relations (see Section 2.1) to Regions (1) and (2) across the shock front,

$$\frac{P_2}{P_1} = \frac{2\gamma M^2 - (\gamma - 1)}{\gamma + 1}, \quad (2.35)$$

$$\frac{\rho_2}{\rho_1} = \frac{M^2(\gamma + 1)}{(\gamma - 1)M^2 + 2}, \quad (2.36)$$

$$\frac{T_2}{T_1} = \frac{(\gamma M^2 - \frac{\gamma-1}{2})(\frac{M^2(\gamma-1)}{2} + 1)}{(\frac{\gamma+1}{2})^2 M^2}, \quad (2.37)$$

where the subscripts denote the gas region. Assuming the flow across the rarefaction fan is isentropic and the driver and driven sections are of constant cross-sectional area (as for a tube), the pressure ratio across the diaphragm prior to rupture is given by:

$$\frac{P_4}{P_1} = \frac{P_2}{P_1} \left(1 - \frac{(\gamma_4 - 1) \left(\frac{c_1}{c_4} \right) \left(\frac{P_2}{P_1} - 1 \right)}{\sqrt{2\gamma_1 [2\gamma + (\gamma + 1) \left(\frac{P_2}{P_1} - 1 \right)]}} \right)^{\frac{-2\gamma_4}{\gamma_4 - 1}}, \quad (2.38)$$

where γ_1 and γ_2 are the specific heat ratios for the driver and driven regions. respectively (equal for an air-driven system). From Eq. (2.38), it can be seen that higher pressure ratios across the diaphragm can be used to achieve a higher Mach number gas flow. Eqs. (2.35) to (2.37) also hold for the reflected shock such that its pressure and temperature may be expressed in terms of the incident shock Mach number:

$$\frac{P_5}{P_1} = \left(\frac{2\gamma_1 M^2 - (\gamma_1 - 1)}{\gamma_1 + 1} \right) \left(\frac{(3\gamma_1 - 1)M^2 - 2(\gamma_1 - 1)}{(\gamma_1 - 1)M^2 + 2} \right), \quad (2.39)$$

$$\frac{T_5}{T_1} = \frac{(2(\gamma_1 - 1)M^2 + 3 - \gamma_1)((3\gamma_1 - 1)M^2 - 2(\gamma_1 - 1))}{(\gamma_1 - 1)^2 M^2}. \quad (2.40)$$

In the laboratory, this theoretical framework allows the experimenter to predict the incident and reflected shock pressure (P_2 and P_5 , respectively) for a given diaphragm burst pressure.

There are several caveats to this treatment of the shock tube. The approximation to an ideal gas means that it is assumed the adiabatic index γ does not depend on temperature, which is not true of real gases. Secondly, the diaphragm does not rupture instantaneously, but rather bulges prior to failure [44]. The bulging acts to enhance mixing of the driver and driven gas such that the contact surface is, in fact, a region of finite width. This finite rupture time is also the key factor determining the shock formation distance. As a guideline, 4 tube diameters are required for a weak shock ($M < 1.7$) and 8-10 for a strong shock [44, 129]. Finally, fluid viscosity and drag in the boundary layer with the inner wall of the shock tube act to introduce turbulence, decelerating the flow [129].

2.7 Discussion

2.7.1 Review

Taking into consideration the objectives of this work (see Section 1.2) and the background here, clear areas of research interest emerge. It is instructive to consider the open-field scenario as it enables ready comparison to prior blast mitigation work and can be replicated in the laboratory using a shock tube. This platform enables a variety of loading pressure and durations to be investigated applicable to a variety of IED threats. It must be noted that under real-world circumstances, the blast environment, the charge's position within it, distance to target and charge shape are just some of the crucial factors that lead to greater loading complexity, injury pathologies and severity.

Blast injury patterns and particularly PBI are extremely complex at both microscopic and macroscopic length scales. BLI has been widely studied in the military sphere and is the basis for most casualty criteria. There is increasing PBI clinical burden since penetrating trauma is becoming more survivable due to advances in ballistic protection. There is circumstantial evidence from analyses of prior conflicts that ballistic vests may not provide protection against the blast wave and this has been supported by subsequent animal

model research. However, the combination of a hard ballistic plate with a foam backing was found to be effective against the blast. Analytical models describing the necessary thickness of such a foam backing have been proposed but remain to be experimentally validated. Whether such animal model findings can be trivially extended to the higher mass case of a human is unknown. In this area, full-scale ballistic plate blast mitigation performance has been investigated, but the use of a steel human surrogate may have led to the overestimation of the plate's blast attenuation performance; the mitigation found in this study would imply that ballistic plate's should near eliminate the occurrence of BLI on the battlefield, but this is contradicted by clinical observation.

The nature of blast injuries in the civilian environment has received less focus than the military case, so the need for PBI mitigation relative to secondary injury is not well understood. Much of the data in this area is guided by the high-quality studies produced by investigators in Israel. However, as a result the data is heavily influenced by the particular IEDs (high shrapnel mass) and attack strategies seen there.

In terms of mitigation materials, efforts related to lightweight blast mitigation solutions have primarily concentrated on cellular material solutions. Polyurethane open-cell elastomeric foam is the most widely studied material, and has been found to exhibit a shock enhancement effect in shock tube studies that acoustic impedance considerations partly, but not completely, explain. It has been suggested that gas percolation dynamics in the foam may be an important part of this phenomenon. For blast mitigation, other forms of cellular material that enable the material properties, particularly the plateau stress, to be carefully chosen may allow the transmitted stress to the body to be kept below critical damage thresholds. Syntactic foams are a promising class of material in this respect. Alternative mechanisms for blast mitigation have also been explored, with water-based studies demonstrating significant BOP reductions in excess of 75% for water sprays and sheets, though such solutions are not readily practical for personal use.

The majority of this blast mitigation work concentrates upon quantifying blast at-

tenuation from comparison of peak pressure and impulse prior and following the sample. However, little research has considered the clinical significance of these findings for the human body. Casualty criteria and appealing to the Bowen *et al.* and Bass *et al.* lethality curves may allow for the injury mitigation ability of the armour to be assessed. It must be noted here that the casualty curves and criteria are highly arbitrary and the authors acknowledge the tentativeness of their assumptions. Nonetheless, this information can provide a first-order understanding of the blast *injury* mitigation performance of body armour.

2.7.2 Research Outline

As an initial step to building an understanding of the modern nature of PBI, it is necessary to establish the clinical burden of PBI, and specifically BLI, in the civilian environment where current epidemiological understanding is more limited than the military case. Indeed, this issue has gained more interest in recent years due to the risks faced by emergency services responding to terrorist bombings, while it may also inform national infrastructure protection priorities in common targets such as subway systems.

Due to the focus of prior studies on BLI, including experimental studies and derivation of casualty criteria, the aim of this work will be to investigate mitigation materials appropriate for the thorax. As such, mitigation materials will include open-cell polyurethane foams as well as syntactic foams due to the potential they present for engineering their mechanical properties, an important consideration for tailoring the material for mitigating injury. As a comparison to this, hydrogels have also been considered as a potential mitigation material due to the demonstrated potential of water to mitigate blast, and the fact that hydrogels which can be manufactured as approximately 99% water. It is necessary to mechanically characterise these materials under compression at high strain rates applicable to blast loading ($100\text{--}1000\text{ s}^{-1}$ order of magnitude strain rates).

In assessing the performance of these materials for blast applications, blast loading

shock enhancement is a strong area of interest as it remains poorly understood and it is instructive to compare widely studied elastomeric polyurethane with other mitigation materials. This would allow for the role of mechanical properties (*e.g.* elastic *vs.* plastically deforming), material length and gas dynamics to be investigated in greater detail as this will play a key role in the loading transmitted to the thoracic wall. Comparison to computational simulation will also be instructive for understanding the phenomena responsible for blast attenuation or amplification.

Finally, it remains that the blast mitigation performance of ballistic plates at scales comparable to the human thorax have not been investigated with a biofidelic human surrogate. Prior studies have neglected the intermediate strength nature of the body. Additional considerations include investigating the influence of a foam backing which has been found to be effective in the murine case, and experimentally interrogating analytical predictions for the thickness of this layer. A significant ergonomic factor that has been previously mentioned is the risk of impact injury when there is a finite stand-off between the armour plate and thorax. An aim of this work will be to quantify how significant this is. Indeed, the clinical significance of these findings will be assessed by considering human tolerance to blast through casualty criteria.

Chapter 3

Epidemiological Study of Blast Injuries Inflicted by Terrorist Bombings in the Civilian Environment

An understanding of blast injuries in the civilian environment is essential for identifying vulnerable body structures and informing mitigation strategy. Addressing the threat of terrorism in the civilian environment has been a long-standing priority of the international agenda, particularly following the events of 9/11. Modern terrorism remains the subject of intense analysis by a broad range of research disciplines, studying changes in terrorist tactics, recruitment, operational scope, ideology and weaponry [130]. Though attacks may vary greatly in their implementation, such as lone wolf *vs.* coordinated attacks, a hallmark of modern terrorism has been psychological warfare achieved through indiscriminate and ostensibly random attacks on the public with the intention of inflicting maximum morbidity and mortality. In the modern age, rapid and widespread publication of such events enable terrorists to achieve publicity while reinforcing and prolonging

public alarm. To this end, IEDs have been utilised globally by terrorists with devastating effect.

In this context, the strategy for blast mitigation research must account for contemporary trends in the occurrence of civilian blast and the resulting injury profiles. There are a wide range of epidemiological reports of blast injuries sustained by military populations [55–57, 131, 132], with significantly less focus on the civilian environment. For blast mitigation research, the civilian setting may be of particular interest due to the fact that the victims will largely not be equipped with Personal Protective Equipment (PPE), allowing the unmitigated effects to be assessed. However, the civilian case raises challenges in the nature of the available data. Patients will often experience a varied immediate medical response, transit to trauma centres and ongoing treatment. When considering blast injury data, different trauma registries or investigators provide significantly different degrees of detail in medical records. Moreover, those records can be limited and not always accessible.

An epidemiological study of blast incidents and injuries in the civilian environment was carried out across all geographical regions. The aims of the study can be summarised by the following three research questions:

- What is the incidence of terrorist blast attacks in the civilian environment and how has this changed in recent years?
- What is the distribution of incidents according to attack setting?
- What are the predominant injury profiles associated with IEDs and does this have a significant dependence on the attack environment?

3.1 Methodology

The study was comprised of two parts. The first was to address the statistical occurrence of civilian blast events and the second was to determine the resulting blast injury dis-

tributions. Data for blast incidence was obtained from analysis of the Global Terrorism Database (GTD) [133] using computational algorithms. The GTD is an open-source and unclassified database of global terrorist events since 1970, managed by the University of Maryland and the National Consortium for the Study of Terrorism and Responses to Terrorism (START). The database includes data for 163 countries, covering 99.7% of the global population. The statistical information for a given attack is derived from a variety of credible media sources. In the rare case of a conflict of statistics, the most recent source is taken. Though there is not a universally accepted definition for ‘terrorism’, the GTD defines it as ‘the threatened or actual use of illegal force and violence by a non-state actor to attain a political, economic, religious, or social goal through fear, coercion, or intimidation’ [133]. This definition accounts not only for the physical impact of successful terrorist attacks, but also the psychological consequences brought by the threat of further attacks. For a given event, the database provides a detailed account of variables including time, location, type of threat, casualty rate and mortality rate. Its terminology is outlined as part of the GTD codebook [133]. The focus of this study is, in particular, terrorist bombing or explosion events against civilians. The GTD accounts for 23,352 such events that resulted in a total of 78,772 deaths and an additional 252,537 casualties. The complete search criteria for the study are summarised in Table 3.1.

Search Criteria	Value
Time Period	1970-2016
Regions	Worldwide
Event Type	Bombing/Explosion Events
Target	Civilians
Outcome	Successful

Table 3.1: Global Terrorism Database (GTD) search parameters for the epidemiological study. The terminology is defined in accordance with the syntax outlined by the GTD codebook [133]. ‘Successful’ attacks were those where the Improvised Explosive Device (IED) functioned as intended by the perpetrator(s).

The study was carried out up to 2016 (inclusive), where the time was separated into two periods: 1970-1999 and 2000-2016. This was designed to assist identification of any recent changes to attack incidence.

The second arm of the study concerned the gathering of clinical blast injury data with particular emphasis on the distribution of injuries across attack settings. A variety of studies have reported blast injuries sustained as a result of isolated incidents or a series of incidents in a given region over a short time period (see Table 3.4). As such, a meta-analysis of these studies was carried out to compile the casualty figures. In the literature, analogous meta-analyses have been carried out infrequently, with the most recent studies being published in 2004 [134] and 2015 [135]. The eligibility of studies for inclusion in the meta-analysis was assessed through a Preferred Reporting Items for Systematic Reviews and Meta-analyses (PRISMA) search [136]. The PRISMA search was performed manually on results returned by the PubMed database, managed by the United States National Library of Medicine (NLM). The database was referenced according to the keywords ‘blast’ *and* ‘injury’ *and* ‘bombing’, filtering for articles during 1970-2016 for consistency with the GTD analysis. The PRISMA search incorporated both primary and secondary exclusion criteria as part of screening and eligibility checks, respectively. Screening exclusion criteria included studies considering non-civilian events, non-bombing/explosive events and review articles. To remain eligible following screening, studies were required to provide sufficient attack setting information and blast injury observations. Finally, for those documents included in the quantitative study, blast injury data was extracted according to the framework shown in Table 3.2.

Blast Injury	Mechanism
BLI	Primary
TMP	Primary
Intra-abdominal Rupture	Primary
Penetrating Torso	Secondary
Penetrating Head, Neck and Face	Secondary
Penetrating Extremity	Secondary
Skull Fracture	Tertiary
Other Fracture	Tertiary
Traumatic Amputation	Tertiary
Burns	Quaternary

Table 3.2: Classification of ten blast injuries for systematic review of epidemiological data. BLI: Blast Lung Injury, TMP: Tympanic Membrane Perforation.

The framework was designed by taking into account the blast injuries commonly re-

ported by the documents meeting the study inclusion criteria. The granularity with which each injury was defined was chosen such that comparisons between studies providing differing levels of detail could be drawn (*e.g.* a study reporting ‘penetrating extremity injuries’ and another providing a detailed breakdown according to limb). Thus, the Table 3.2 classification represents a compromise between minimising unused blast injury data and allowing for comparisons across multiple studies.

3.1.1 Definitions

It was instructive to delineate the severity of the events meeting the Table 3.1 criteria. Two categories of event were defined:

- Incident: an event with ≥ 1 casualty.
- Mass Casualty Incident (MCI): A more severe incident with ≥ 10 casualties.

Here, a ‘casualty’ was defined as an individual who was either wounded or killed. It should be noted that these event definitions were arbitrary. Though a formal number of casualties defining an MCI does not exist, in general, an MCI is an event where emergency response is overwhelmed by the number and severity of casualties. For the purposes of this study, a numeric threshold defining an MCI was instructive.

In order to interrogate the GTD according to attack environment, each event was attributed to a setting. These settings were defined for consistency with the settings typically identified in retrospective studies of blast casualties (see Table 3.4). The attack settings are shown in Table 3.3 (see Appendix A for the complete allocation of GTD settings).

Attack Setting	Example Subsettings
Train	Train, Trolley, Subway
Bus	Bus, Van, Vehicle, Taxi
Semi-Confined Space (SCS)	Restaurant, Bank, Hotel
Open Space (OS)	City, Village, Road

Table 3.3: Attack settings defined for the epidemiological study. The examples are as defined by the Global Terrorism Database (GTD) codebook [133].

Broadly, these settings were defined to distinguish between enclosed, partly enclosed and unenclosed spaces, where the physics of the blast wave propagation are expected to be distinct (see Figure 2.3). It should be noted that sub-settings of undetermined environment (*e.g.* ‘food supply’, ‘political party rally’ *etc.*) were excluded.

3.2 Results

3.2.1 Analysis of the Global Terrorism Database

Against civilian targets, bombings were responsible for 38.5% of fatalities due to terrorism, the highest of any attack type. Time series depicting the evolution of terrorist bombings are shown in Figure 3.1. Two metrics were considered: the frequency of attacks and a measure based on the Global Terrorism Index (GTI) score. The GTI is an annual report published by the Institute for Economics and Peace (IEP) that examines trends in modern terrorism [137]. The GTI score accounts for the relative impact of terrorism by encoding not only the number of events but also the corresponding numbers of casualties. The score is given by:

$$GTI\ Score = M_i w_i + M_k w_k + M_w w_w, \quad (3.1)$$

where M_i is the number of incidents and M_k and M_w are the number of people killed and wounded in those incidents, respectively. The w_i , w_k and w_w are corresponding weights for these quantities. Note that a fourth term accounting for property damages can be included, but is omitted here to concentrate upon human rather than economic cost. The GTI places an emphasis on the impact of fatalities, with weights being set as $w_i = 1$, $w_k = 3$ and $w_w = 0.5$. The GTI score may be computed over an arbitrary time period and geographical span, from a country to worldwide. In Figure 3.1(b), the score over time is provided globally. It should be noted that the GTI scores computed here differ from those quoted by GTI reports [137] due to the focus on bombing events only.

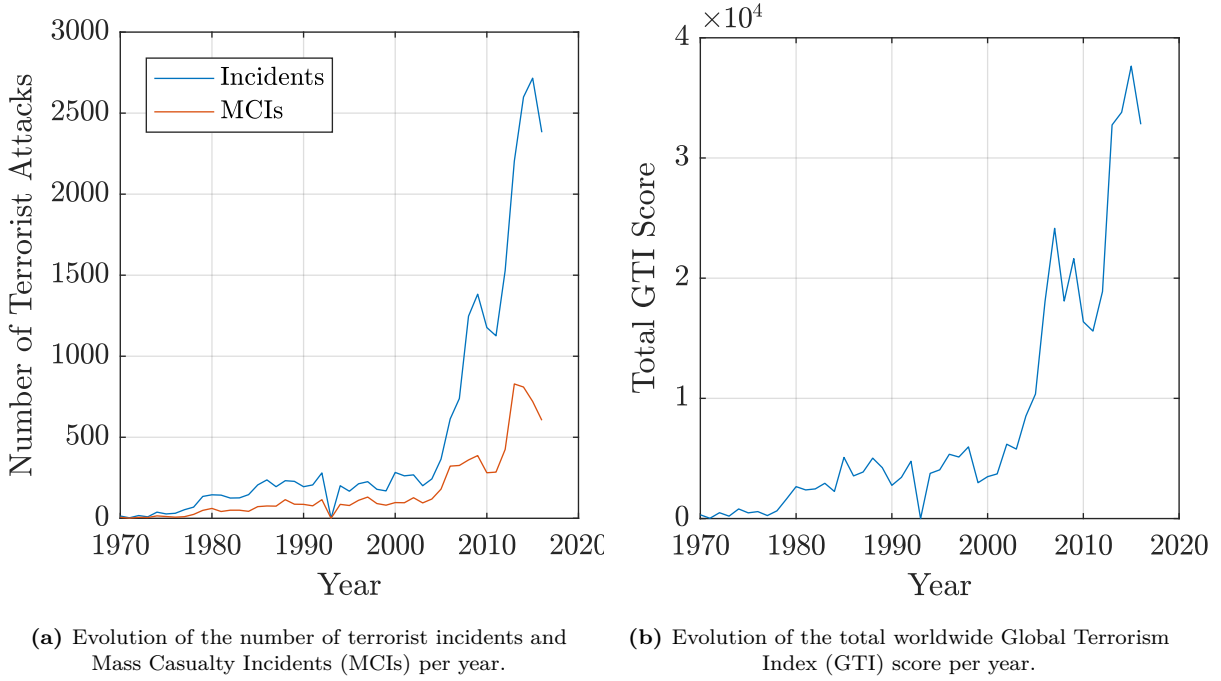
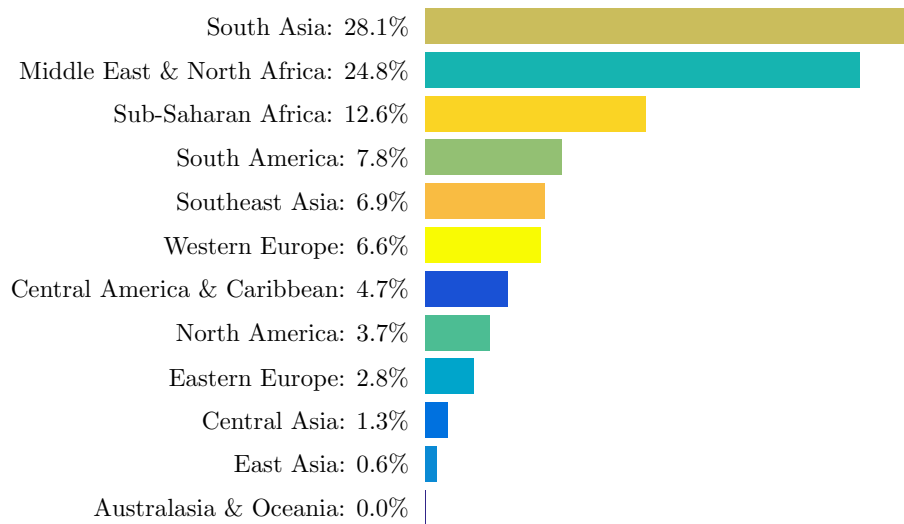


Figure 3.1: Metrics of terrorist activity and its relative impact over the period 1970 to 2016.

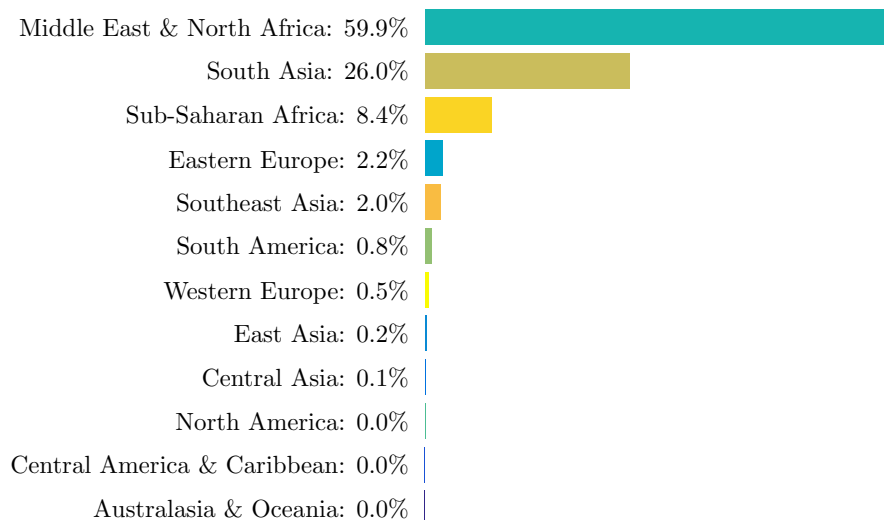
The trend of terrorist activity shown in Figure 3.1(a) demonstrates a marked increase since the 1970 level. Accounting for casualty statistics to improve the validity of year-to-year comparisons, it can be seen that the trend is closely reflected by the GTI score time series in Figure 3.1(b). In detail, a gradual increase in attack frequency occurred from 1970 to 1992, broadly attributed to geopolitical instability during the Cold War and the activities of separatist or extremist groups in Western states such as Spain and Northern Ireland. Following the collapse of the Soviet Union, a general decrease and plateau can be observed up to 2004. It should be noted that this overall trend does not always reflect the experience of an individual region. For example, the formation of terrorist groups in former Soviet states resulted in a proliferation of attacks in Eastern Europe.

The most distinct feature of these results is the marked increase of terrorist activity in the aftermath of 9/11. Analogous to the general shift of terrorist motivation away from nationalist or separatist ideologies following the end of the Cold War, 9/11 is considered to represent a further paradigm shift of terrorist activity being largely motivated by ‘fundamentalist’ aims [138]. Subsequent increased and direct Western military involvement in the Middle East, combined with the fact that the region has been beset by multiple

and ongoing episodes of civil unrest, continues to contribute to the significant volatility seen since 2004. During the more recent study period of 2000-2016, the majority (60%) of global fatalities inflicted by terrorism have occurred in the Middle Eastern & North Africa region alone (see Figure 3.2).



(a) Proportion of fatalities by region during period 1970-1999 (Total: 14,610).



(b) Proportion of fatalities by region during period 2000-2016 (Total: 64,162).

Figure 3.2: Regional distribution of fatalities due to bombings.

It is important to note that the highly complex subject of international terrorism, its initiation, tactics and aims demands analytical treatment by a broad range of disciplines. This extremely brief overview of the evolution of modern terrorism is to serve as an an-

thropological context for the subject of blast mitigation. It also emphasises how terrorism is rarely without ideological or political motive, but rather the perceived motivations of terrorists can be connected with global events.

The worldwide geographical distribution of terrorist attacks is shown in Figure 3.3 for the two study periods.

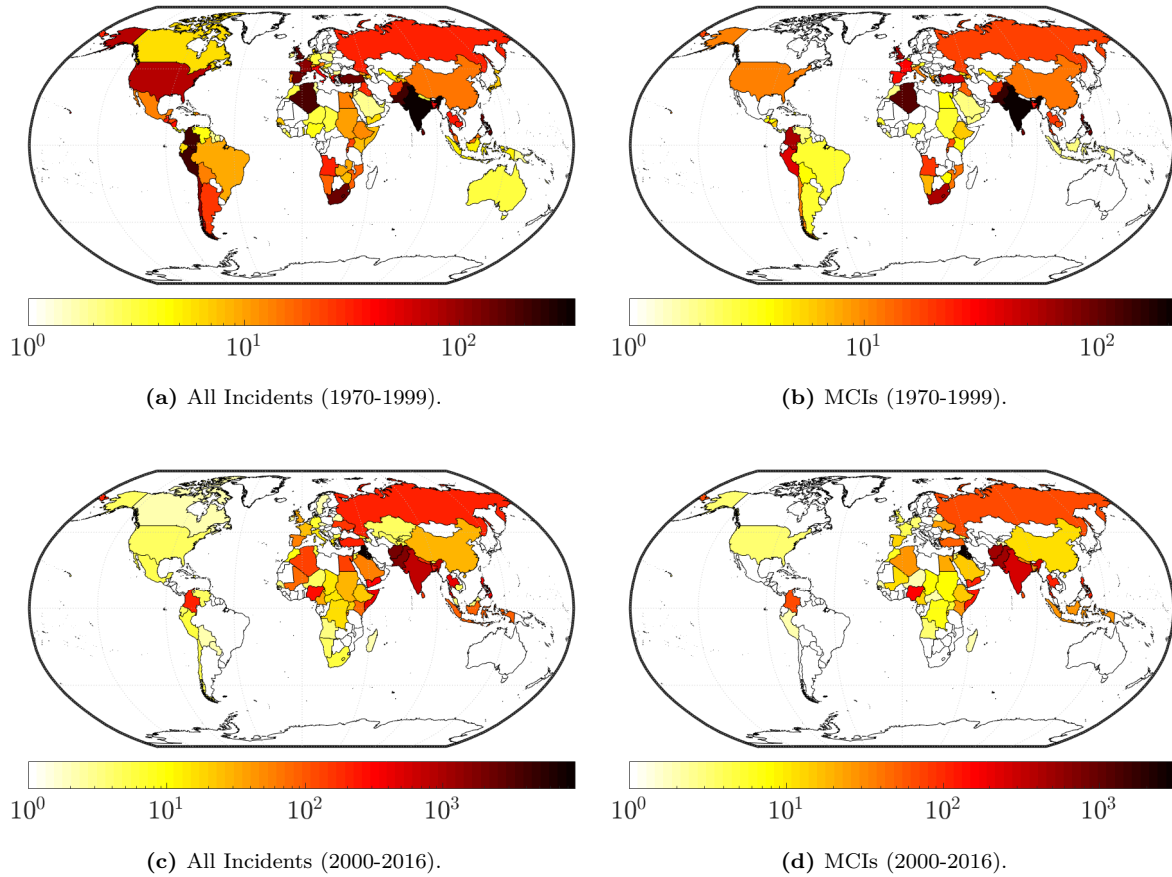


Figure 3.3: Heat maps depicting the geographical distribution of incidents and Mass Casualty Incidents (MCIs) in the time periods of 1970-1999 and 2000-2016. A logarithmic scale has been used in each case to reflect the wide range of occurrences across different countries.

It can be seen that certain regions, in particular the Middle East & North Africa and South Asia, are consistently areas of significant terrorist activity. The MCI distributions of Figures 3.3(b) and 3.3(d) also illustrate that the relative predominance of terrorist activity in Western Europe is diminished when concentrating upon these more severe events. Also noteworthy is that marked increase of terrorism in the more recent time period. Comparing the period 2000-2016 with 1970-1999, average incidents per year increased by a factor of 8.5 while MCIs per year increased by a factor of 6.4

A breakdown of global incidents and MCIs according to attack environment is shown in Figure 3.4.

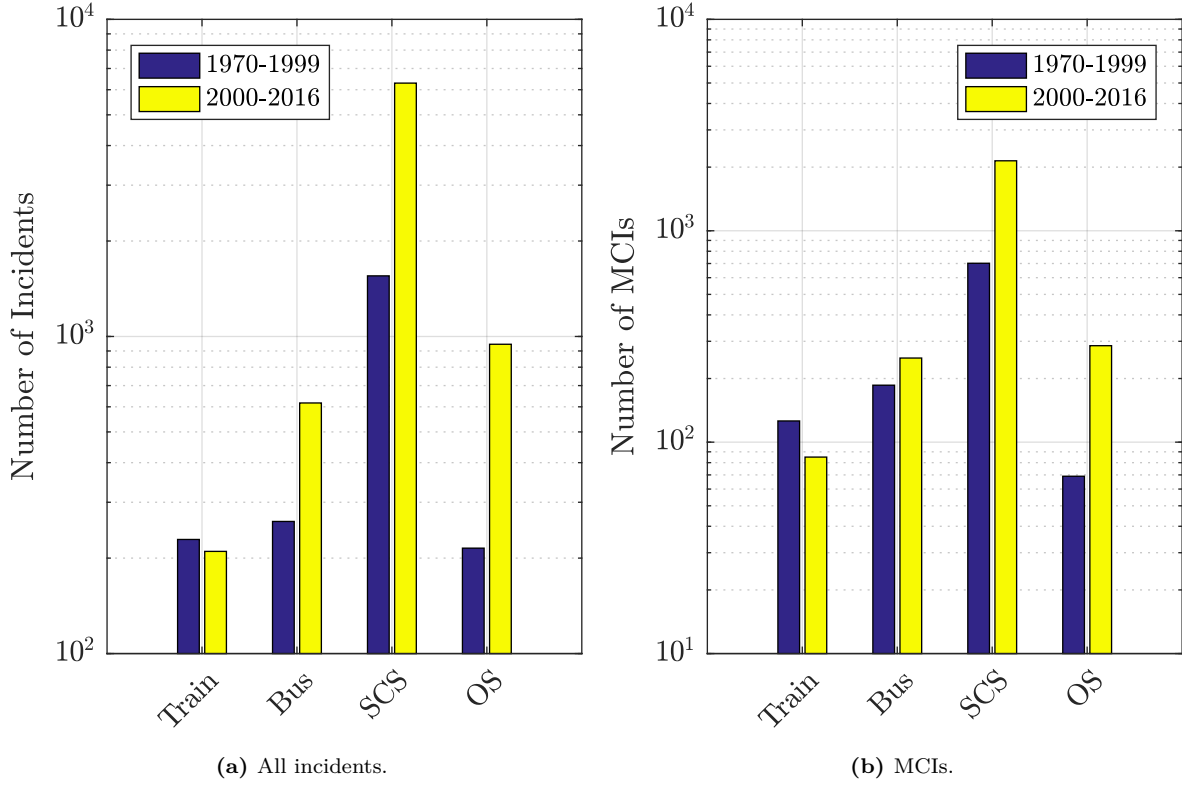


Figure 3.4: Distribution of terrorist activity across attack settings. MCI: Mass Casualty Incident.

From Figure 3.4, it can be seen that the SCS environment is prominent due to the fact that a large number of environments meet its broad description (see Appendix A). The relative proportion of closed space (trains and buses) explosions is greater for MCIs in 1970-1999 (20.4% compared with 13.0% overall) and 2000-2016 (5.9% compared with 4.3% overall). A more severe definition of MCI with a greater casualty threshold would emphasise this further. Indeed, the damaging effects of the blast wave are exacerbated in enclosed spaces where the blast is likely to undergo multiple reflections, increasing the peak stress and inflicting greater morbidity. The number of casualties and injuries will also tend to be escalated by the inherently high density of people on public transport. These factors have been exploited by terrorists on multiple occasions in recent history (*e.g.* Madrid 2004, London 2005 *etc.*) and render such environments attractive targets [139]. The damaging capability of IEDs in confined spaces is emphasised by consideration of the casualty statistics, as shown in Figure 3.5.

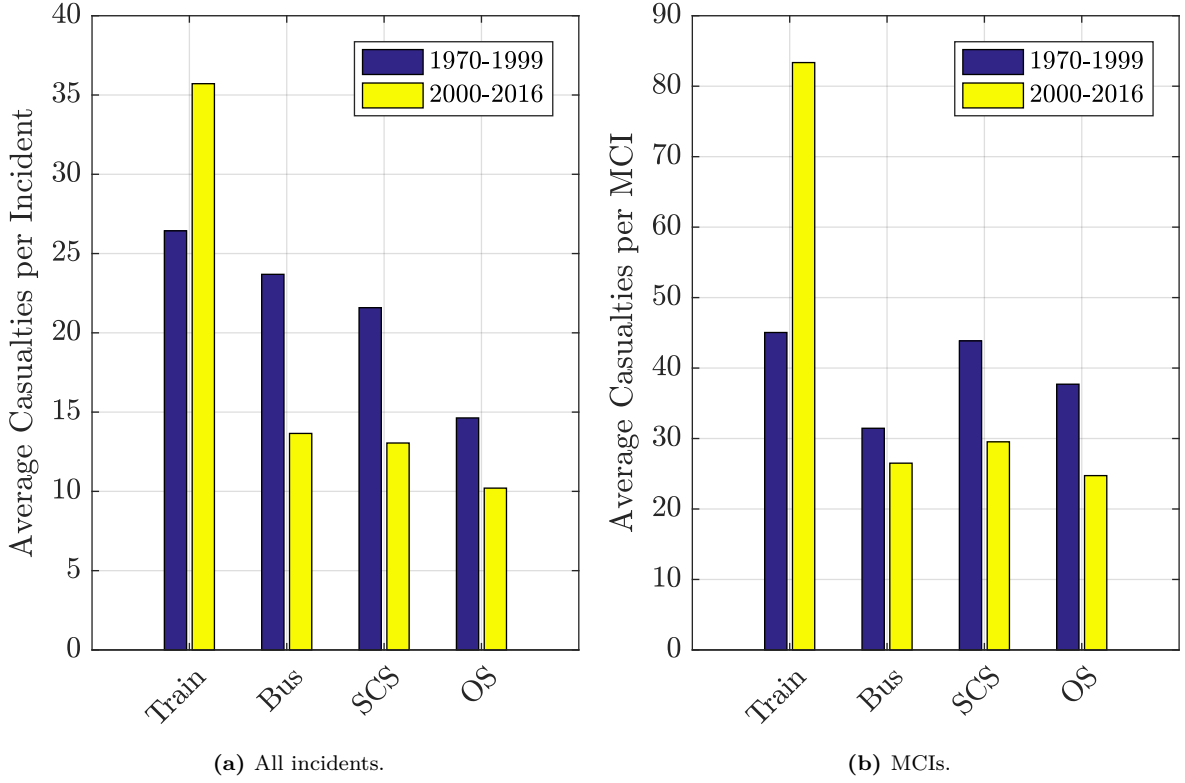


Figure 3.5: Distribution of terrorist activity across attack settings.

The severity of train incidents is made clear in Figure 3.5, a common feature across both time periods. Over all incidents in the period 1970-1999 (Figure 3.5(a)), there is a statistically significant ($p < 0.05$) difference in casualties per incident between at least partially enclosed spaces (train, bus and SCS) and open spaces. Indeed, explosions in open spaces are generally associated with the least morbidity, attributed to rapid attenuation of the blast wave in free space and, potentially, more dispersed targets. During the same period, there is no significant difference ($p > 0.05$) in the severity of MCIs across different environments (Figure 3.5(b)). The key change to this picture in the more recent time period is the dramatic increase in severity of train attacks. This difference is statistically significant to all other attack environments. Also notable is the fact that over all incidents, bus and SCS attacks were no longer significantly different in severity from open space attacks in 2000-2016. Such observations could be due to a number of unknown factors including the proximity of the targets for a given incident, or modern trends in IED design. For example, since the blast wave dissipates quickly in open space, it has been speculated that a modern IED may be designed with the intention to instead maximise

secondary injury by incorporating substantial shrapnel [50].

3.2.2 Blast Injury Meta-Analysis

The initial PubMed literature search returned a total of 197 articles. Of these, 18 articles were included in the study for quantitative analysis. This is summarised by the Figure 3.6 PRISMA flow diagram.

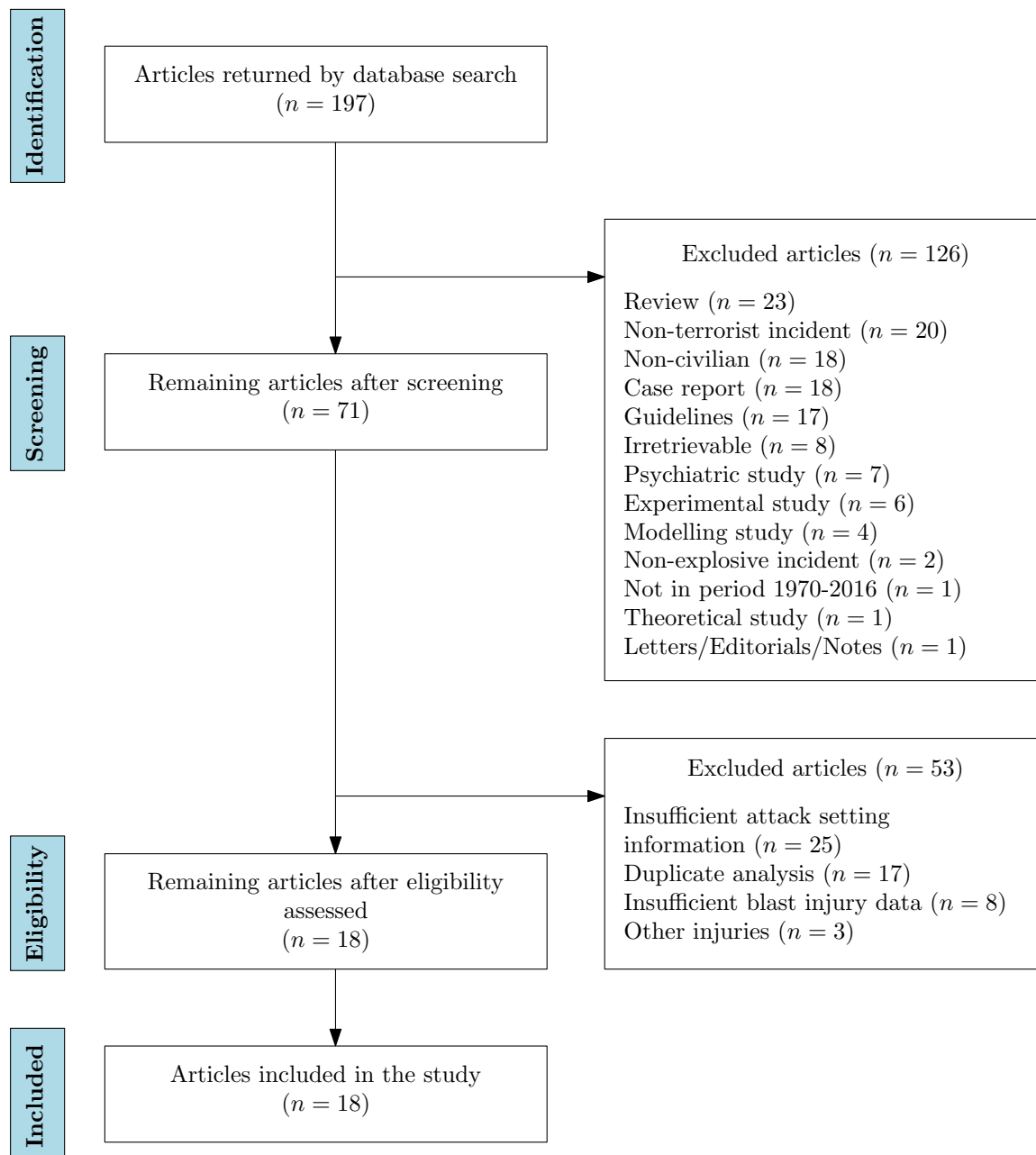


Figure 3.6: Preferred Reporting Items for Systematic Reviews (PRISMA) flow diagram for systematic review of epidemiological data.

The exclusion criteria for the screening and eligibility stages are shown in descending order of occurrence in Figure 3.6. Screening criteria were predicated upon the nature of the document. Almost half (48%) of the articles were screened out due to being reviews discussing the theoretical background to blast, based on non-terrorist incidents or being non-civilian focussed. Other commonplace documents that were excluded were those providing guidelines for clinicians or disaster response, detailing psychiatric effects and those that were not based upon a real-world explosive incident.

In the eligibility assessment, articles were excluded on the basis of their informational content, only retaining those documents with specification of both the attack settings and blast injuries. Such studies derive their data from retrospective reviews of trauma registry records. A lack of environment information was the most common reason for non-eligibility, though it should be acknowledged that in many papers this was not relevant or unknown for the majority of casualties. Moreover, studies examining longer term complications (termed ‘other injuries’), such as Heterotopic ossification or candidemia, were excluded. It was necessary to eliminate studies reviewing the same blast injuries from trauma records over a common time period and location. This prevented the casualty statistics from a particular event contributing to the statistics more than once. In such overlapping studies, the study providing data most appropriate for the Table 3.2 classification was included. An exception to this treatment was the studies of Radford *et al.* [60] and Patel *et al.* [140] which addressed distinct blast injuries for the same incident (London, 2005). The articles included in the study are summarised in Table 3.4. Across these studies, a summary of the incidence of each injury by attack setting is shown in Table 3.5.

Study Authors	Study Period	Location	Setting(s)
Almogly <i>et al.</i> [48]	2001-2004	Jerusalem, Israel	Bus, SCS, OS
Almogly <i>et al.</i> [50]	1994-1997	Israel	Bus, SCS, OS
Brismar & Bergenwald[24]	1980	Bologna, Italy	SCS
Cooper <i>et al.</i> [25]	1973-1974	UK	SCS
Hogan <i>et al.</i> [141]	1995	Oklahoma City, USA	SCS
Katz <i>et al.</i> [54]	1989	Jerusalem, Israel	Bus
Miller <i>et al.</i> [74]	1998	Omagh, UK	SCS
Odhiambo <i>et al.</i> [142]	1998	Nairobi, Kenya	SCS
Patel <i>et al.</i> [140]	2005	London, UK	Train, Bus
Persaud <i>et al.</i> [143]	1999	London, UK	SCS
Radford <i>et al.</i> [60]	2005	London, UK	Train, Bus
Rodoplu <i>et al.</i> [144]	2003	Istanbul, Turkey	OS
Rodoplu <i>et al.</i> [145]	2003	Istanbul, Turkey	OS
Singh <i>et al.</i> [146]	2013	Boston, USA	OS
Turegano-Fuentes <i>et al.</i> [147]	2004	Madrid, Spain	Train
Walsh <i>et al.</i> [148]	1992	London, UK	SCS
Yazgan & Aksu[149]	2015	Ankara, Turkey	OS
Zafar <i>et al.</i> [150]	2002	Karachi, Pakistan	SCS

Table 3.4: Studies included in the epidemiological meta-analysis. The study period refers to the time during which the incident(s) took place, given as a range for the studies that examined events over multiple years. SCS: Semi-confined Space, OS: Open Space.

Blast Injury	Attack Setting (Casualties with Injury/ Total Casualties Studied)			
	Train	Bus	SCS	OS
BLI	43/512	64/361	41/601	13/433
TMP	286/617	118/399	322/786	50/433
Intra-abdominal Rupture	28/512	7/101	11/165	2/155
Penetrating Torso	40/512	19/72	56/250	32/112
Penetrating Head, Neck and Face	94/512	49/72	140/528	45/117
Penetrating Extremity	77/512	26/72	63/153	26/64
Skull Fracture	9/512	8/72	30/443	6/107
Other Fracture	171/512	24/361	123/528	28/174
Traumatic Amputation	45/617	3/327	3/119	0/76
Burns	89/512	60/361	90/310	12/174

Table 3.5: Number of casualties presenting each injury out of the total number of casualties studied in each attack setting. These numbers are computed as a percentage in Table 3.6. For each setting, the total casualties studied can vary due to some studies focussing on certain injuries. For example, in the train setting, one of the two studies comprising the data considered only TMP and traumatic amputation, resulting in a larger total casualty population for these injuries compared to other injuries. BLI: Blast Lung Injury, TMP: Tympanic Membrane Perforation, SCS: Semi-confined Space, OS: Open Space.

The objective of the quantitative analysis was to establish the proportion of casualties

presenting the different injuries for each attack setting. These proportions are shown in Table 3.5. It can be seen that there are variations of the sample size. For example, in open space, just 64 casualties were considered for penetrating extremity injuries compared with 433 for BLI. Since the population of casualties was stratified into these separate study populations, it was necessary to account for the fact that greater confidence must be attached to studies considering a larger study population. As such, stratified sample theory was invoked where the studies were treated as ‘strata’. A schematic representation of this is shown in Figure 3.7.

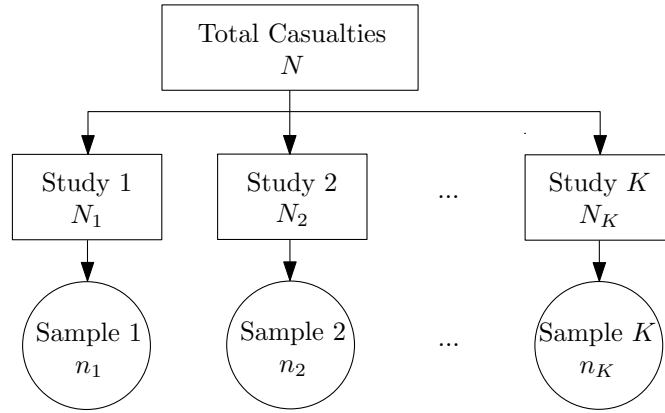


Figure 3.7: Schematic illustrating stratification of the total number of casualties. N : Total casualties across all studies, N_k : Number of casualties reported in k^{th} study, n_k : Sample size of casualty population N_k in k^{th} study.

In this case, there were $K = 18$ strata where the k^{th} stratum had a population of N_k casualties. For a given study, the total number of casualties for the incident(s) were taken as the sum of the number admitted to hospital and the number of pre-hospital deaths. In many cases, this was an underestimation due to the fact that it was certainly possible for those who were not hospitalised to suffer injury. Moreover, it is challenging to retrospectively quantify the exact number of people affected by a blast in a crowded public space. From each study’s total population N_k , a sample of size n_k was taken, from which the distribution of blast injuries was reported. In a majority of cases (89%), the sampling rate was less than 100%, primarily due to a subset of hospital records being unavailable to the investigators. It was assumed that the sample population could be treated as having been randomly drawn from the study’s total casualty population, termed Simple Random Sampling (SRS) [151]. From this, it was clear that the overall

number of casualties (N) and sample number of casualties (n) included in this study were given by:

$$N = \sum_{k=1}^K N_k \quad (3.2)$$

and

$$n = \sum_{k=1}^K n_k, \quad (3.3)$$

respectively. For each injury, j , the objective was to compute the proportion (p_j) of the total population, N , possessing that attribute *i.e.* $p_j = J_j/N$ where J_j was the total number of casualties with the j^{th} injury. For a given study k , this proportion was given by:

$$p_{k,j} = \frac{1}{N_k} \sum_{j=1}^{N_k} y_{k,j}, \quad (3.4)$$

where $y_{k,j}$ was a binary parameter for each casualty that was set to unity if the casualty had the injury or zero if the casualty did not [151]. However, since a sample of the total casualties n_k was taken, $p_{k,j}$ was unknown and consequently estimated as:

$$\hat{p}_{k,j} = \frac{1}{n_k} \sum_{j=1}^{n_k} y_{k,j}. \quad (3.5)$$

In order to obtain an unbiased estimator of the proportion possessing a given injury j across all studies, $\hat{p}_j$, it can be shown that:

$$\hat{p}_j = \sum_{k=1}^K W_k \hat{p}_{k,j}, \quad (3.6)$$

where $W_k = N_k/N$ was the weight assigned to the k^{th} study [151]. Thus, $\hat{p}_j$ was simply a weighted average of stratum-specific estimates. It was also crucial to quantify confidence in the computed $\hat{p}_j$ values. Given stratum populations with different estimators $\hat{p}_{k,j}$, an appropriate method for this was pooled variance. The pooled variance is a weighted (unbiased) estimate of the variance of each stratum sample under the assumption that all strata have the same variance. It can be shown that the pooled variance estimator is

given by:

$$\hat{V}(\hat{p}_j) = \sum_{k=1}^K W_k^2 \left(\frac{N_k - n_k}{N_k} \right) \left(\frac{\hat{p}_{k,j}(1 - \hat{p}_{k,j})}{n_k - 1} \right), \quad (3.7)$$

where the overall standard deviation can be computed as its square root [151]. Using this, the 95% confidence interval (CI) indicating the uncertainty of a $\hat{p}_j$ estimator for a given injury was defined by: $\hat{p}_j \pm t_{1-\alpha/2}(dof)\sqrt{\hat{V}(\hat{p})}$, where $t_{1-\alpha/2}(dof)$ was the critical value of the two-tailed t -distribution with significance level $\alpha = 0.05$ and $dof = n - K$ degrees of freedom. With this statistical framework, the resulting blast injury rates according to attack environment are summarised in Table 3.6.

Injury	Proportion Presenting Injury ($\pm$ 95% CI)			
	Train	Bus	SCS	OS
BLI	8.40 (1.22)	17.69 (2.65)	6.87 (1.43)	2.96 (1.09)
TMP	46.29 (4.62)	29.70 (3.30)	40.96 (1.86)	11.53 (3.53)
Intra-abdominal Rupture	5.47 (1.00)	6.88 (3.47)	6.91 (2.77)	1.00 (1.07)
Penetrating Torso	7.81 (1.18)	26.39 (7.87)	22.35 (3.67)	28.25 (7.42)
Penetrating Head, Neck and Face	18.36 (1.71)	68.06 (8.33)	26.48 (2.75)	38.33 (7.40)
Penetrating Extremity	15.04 (1.57)	36.11 (8.58)	40.97 (5.81)	40.02 (9.68)
Skull Fracture	1.76 (0.58)	11.11 (5.61)	6.85 (1.75)	5.87 (3.33)
Other Fracture	33.40 (2.08)	6.75 (1.85)	23.38 (2.35)	16.28 (4.20)
Traumatic Amputation	7.34 (3.07)	0.77 (0.41)	2.55 (2.2)	0.00 (0.00)
Burns	17.38 (1.67)	16.49 (2.77)	28.9 (3.63)	7.08 (3.60)

Table 3.6: Proportions of casualties presenting different blast injuries by attack setting. These values provide the expected percentage of casualties with each injury according to attack setting. The percentage is computed from the ratio of the number of casualties reported to have the injury to the total number of casualties in the attack setting (shown in Table 3.5). Where this total number of casualties was higher for a given attack setting and injury, the confidence in the calculated proportion was higher, resulting in a narrower Confidence Interval (CI). As this total number of casualties was contributed to by multiple studies for a given setting, this confidence interval was computed as the weighted average. For example, for the train TMP data, two studies contributed total casualty populations of 512 and 105, leading to smaller and larger CIs, respectively due to the difference of sample sizes. In this example, the overall confidence interval was computed as the average of the CIs for the two studies. BLI: Blast Lung Injury, TMP: Tympanic Membrane Perforation, SCS: Semi-confined Space, OS: Open Space.

An important additional step was to ascertain the statistical significance of injury rates on attack environment. An instructive method for this was to utilise CIs. For each injury and combination of any two attack settings, the difference of rates was calculated (*e.g.* for a train-bus comparison of BLI, $17.69 - 8.40 = 9.29$) with a corresponding 95% CI. If the resulting range did not span zero, then the null hypothesis (that the injury has no dependence on the two settings) could be rejected. A detailed analysis of this can be found in Appendix B. In all cases, the difference of injury rate was found to be significant, leading to the widely accepted conclusion that the distribution of blast injuries is dependent on attack setting [50, 92, 134, 135].

From the Table 3.6 results, the highest contrast comparison is seen between the most enclosed environments (train, bus) and open space. Targets in open space are less likely to suffer injuries induced by the blast wave or fireball with significantly fewer casualties presenting BLI, TMP and intra-abdominal rupture. This can be attributed to the rapid decay of the blast wave in open-space, as opposed to the reflections that prolong the loading duration in enclosed spaces. This is particularly clear when considering the injury that occurs at the highest pressure threshold - intra-abdominal rupture. It is observed at a rate of approximately 5% in all environments except open space, where the rate is extremely low. Conversely, casualties in open space were more susceptible to penetrating trauma which may be a result of the relatively uninhibited trajectory of fragments compared to partially enclosed environments. In terms of tertiary injury, there is not an ostensible rationale for connecting the incidence of fracture with setting. Indeed, bodily displacement against rigid objects can occur in any environment. It is possible that the mobile nature of trains and buses (and the subsequent sudden stop of the vehicle) is also an important factor here. The significantly higher level of traumatic amputation on trains may be due to the combination of high pressure magnitude and the seated position of many of the targets, leaving lower limbs in closer proximity to a threat detonated at ground level [140]. Finally, it is apparent that burns are suffered to a greater extent in enclosed environments compared to open space due to transient confinement of the fireball. Considering the overall distribution of injures, it is clear that TMP is a

particularly prevalent blast injury, owed to the fact that it occurs at a low peak pressure threshold of approximately 100 kPa for any loading durations longer than 10 ms [71].

There are several caveats to this analysis. In comparing injury rates across attack settings, it is implicit that there are no other independent variables. In actuality, the nature and TNT equivalent of the threat are unknown, as well as the proximity of the targets. This is the main source of uncertainty in this analysis. As an example, the data for bus bombings is strongly contributed to by data from Israel (see Table 3.4). A hallmark of these attacks in the past decade was the prevalence of penetrating trauma, with investigators finding that intra-abdominal trauma was predominantly caused by fragment impact rather than the blast wave [50]. This is in contrast to earlier incidents, such as the 1989 bus bombing reported by Katz *et al.*, where primary blast was the most significant cause of mortality [54]. This was linked by investigators to modern changes in IED design including a shift towards higher mass shrapnel [50]. In terms of deployment, it was also found that the IED is detonated at chest height rather than under a seat [50], which may be a rationale for the high incidence of penetrating head, neck and face trauma observed on buses. A second caveat to note is that there are inherent differences in how comprehensive and timely the diagnosis and subsequent documentation of blast injuries are for different trauma centres, particularly in the wake of an MCI that may overwhelm services. Following from this, it was necessary to treat injuries dichotomously (a casualty either had the injury or did not) due to injury severity being unreported in most cases *e.g.* open/closed fractures, number of fractures, degree of BLI *etc.*

3.3 Conclusions

Notwithstanding the data limitations of the meta-analysis, due to the large casualty population over the studies included in the meta-analysis, conclusions can be drawn for the purpose of informing blast mitigation strategy. A more enclosed space places greater emphasis on the need to mitigate the blast wave, while in open space, protection from fragment impact must be prioritised. This results in a fundamentally different mitigation

material design, with blast wave mitigants incorporating a deformable component for impulse energy absorption. In the case of secondary injury, this is the priority of conventional PPE which is designed for defeating ballistic or penetrating threats, typically incorporating combinations of hard ceramic plates and aramid fibres. In the civilian case, there is particular need for effective primary blast injury mitigation. This is highlighted by both the GTD and PRISMA studies. Indeed, Figure 3.4 demonstrates the increase of terrorist activity since 2000 across all attack environments but, in particular, enclosed settings. These settings are associated with the greatest severity (Figure 3.5) with train incidents resulting in the most morbidity. In response to this increase of frequency and severity, O'Neill *et al.* identified the structural components of trains and rail systems that must be addressed for mitigating the effects of the blast wave [139]. Areas such as the walls, doors and roof are especially of interest for introducing mitigation layers, while an analogous need also exists in buildings (where retro-fitted cladding may be preferred). O'Neill *et al.* recommended two further full-scale blast tests of train panels intended to first assess the response of current panels and then compare this with mitigation technologies. For personal applications, such as law enforcement, lightweight mitigations materials are required.

Chapter 4

Materials & Preparation

Both cellular and hydrogel blast mitigation materials were investigated. Cellular materials included open-cell polyurethane foams due to their being lightweight and interest in the shock enhancement phenomenon. In addition, syntactic foams were also manufactured as their properties can be adapted to the application, as well as hydrogels which instead provide energy dissipation through brittle fracture mechanisms. Unlike the open-cell polyurethane, these two materials were manufactured. For blast mitigation experiments, a 20 mm sample thickness was chosen due to its approximate equivalence to the thickness of a ballistic plate.

4.1 Casting Equipment

A range of sample sizes were required for the materials to ensure samples were appropriate for material characterisation, shock tube scale and full-scale experiments. For the hydrogels and polyurethane-based syntactic foams, casting was required. CAD drawings for these parts are shown in Appendix C. These parts were designed with a removable base and polycarbonate cover to enable the degassing methods to be applied to the material if necessary as well as to assist with demoulding.

Material characterisation samples were cast to either 1.6 mm or 2.0 mm thickness with a 9.0 mm diameter, ensuring a length to diameter aspect ratio that minimised frictional and inertia effects in SHPB experiments. These samples could also be made annular (by coring the sample) to minimise radial inertia induced stress. The materials were cast into acetal moulds due to the ease of demoulding syntactic foam samples. In this case, a wax aerosol release agent was used (MacWax, MB Fibreglass).

For shock tube scale mitigation and shock enhancement experiments, acetal moulds for manufacturing samples of 60 mm diameter and 20 mm thickness were used. The 60 mm diameter is equal to the shock tube diameter (see Section 5.1) while samples of greater length could be created by using multiple 20 mm samples.

Finally, full-scale blast mitigation experiments utilised square 30 cm $\times$ 30 cm panels with 20 mm thickness, approximating the dimensions of ballistic plates applied to the thorax.

4.2 Polyurethane Foams

4.2.1 Reticulated Foams

Cellular open-cell reticulated foams offer high compressibility due to their porosity and are widely used in packaging applications where they undergo large volume changes while absorbing kinetic energy under load. The porosity for such foams is conventionally expressed in pores per inch (ppi). Under blast loading, it has been reported by Wilgeroth *et al.* [152] that peak pressure can be mitigated by approximately 10% when using 40 mm lengths of reticulated foam of 65-79 ppi. However, these foams were fully clamped, and for personal mitigation applications, the boundary conditions will require that the foam is fully supported at the rear by the protected structure (*e.g.* thoracic wall). This boundary condition replicates the loading conditions used in shock enhancement experiments, where 20-80 ppi reticulated foam were used. As such, a range of foams (A13 EtherCell Filter

4.2. POLYURETHANE FOAMS

Foam, Acoustaf foam) were investigated and are shown in Table 4.1.

Pores per inch (ppi)	Relative Density, ρ^*/ρ_s (%)	Porosity, ϕ (%)
20	3.26 ± 0.07	96.72 ± 0.07
40	2.71 ± 0.07	97.27 ± 0.07
60	2.19 ± 0.05	97.81 ± 0.05
80	1.62 ± 0.04	98.36 ± 0.04

Table 4.1: Reticulated polyurethane foams used in this work and their measured porosities using a helium pycnometer. The porosities were computed as the mean of 5 repeat measurements (5 separate foam samples), with the uncertainty calculated as the standard error of the mean. Increasing relative density, ρ^*/ρ_s , corresponds to a greater solid phase proportion in the foam. ρ^* : Foam bulk density, ρ_s : Foam skeletal density.

The foams are manufactured industrially by ignition of hydrogen to remove the cell faces of a closed-cell foam to leave an open-cell solid phase ‘skeleton’ (see Figure 4.1).



Figure 4.1: (a): 40 pores per inch (ppi) Acoustaf foam reticulated foam used in this research [153]. (b): Scanning electron micrograph of 40 ppi polyurethane open-cell foam (Hitachi S-3500-N, 10 kV accelerating voltage) exhibiting the high void fraction relative to the skeletal mass [154].

Approximate average bulk densities are quoted for the foams shown in Table 4.1 in the range of $30\text{--}45\text{ g cm}^{-3}$. Accurate determination of the porosity of the foams requires the solid volume fraction of the foam to be measured. A higher ppi foam does not necessarily imply lower solid fraction or relative density as ppi does not specify pore size. Relative density was measured using a Micromeritics AccuPyc II 1340 Helium Pycnometer with cubic foam samples of side length $20.0 \pm 0.5\text{ mm}$. A pycnometer enables the solid fraction of the foam to be established by measuring the volume of helium gas displaced by the porous sample. The errors shown for the relative density and porosity in Table 4.1 are the standard error of the mean over 5 repeat measurements. These findings indicate

consistency of the foam manufacturing method as well as statistically significant difference of porosity between the selected foams.

4.2.2 Syntactic Foams

Syntactic foams were manufactured by filling a polymeric matrix with hollow glass microspheres. Polyurethane-based foams have been widely studied in energy absorbing applications and suggested by Pellegrino *et al.* for blast mitigation applications [83]. The two approaches of varying syntactic foam porosity are to alter the microballoon volume fraction or the microballoon wall thickness. The former approach was adopted in this work.

Polyurethane Shore hardness 30 A (Xencast[®] PX30 Soft Flexible Polyurethane Rubber, Easy Composites) was used as the matrix material (PU30), with glass microballoons used as hollow particulate (3M[™]K20 Series Glass Bubbles, Easy Composites). Shore hardness is a standard (ASTM D2240) measure of a material's resistance to indentation by a durometer, where the Shore A test indicates a specific durometer indenter geometry. The Shore hardness is a dimensionless scale where higher values indicate a harder material. The (Xencast[®] polyurethane was manufactured by mixing a polyol and a bithane hardener together in a 1:1 ratio by mass. This was accomplished by vigorous hand mixing for 2 minutes, following which, the microballoons were added to yield samples of 94%, 96% and 98% porosity. The mass of microballoons necessary to achieve these porosities was estimated according to the Figure 4.2 illustration.

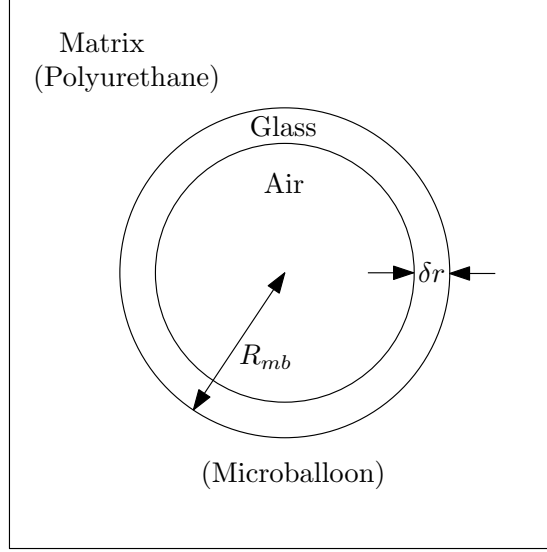


Figure 4.2: Illustration of the syntactic foam with a glass microballoon shown embedded in the polyurethane matrix. The single particle is shown with mean microballoon radius R_{mb} and mean microballoon wall thickness δr . This diagram provides a geometric basis for the air void fraction of the microballoon to be estimated. This allows the necessary mass of microballoon additive to achieve a target syntactic foam porosity to be calculated as shown in Eq. (4.1).

The K20 Series microballoons had a specified average particle diameter of $d_{mb} = 60 \mu\text{m}$ and true density $\rho_{mb} = 0.18 \text{ g cm}^{-3}$. By determining the air void fraction in Figure 4.2 from geometrical considerations, the necessary total mass of microballoons (m_{tot}) to be added to the polyurethane could be estimated as:

$$m_{tot} = \phi \rho_{mb} \left(\frac{R_{mb}^3}{R_{mb}^3 - (R_{mb} - \delta r)^3} \right) V_{bulk}, \quad (4.1)$$

where ϕ is the target porosity, $R_{mb} = 30 \mu\text{m}$ is the average particle radius, $\delta r = 1.29 \mu\text{m}$ is the average microballoon wall thickness, and V_{bulk} is the total volume of polyurethane matrix material. As a check, the average particle diameter and wall thickness were measured using an optical microscope (Brunel SP400) using incident illumination and a 40x objective as shown in Figure 4.3. In order to clearly visualise each microballoon, separation between the particles was created by adding them to an optically transparent viscous fluid (Glucose Syrup, Special Ingredients) and sandwiching the mixture between microscope slides. In the micrographs, the length scale was determined from the pixels per unit physical distance in a calibration image of a standardised 1 mm length object.

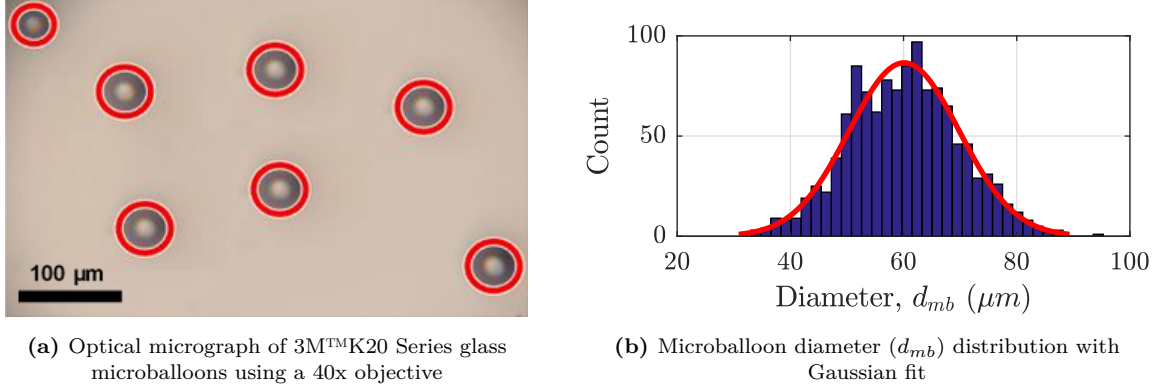


Figure 4.3: (a) Optical micrography and (b) circle finding analysis to determine size distribution of microballoons.

By measuring a large sample of microballoons, the distribution of microballoon diameters, average diameter and associated uncertainty could be accurately determined. To do this efficiently, 50 micrographs were recorded as shown in Figure 4.3(a) covering 1189 microballoons. An algorithm was written using the Hough transform to extract the circular features of each image and return the diameters. From the resulting normal distribution visible in Figure 4.3(b), d_{10} , d_{50} and d_{90} values could be calculated. These values are commonly reported for particle size distributions [155]. d_{10} is defined as the particle diameter where 90% of the particles are larger, while the d_{90} is the particle diameter where 10% of the distribution is larger. Thus, the d_{50} size is the diameter where half the distribution is larger and smaller, arithmetically equal to the median particle diameter. For the microballoons, it was found that $d_{10} = 45 \text{ mm}$, $d_{50} = 58 \text{ mm}$ and $d_{90} = 74 \text{ mm}$. The mean particle diameter was calculated as $d_{mb} = 60 \pm 10 \text{ μm}$, where the uncertainty was given by the standard error of the mean, consistent with the manufacturer specifications. Agreement was also found for the average wall thickness, with a check of a sample of 20 microballoons yielding $\delta r = 1.32 \pm 0.05 \text{ μm}$ where the uncertainty was again computed as the standard error of the mean.

The pot life of the PU30 was approximately 30 minutes at room temperature ($23 \pm 2 \text{ °C}$), after which the material could no longer be cast. As such, the microballoons were weighed beforehand to ensure no delay in the mixing process. However, this mixing of the polyurethane and microballoons introduced substantial air bubbles to the mixture.

It was crucial to degas the mixture. Indeed, matrix porosity at length scales similar or larger than the microballoons, known as ‘unreinforced voids’ (see Figure 2.11(a)), have been shown numerically to have a significant effect on the foam’s bulk mechanical properties [156]. This was addressed by degassing the mixture under vacuum for 20 minutes. Following this, the mixture was cast into the release agent sprayed mould, sealed, cured for 24 h at room temperature, then demoulded.

Resulting optical micrographs for bulk PU30 and 98% porous polyurethane syntactic foam samples are shown in Figure 4.4 as a check of the reproducibility of the manufacturing method and the homogeneity and isotropy of the materials.

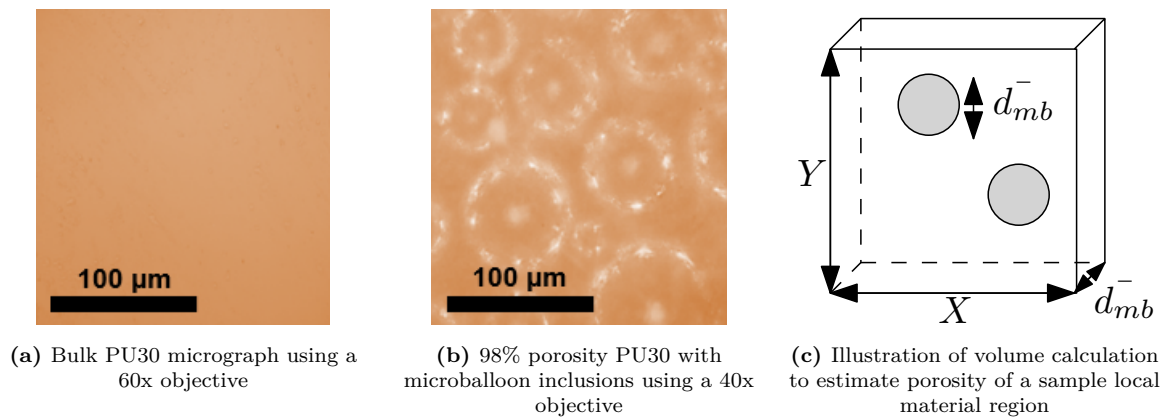


Figure 4.4: (a) Optical micrographs of bulk polyurethane Shore hardness 30 A (PU30) and (b) with the addition of microballoons. d_{mb}^- : Mean microballoon diameter in image.

For each material, optical micrographs were obtained for random sample regions. For the bulk material (Figure 4.4(a)), no air bubbles at the micron scale were visible, indicating negligible matrix porosity and the effectiveness of the degassing method. With the addition of the hollow particulate (Figure 4.4(b)), the sample micrographs indicated that the materials were homogeneous and isotropic. Note that these optical observations were later validated in the mechanical characterisation of these materials (see Chapter 6). It was also possible to estimate the porosity of the materials from the micrographs to validate the theoretical calculation of the necessary microballoon mass needed to achieve the target porosity. Indeed, given the closed-cell nature of the microballoons, the pycnometer gas displacement measurement method was not applicable. Using the circle finding algorithm (see Figure 4.3(a)), the void volume for each sample micrograph could

be estimated as:

$$V_{void} = \frac{4}{3}\pi r_{mb}^{-3}, \quad (4.2)$$

where

$$r_{mb}^{-} = \frac{\bar{d}_{mb}}{2} \quad (4.3)$$

was the average microballoon radius in the image. The porosity for the sample could then be estimated from the Figure 4.4(c) picture as the ratio of the void volume V_{void} to the total sample volume (V_{tot}):

$$\phi = \frac{V_{void}}{V_{tot}}, \quad (4.4)$$

where

$$V_{tot} = XY\bar{d}_{mb}. \quad (4.5)$$

A summary of the measured porosities is shown in Section 4.2.2.

Target ϕ (%)	Measured ϕ (%)	Relative Density, ρ^*/ρ_s (%)
94	94.0 ± 0.3	6.0 ± 0.3
96	96.0 ± 0.2	4.0 ± 0.3
98	98.0 ± 0.2	2.0 ± 0.2

Table 4.2: Polyurethane syntactic foam porosities (ϕ) estimated from optical micrography. For each target porosity, 10 independent optical micrographs were collected from separate syntactic foam samples. The measured porosities were computed as the mean of the estimated porosity for each of the 10 samples. The errors were computed as the standard error of the mean. ρ^* : Foam bulk density, ρ_s : Foam skeletal density.

The estimated porosities were found to agree with the target porosities calculated from Eq. (4.1) and were statistically distinct. No variation in these results for the bulk and porous samples were found from sample-to-sample, between batch preparations or for different casting volumes.

4.3 Hydrogels

Hydrogels are materials that exhibit a molecular-scale matrix of hydrophilic polymeric chains that entrap large quantities of water or other fluids [157, 158]. Conventionally, hydrogels are widely used in tissue engineering for encapsulating cell cultures in a three-

dimensional platform. However, hydrogels may also be of interest for physical applications due to the ability to tune their mechanical properties, high porosity and ease of casting [158]. Absorbance spectrum measurements indicate that pore sizes are in the range of 100–600 nm [159].

In this work, agarose hydrogel was used (see Section 4.3.1). Agarose is derived from the extraction of the natural polysaccharide polymer from the gelling fraction of marine algae [160]. Agarose dissolves in water as an aqueous solution at 90 °C and takes the form of randomly orientated copolymer chains of 1,4-linked 3,6 anhydro- α -L-galactose and 1,3-linked β -D-galactose [160]. Under cooling, the hydrogel is thermosetting and undergoes gelation at 38 °C. This gelling mechanism has two parts. Firstly, the copolymer chains shift from random coils to intermolecular double helix structures bound by non-covalent bonds including hydrogen bonds and van der Waals forces [160]. Following this, the double helices aggregate into larger structures, corresponding to the solidification of the mixture. This process is reversible under heating since the non-covalent nature of the bonds allows them to be broken down again at the gelling temperature (and re-established upon cooling). Agarose is attractive to study as small changes in its concentration result in substantial changes in the resulting hydrogel’s mechanical properties (see Chapter 6). Typical hydrogel concentrations are in the range of 0.5% (w/v) to 3% (w/v), indicating that these materials are predominantly constituted of water. Given the promise demonstrated in previous research for water to mitigate blast peak pressure, hydrogels may have also be effective in this application due to analogous energy dissipation through the breakdown of the hydrogel to increasingly small length-scale particles. It is also possible to enhance this process by considering inhomogeneous hydrogel variants that introduce additional porosity at a macroscopic length scale (see Section 4.3.2).

4.3.1 Agarose Hydrogel

In this research, three agarose concentrations of 1.5% (w/v), 2.0% (w/v) and 2.5% (w/v) were investigated (Roche Agarose MP multi purpose, Sigma-Aldrich). The specified

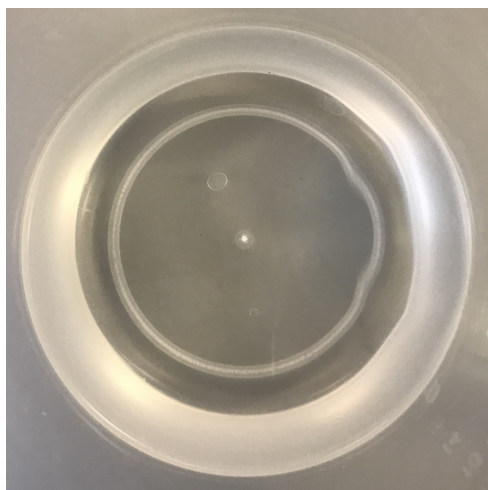
gelling and melting temperatures of this agarose were $38 \pm 1^\circ\text{C}$ and $88 \pm 1^\circ\text{C}$, respectively. In order to obtain an accurate concentration, it was necessary to avoid loss of water due to evaporation of the heated solution when dissolving the agarose. In order to do this, the agarose was mixed with the requisite quantity of distilled water by mass in a heat-resistant borosilicate vessel that was then sealed and heated in a microwave oven until dissolution of the white agarose powder was achieved. As with the polyurethane, it was important to degas the dissolved solution to avoid introducing air into the final hydrogel. The thermal regulation property of aqueous solutions implies that the solution dissolves less gas with increasing temperature according to Henry's Law which states that the dissolved gas in the liquid phase is proportional to its partial pressure. Hence, degassing was achieved by submerging the heated vessel in a pre-heated water bath held at 70°C for 30 minutes. Following this, the water bath temperature was reduced to 40°C and the solution allowed to cool down to this temperature (close to its gelling temperature). It was important to control this cooling rate since the mechanical properties of the hydrogel have been shown to depend on this [160]. The hydrogel was held at this temperature for 10 minutes to ensure thermal equilibrium between the water and solution, mediated by the vessel walls. This enabled the solution temperature to be known from measurement of the water bath temperature without introducing the inconsistency of unsealing the vessel and losing water to evaporation. Following this, the solution was cast into its mould and allowed to cool to its gelling temperature of 38°C and then room temperature. The hydrogels were then demoulded, placed in distilled water to maintain hydration of the polysaccharide matrix, and then kept in cold storage at 5°C .

4.3.2 Hydrogel Beads & Two-Phase Hydrogel

In addition to the homogeneous hydrogels, two-phase hydrogels were also investigated. In this case, regions of more compressible (lower concentrate) hydrogel were introduced to the stiffer bulk hydrogel matrix (higher concentrate) analogous to a syntactic material. In order to prepare these materials, 1.0% (w/v) agarose particles ('agarose beads') were

introduced to the bulk agarose at a concentration of 20% (w/w).

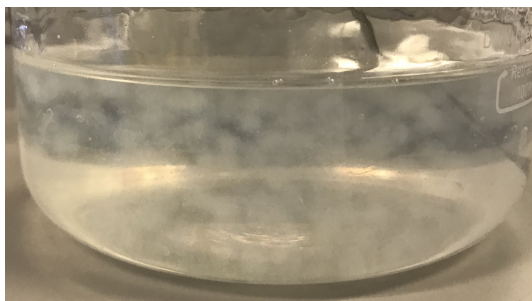
Agarose beads can be prepared by emulsifying the aqueous agarose phase (above its gelling temperature) and a hydrophobic water-immiscible continuous phase. A surfactant can be added which acts to minimise particle coalescence by reducing the energy needed to produce small drops as well as introducing a repellent charge on the particle surface [161]. In this work, this phase was mineral oil (M3516 Light Mineral Oil, Sigma-Aldrich) mixed with polysorbate non-ionic surfactant (P1754 Tween[®] 80 Viscous Liquid, Sigma-Aldrich). The mineral oil had a specified density of 0.84 g ml^{-1} at $25\text{ }^{\circ}\text{C}$. The 1% (w/v) agarose was prepared as outlined in Section 4.3.1, but following removal from the water bath, was added to chilled mineral oil ($5\text{ }^{\circ}\text{C}$) at a ratio of 1:4 by mass and stirred vigorously and continuously for 60 s. The purpose of this was to ensure rapid gelation of the $40\text{ }^{\circ}\text{C}$ agarose on addition to the mineral oil, stabilising the particles and resulting in highly spherical, soft agarose beads. Due to the oil's hydrophobic property, the agarose rapidly formed a suspended particulate, with the greater mass of oil ensuring that the solution is fully emulsified. Following this, the mixture was sieved and washed to remove any excess mineral oil. The agarose beads were then stored in distilled water and kept in cold storage at $5\text{ }^{\circ}\text{C}$.



(a) Emulsification of hydrogel and water-immiscible oil with surfactant



(b) Separated agarose beads following draining and washing



(c) Agarose beads suspended in 2% (w/v) agarose (above gelling temperature)

Figure 4.5: Preparation of 1.0% (w/v) agarose beads and two-phase hydrogel. A small difference in refractive index between the beads and bulk hydrogel allows the suspension to be visible prior to gelation.

The surfactant concentration was the determining factor of the agarose bead particle size distribution with a greater concentration correlating with a smaller average particle size. Current theories linking particle size to surfactant concentration rely only on reduction of interfacial tension, but it is known that the surface charge induced by the surfactant is a more significant effect [161]. As such, this relationship must be determined experimentally. The agarose bead preparation process was carried out for Tween[®] 80 surfactant concentrations in the range of zero to 0.100% (w/w) and the resulting particle sizes measured by optical microscopy of approximately 500 particles and applying the Hough transform circle finding algorithm. The effect of surfactant concentration on the resulting particle size distribution is shown in Figure 4.6.

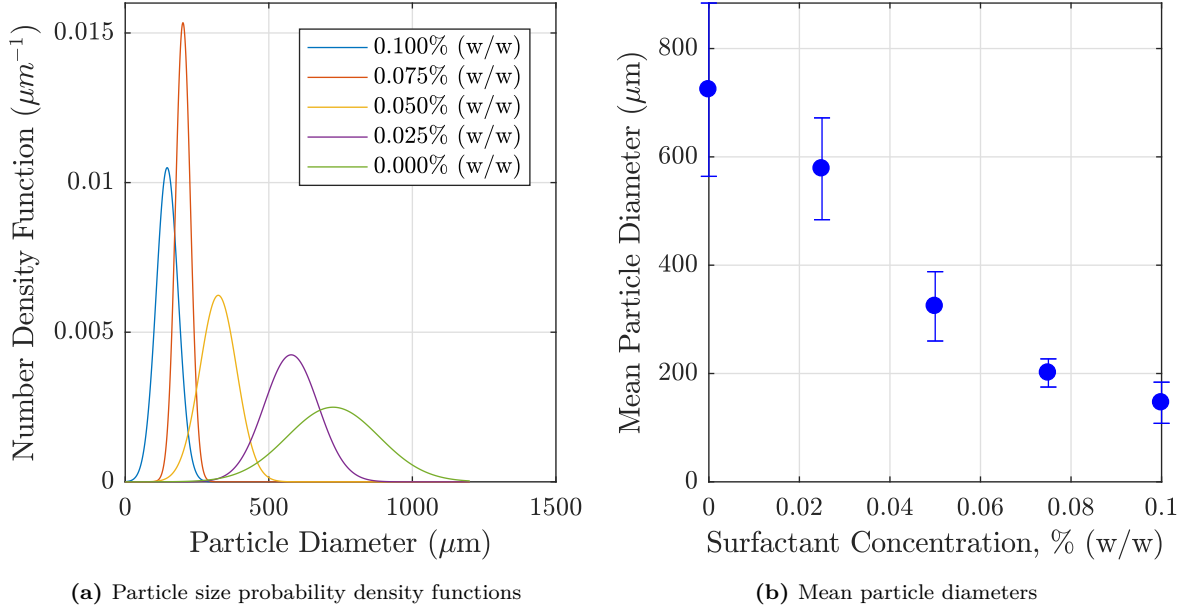
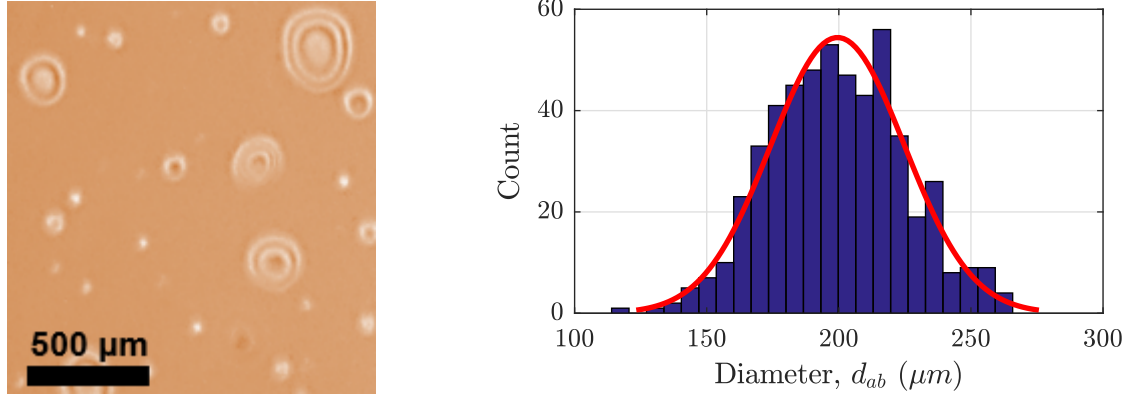


Figure 4.6: Effect of surfactant concentration on mean agarose bead particle size. The diameters were measured from optical microscopy of approximately 500 particles for each surfactant concentration. In (a), normal number density functions were computed to represent the spread of particle diameters. The mean diameters for each surfactant concentration are shown in (b). The uncertainties were computed as the standard error of the mean for each surfactant concentration. Note that the standard deviation for each data point is approximately equivalent to the half width of the number density function peak in (a) at 60% of the full height.

Increasing surfactant concentration corresponded to decreasing average agarose bead particle diameter, d_{ab} , as the surfactant suppresses coalescence. With no surfactant added, the manual stirring became the dominant factor determining the particle size distribution. As such, the variation of particle size was greatest with no surfactant. With increasing surfactant concentration, this uncertainty reduced and the particle size distributions narrowed to a minimum at a concentration of 0.075% (w/w). These measurements agree closely with those of Mu *et al.* [161]. However, this uncertainty increased again at the highest concentration of 0.100% (w/w) ($d_{ab} = 146 \pm 38 \mu\text{m}$) where it was observed that particle coagulation was occurring and likely responsible for the increased variation of particle size. As a result, to ensure reproducible samples with a well-known particle size distribution, the concentration of 0.075% (w/w) was used for preparing the two-phase hydrogel samples. The particle size measurements for these agarose beads are shown in Figure 4.7.



(a) Optical micrograph of agarose beads with 40x objective (b) Agarose bead diameter distribution with Gaussian fit

Figure 4.7: Particle size measurements of the agarose beads investigated in this work, prepared with a Tween 80 surfactant concentration of 0.075% (w/w).

The resulting agarose bead particle size distribution yielded values of $d_{10} = 162 \mu\text{m}$, $d_{50} = 198 \mu\text{m}$ and $d_{90} = 238 \mu\text{m}$, with a mean particle diameter of $d_{ab} = 201 \pm 26 \mu\text{m}$ where the uncertainty was given by the standard error of the mean.

4.4 Ballistic Armour

In order to assess the blast mitigation performance of ballistic protective equipment, it is necessary to obtain an understanding of modern body armour design. The UK Centre for Protection of National Infrastructure (CPNI) recommends body armour for police forces likely to experience stabbing, slashing or ballistic attacks, where GSW trauma represents the most complex threat [13]. Armour presents an optimisation problem; it is not possible to protect all areas of the body so vulnerable areas must be prioritised and ergonomic factors accounted for (*e.g.* seated personnel may require a shorter jacket than predominantly standing personnel). In the military sphere, a representative system is the OSPREY armour system issued to the UK Armed Forces, first introduced during the Iraq conflict in 2006 and now in its fourth iteration [14]. Note that Virtus also exists as a lighter armour system developed by the UK Ministry of Defence (MoD).

The objective of such armour systems is to prevent penetrating trauma to the thorax and abdomen. This is prioritised because bleeding from penetration of the thoracic cavity

cannot be effectively treated by application of compression, through modern haemostatic dressings may be able to delay complications or death such that the casualty can be transmitted to a treatment centre [14]. From the 1970s, a greater, less arbitrary, emphasis was placed on targeting protection to the most vulnerable bodily structures, resulting in armour systems incorporating both soft and hard armour [14, 15, 17, 162]. Soft armour consists of woven, flexible aramid fibres (*e.g.* Kevlar® or Twaron®) designed to defeat secondary blast trauma from fragments. Conversely the hard armour plates are intended to defeat high energy bullets, typically composed of rigid ceramic or composite materials.

An early incarnation of such a system was the Improved Northern Ireland Body Armour (INIBA) system which utilised rectangular front and rear hard plates in combination with soft armour [14]. This was the armour system casualties were equipped with in the Cooper *et al.* study of primary blast injuries sustained in Northern Ireland [25]. This system was not developed with a precise anatomical protection remit. Since then, the heart and mediastinum have been identified from vulnerability studies [14, 15]. An additional change from the INIBA system was that the expectation time for medical treatment of casualties increased from 20 minutes to 1-2 hours, significant because this required the armour system to also provide protection to the spleen and liver. The OSPREY system was designed to meet these needs while representing a compromise between the protection and mobility afforded to the wearer (see Figure 4.8 and Figure 4.9). Additional physiological factors defined by the CPNI include physical bulk of the armour, flexibility, heat burden, fit and comfort [13].



Figure 4.8: Mark 4a OSPREY armor system (A) carrier made of Cordura® 1000D nylon fabric. This system includes (B) contoured hard front and rear ballistic plates, (C) side ballistic plates and (D) filler soft armour designed to prevent fragmentation impact injuries. Reproduced from [14].

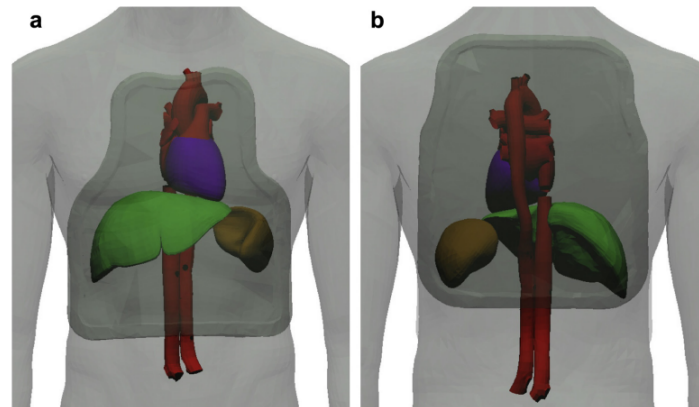


Figure 4.9: Front (a) and rear (b) OSPREY plates superimposed on anterior and posterior views of areas defined as required essential medical coverage (heart, great vessels, liver and spleen) . Note that the exposed lower region of the aorta would be covered by the soft armour component of the OSPREY system. Reproduced from [14].

Significant research has been carried out with the intention of better informing the anatomical distribution of armour [14–16], with much effort devoted to combat face, neck and ocular injuries [163, 164]. Further work has considered the penetrating injury prevention capability of the system, consisting of retrospective reviews of wound incidence

[162] as well as ballistic testing [165].

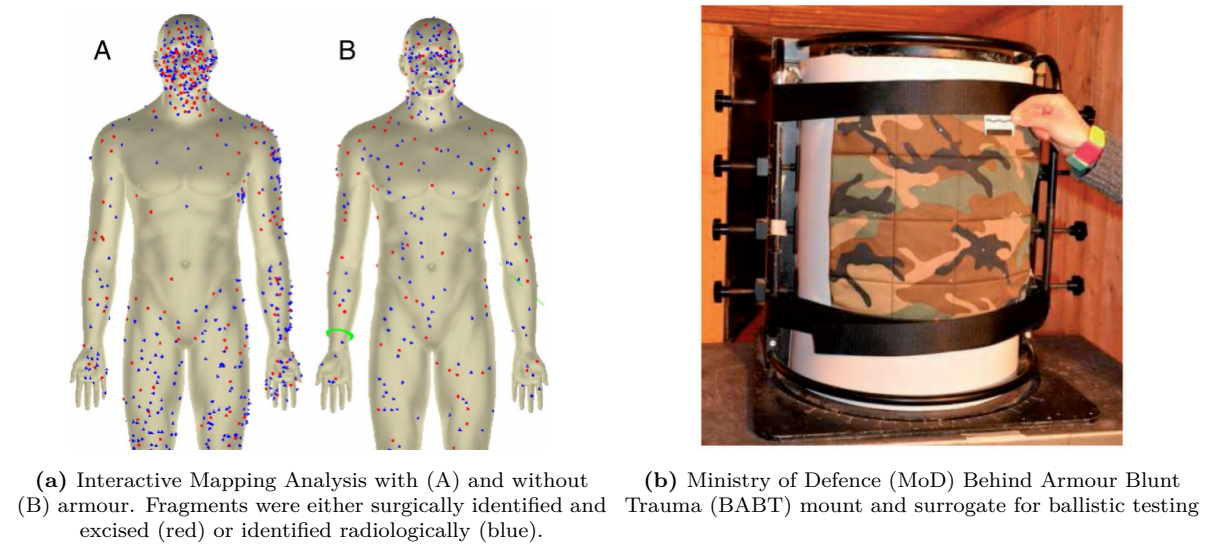


Figure 4.10: Studies of ballistic armour protection. A: computational surface wound location analysis of UK and North Atlantic Treaty Organisation (NATO) casualties in Afghanistan (2012), reproduced from [162]. B: ballistic testing mount of soft and hard armour plates, reproduced from [165].

From the Figure 4.10(a) analysis by Breeze *et al.*, it was found that unprotected personnel were 4.1 times more likely to sustain injury to the thorax or abdomen than those equipped with the OSPREY armour system (AIS of 3.9 compared with 2.9) [162]. The BABT rig shown in Figure 4.10(b) consists of a silicone rubber surrogate for the thorax derived from studies of the thoracic deformation of porcine models under penetrating impact [166]. This rig was proposed as an alternative to the widely-used NIJ surrogate of a clay mass, where post-impact static indentation less or greater than 44 mm constituted a dichotomous pass or fail result, respectively. The BABT primarily aims to reproduce dynamics resulting in surface penetration of the thorax such as the retardation of the projectile on impact to the armour and resulting fragmentation from spall [166].

In this work, measurement of the effectiveness of the ballistic plate against blast overpressure is required, necessitating an armour-surrogate system appropriate for deeper stress wave transmission into the thorax *i.e.* a subdermal tissue simulant. As the loading is non-penetrating, the armour could be treated as a rigid body and its dynamic response neglected, though its mass and geometry must be representative. In this work, due to the concentration on BLI, a front-facing Dyneema[®] ballistic plate conforming to NIJ

Standard 0101.06 was investigated (Nexus NIJ Level 3+ Multi Hit Lightweight Ballistic Plate, double curve front, UK Tactical). This is a multi-hit standard using the NIJ clay indentation surrogate, requiring the plate to defeat a minimum of 3 7.62×51 mm NATO ball rounds at a velocity of $847 \pm 9 \text{ m s}^{-1}$ or 6 7.62×39 mm AK-47 Mild Steel Core (MSC) rounds at 710 m s^{-1} [167].

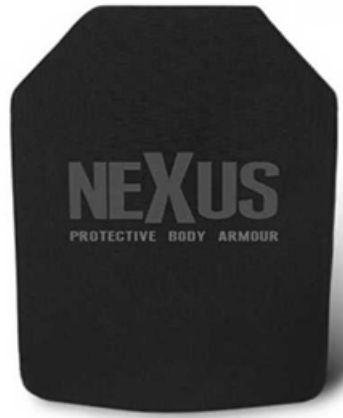


Figure 4.11: Monolithic composite Dyneema® ballistic plate investigated in this work under blast loading. The plate complies with National Institute of Justice (NIJ) Standard 0101.06. The plate's dimensions were $200 \times 300 \times 20$ mm with a total mass of 1.35 kg.

4.5 Gelatine

In this work, a biofidelic gelatine tissue simulant was used as a surrogate for the thorax. Gelatine blocks are widely used in lethality and survivability studies as a material representative of part of the body [168, 169]. In the wound ballistic community, such work has been predominantly focussed on projectile Depth of Penetration (DoP) and energy loss per unit distance. Surrogates typically aim to replicate the thorax or abdomen as military and police are trained to target the body's centre of mass [170]. Though 10 and 20% concentration gelatine are highly common, there is no agreed formulation for surrogate preparation. Gelatine simulants are homogeneous but intended to replicate the average mechanical properties of interest of the heterogeneous anatomical region being studied. Two prevailing specifications are that of the Federal Bureau of Investigation (FBI) and NATO. The FBI specification was based on porcine thigh penetration tests carried out by Fackler and Malinowski [171] and, as such, has been found to replicate the skin, fat,

fascia and muscle tissue of the limbs. Indeed, conventionally, it was been accepted that the tissue characteristics of humans and porcine models are similar. Conversely, the stiffer NATO gelatine concentration has been found experimentally to be more suitable for replicating the thoracic and abdominal anatomical regions [170, 172].

As a result, in this work, the NATO gelatine preparation method proposed by Breeze *et al.* was used [173]. Porcine 20% (w/w) ballistic gelatine was used (Pork Gelatine 300 Bloom Type A, MM Ingredients). Type A gelatine is produced from acid-treated porcine-derived collagen. The Bloom number is a measure of the material stiffness where it is the mass in grams needed for a standard 12.5 mm diameter plunger to indent the gelatine by 4 mm [168]. The requisite mass of dry gelatine powder was slowly added to distilled water at $70 \pm 5^\circ\text{C}$ while being stirred vigorously and continuously for 60 s. Following this, the solution was continuously stirred for 5 minutes, covered and allowed to stand at room temperature for a further 5 minutes to allow degassing. After this period, accumulated foams formed on the surface were removed and then the solution cast into its mould. The gelatine was allowed to cool to room temperature, following which the material was de-moulded and stored in distilled water in cold storage at 5°C .

Chapter 5

Experimental Platforms & Diagnostics

5.1 The CBIS Shock Tube System

5.1.1 Operation

The Royal British Legion Centre for Blast Injury Studies (CBIS) shock tube is an air-driven system comprised of three 1.22 m length stainless steel tubes with an inner diameter of 60 mm as shown in Figure 5.1. The system was originally designed by Dr. D. J. Chapman in 2010 [174] and additional capability has been added since by Dr. T-T. N. Nguyen [44]. One of the three tube lengths constitutes the driver section.

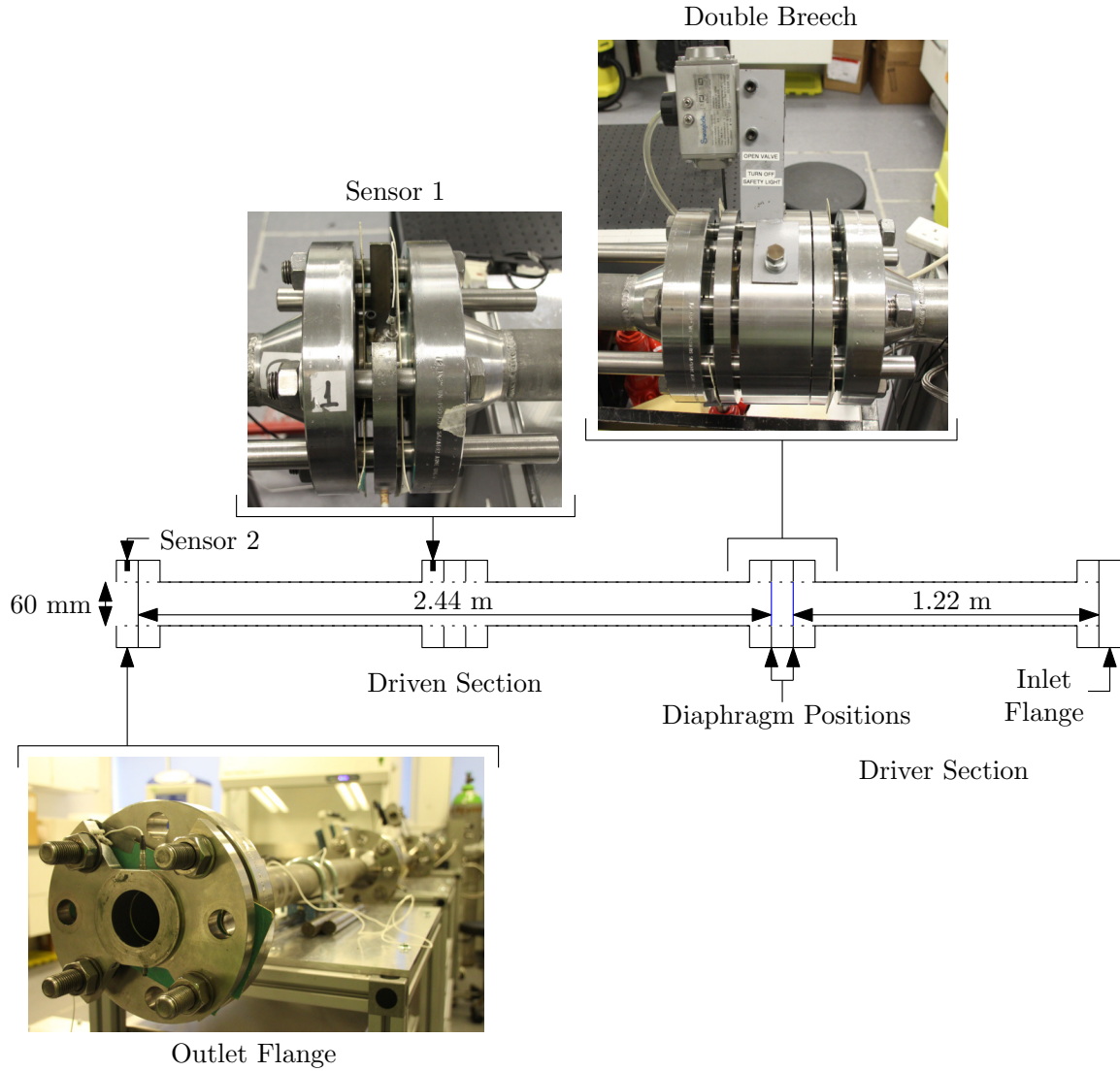


Figure 5.1: Schematic of the Centre for Blast Injury Studies (CBIS) shock tube in an open configuration. The blue lines indicate two possible diaphragm positions. The system can also be closed with a head-on sensor mounted in the end flange to measure the reflected pressure wave.

The tubular sections are bolted through flanges with o-rings and rubber nitrile gaskets placed between metal surfaces to ensure an airtight system (the green material in Figure 5.1). Polyester film (Mylar®) was used as the diaphragm material. Mylar® is an appropriate material as it is available in a variety of thicknesses from 23–250 μm , allowing the peak blast pressure to be increased linearly with diaphragm thickness (see Figure 5.2). Mylar® is also manufactured to a highly consistent ($<1\%$ error) thickness, aiding reproducibility.

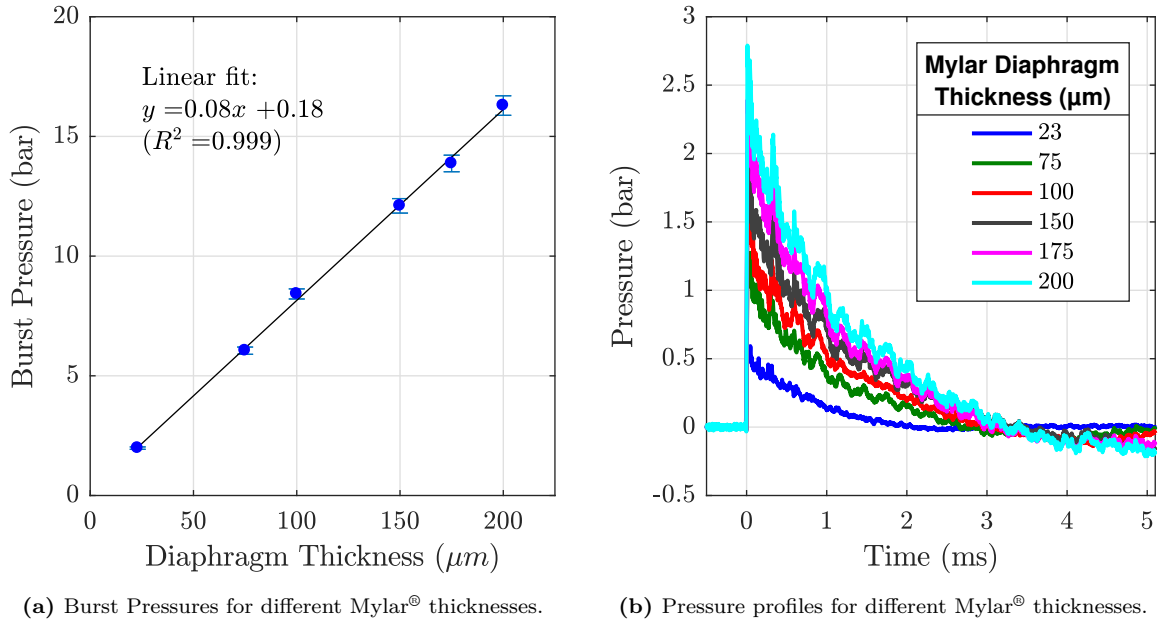
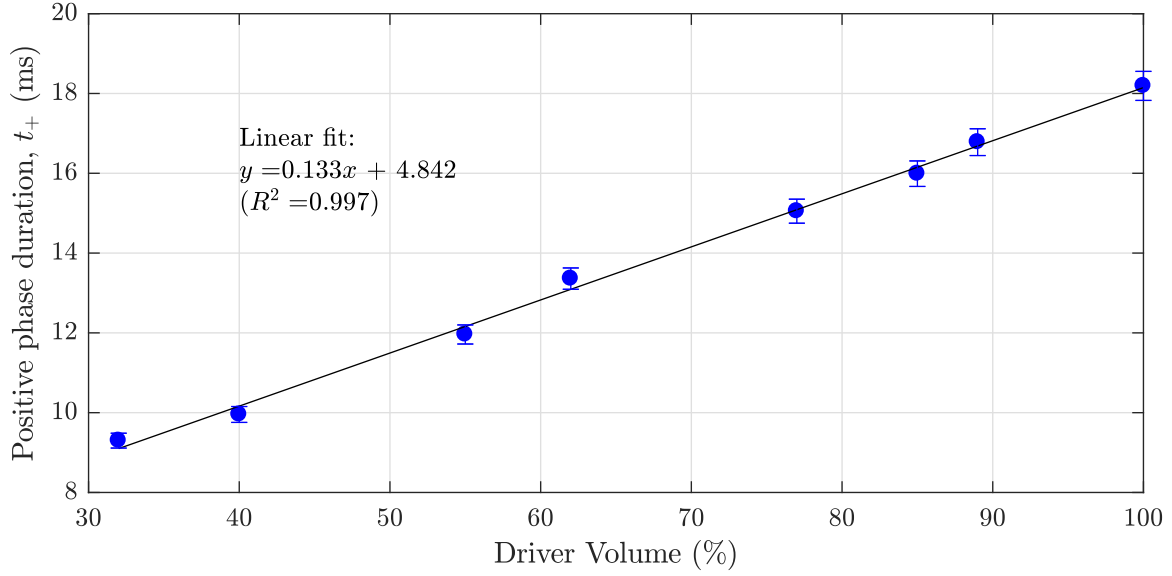


Figure 5.2: (a) Calibration of Mylar[®] thickness to burst pressure averaged over 5 repeat shots. The errors were computed as the standard error of the mean. (b) Sensor 1 pressure profiles measured 1.22 m from the diaphragm position for varying diaphragm thickness.

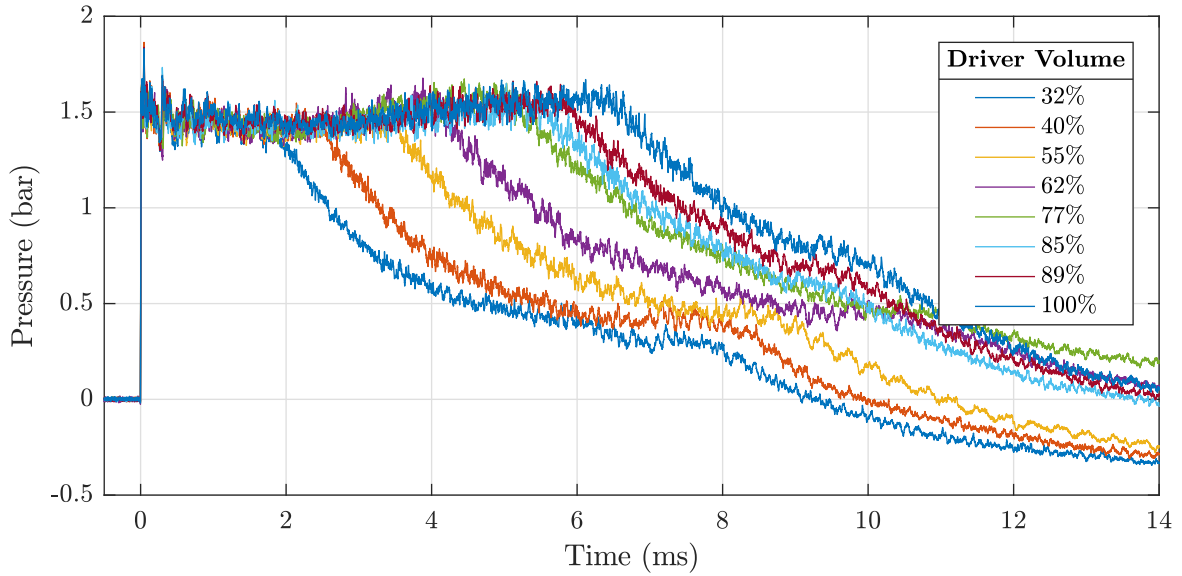
During charging of the driver section, the diaphragm bulges prior to failure due to rapid crack propagation originating from the centre of the diaphragm where the greatest tensions exists assuming no pre-damage is present [44]. Mylar[®] is preferred for this system compared to alternative diaphragm materials due to its low fracture toughness of approximately $1 \text{ MPa m}^{1/2}$ compared to (*e.g.*) $24 \text{ MPa m}^{1/2}$ for aluminium [44]. As a result, polyester film has a faster crack propagation and burst times.

While the diaphragm material controls the blast peak pressure the driver section length determines the loading duration. In the CBIS shock tube system, this can be adjusted by shortening the driver section with polypropylene inserts (see Figure 5.3(b)) in multiples of 10 cm increments [44]. The inserts are secured together by double-headed bolts and inserted into the driver tube from the inlet flange end, with a 6.5 mm tunnel running through the insert assembly to allow charging. The minimum driver length in this system corresponds to the double breach length only (9 cm), sealing off the rightmost driver tube entirely. As such, this minimum driver length is referred to as ‘blanking volume’. Longer driver lengths correspond a greater plateau pressure as there is a greater volume of gas, or blast wind, following the blast front. The system allows the incident

loading duration of the plateau to be varied between approximately 1–7 ms.



(a) Blast positive phase duration for different shock tube driver volumes.



(b) Pressure profiles for different shock tube driver volumes.

Figure 5.3: Variation of incident wave loading duration by varying the shock tube driver volume. A fixed diaphragm burst pressure of 12 bar was used. In the laboratory, the volume was adjusted by adding polypropylene inserts to the driver section. The driver volume is expressed as a percentage of the maximum volume using the full driver length of 1.22 m (see Figure 5.1). The minimum 32% driver volume corresponds to using the shock tube double breach only as the driver and sealing off the rightmost driver tube, termed ‘blanking volume’. In (a), it can be seen that the positive phase blast duration increases linearly with the driver volume. The values were computed as the average over 5 repeat shots with the errors computed as the standard error of the mean. In (b), the measured Sensor 1 pressure profiles 1.22 m from the diaphragm position are shown.

It can be observed that the system allows independent variation of the loading duration without a change to the peak pressure. Hence, shortening of the loading duration

corresponds to a lower positive phase impulse delivered to the target. This is advantageous for blast injury studies as different pathologies are unique in whether they are more sensitive to peak pressure, impulse or loading duration.

In operation, the driver section is pressurised via a compressed air cylinder and flexible pressure hoses through the inlet flange. For safety reasons, the maximum driver pressure is limited to 18.2 bar. The system can be operated in either single diaphragm or double diaphragm configuration. In single diaphragm mode, the driver section is gradually pressurised to the diaphragm's burst pressure. In double diaphragm mode, the driver is set at the desired burst pressure while the double breech (the region between the two diaphragms) is held at half the burst pressure. By ensuring that the differential between the driver section and double breech remains lower than the diaphragm's burst pressure, the system can be fired by rapidly venting the double breech with a solenoid valve. However, this configuration introduces additional peaks to the resulting loading pressure profile as there is a finite time for the double breech to evacuate and this adds complexity to the contact surface between the driver and driven gas. As such, in this work, the single diaphragm mode was used. When securing the Mylar[®] diaphragm, it was essential to fasten the double breech bolts in a diagonally opposite pattern to avoid gas leakage during charging. The diaphragm was clamped against o-rings to avoid introducing damage to the diaphragm from contact with the sharp edges of the inner tube.

5.1.2 Pressure Diagnostics

The sensors used to measure the blast pressure profile were Dytran[®] 2300V1 piezoelectric Low Impedance Voltage Mode (LIVM) dynamic pressure sensors, positioned laterally as Sensor 1 and Sensor 2 in Figure 5.1. The mechanical force from the passage of the pressure wave exerted on the stack of quartz crystals in the sensor generates a linearly proportional strain and charge that is recorded as a voltage signal on an oscilloscope (Tektronix[®] DPO4104B) via a current source. The reading from the first sensor (Sensor 1) was used to trigger the oscilloscope as it is sufficiently distant from the diaphragm

(greater than 4 tube radii) for the shock front to have formed (see Section 2.6), as well as independent from interactions with experimental instrumentation or rigs at the outlet. Each sensor is dynamically calibrated by the manufacturer and have sensitivity values of approximately 20 mV psi^{-1} . The sensors were mounted flush to the inner surface of the shock tube and exhibited a rise time of approximately $1 \mu\text{s}$, sufficiently fast for blast loading which occurs on a millisecond timescale. Any protrusion of the sensor into the shock tube results in greater noise due to mechanical vibration from loading lateral to the piezoelectric crystal stack. The top mounting of the sensors has been previously validated on the basis that the pressure profile is axially symmetric in the system [44].

The small sensing area diameter of 4 mm and rigid steel construction of the sensors gives them high frequency sensitivity of the order of 1–10 MHz. As such, pressure data from the Dytran[®] V1 sensors were signal processed by applying a low-pass filter with a 70 kHz cut-off frequency. The smoothing effect of this filtering is shown in Figure 5.4 for an example 10 bar burst pressure shot incident wave (Sensor 1).

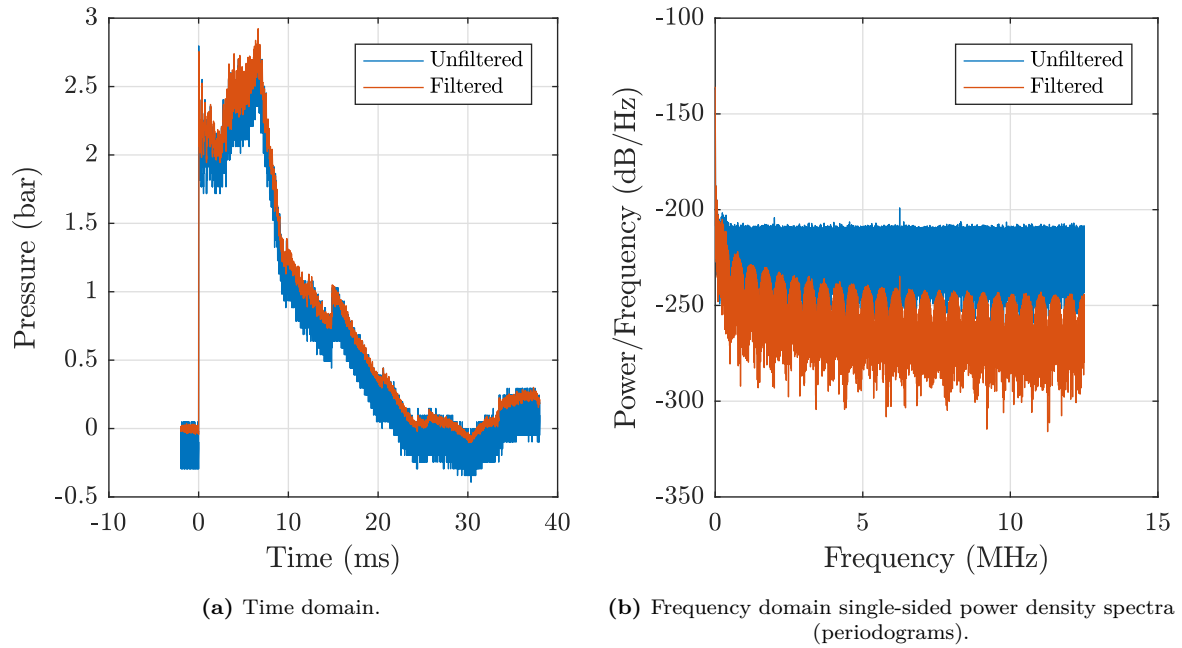


Figure 5.4: Signal processing of shock tube pressure diagnostic data. The filter acts to strongly suppress the significance of frequencies above the 70 kHz low-pass cut-off.

For the gelatine surrogate experiments, a Dytran[®] 2300C4 dynamic piezoelectric pressure sensor was used due to its water-resistance enabling it to be embedded in the gelatine.

This sensor was equal in dimensions and response characteristics to the Dytran® V1 sensors, but operates in charge mode with a sensitivity of 0.30 pC psi^{-1} . As such, this sensor was connected to the oscilloscope via a Dytran® 4020 charge amplifier in piezoelectric mode to convert the signal to a voltage.

An important characteristic of the blast wave in this project was the blast peak pressure. Due to the rapid rise time of the blast wave, the mechanical noise associated with this resulted in the peak overshooting the true peak pressure. This noise was due to the combination of high frequency waves in the shock tube walls and high frequency vibration of the sensor piezoelectric stack. In order to improve consistency between shots and more accurately compute the peak pressure for a given blast wave, the Friedlander waveform (Eq. (2.5)) was fitted to the subsequent positive phase pressure decay and the peak pressure inferred from the fit. This is shown in Figure 5.5.

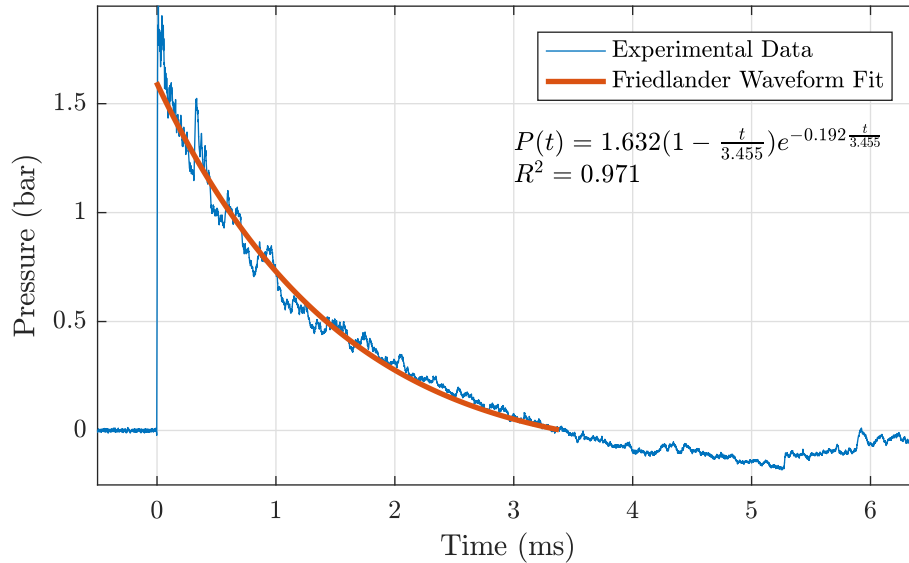


Figure 5.5: Fitting of the Friedlander waveform to the positive phase of a pressure profile from a 10 bar burst pressure and 9 cm driver length open shock tube shot. High frequency noise can be seen in the experimental data due to wave propagation in the shock tube walls and noise introduced by lateral oscillations of the piezoelectric pressure diagnostic. To improve consistency of the calculated peak pressure between shots, this was computed from the value of the fit at the start of the blast wave ($t = 0$ s in this example).

It can be observed from the close fit of the Friedlander function in Figure 5.5 that the shock tube closely replicates the physics of an open-field freely propagating blast wave from a spherical charge. In this example, the Friedlander function provides the

estimate that the peak pressure $P_s = 1.63$ bar, $t_+ = 3.45$ ms and $b_s = 0.19$. Note that the atmospheric pressure term of Eq. (2.5) can be neglected as the gauge pressure was measured in the laboratory. The peak pressure value is less than the simple peak experimental pressure value (1.95 bar) due to the noise present after the rapid rise of pressure. This method of obtaining the peak pressure was considered more accurate and reproducible than this value as it utilises the physics of the subsequent rate of pressure release.

5.2 Universal Testing Machine

Universal Testing Machine's (UTMs) are widely used for mechanical characterisation of materials at quasi-static strain rates in the range of 10^{-5} – 1 s $^{-1}$. These platforms are highly versatile and capability of carrying out compression, tension, relaxation, cyclic and creep tests under a wide range of conditions. UTMs can range from large and high load capacity platforms appropriate for structural engineering to high sensitivity machines suitable for soft material testing. A UTM utilises a high stiffness frame and a crosshead that is translated by servo-hydraulic or electromechanical actuation. The frame rigidity, power and maximum crosshead speed are considerations for testing of a given sample. In this work, an electromechanical motion Instron[®] 5566 UTM was used for uniaxial compressive characterisation of the materials discussed in Chapter 4.

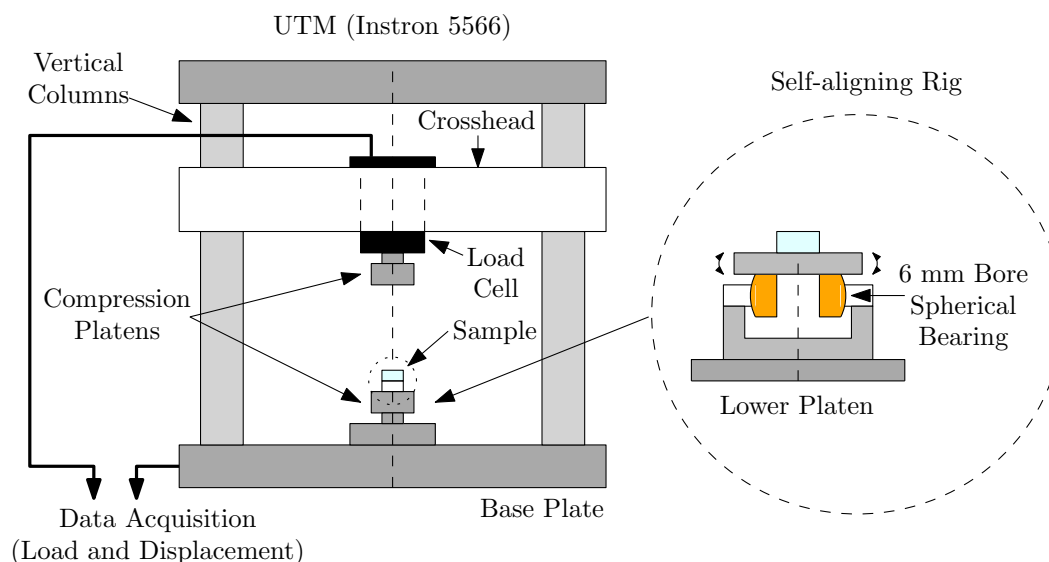


Figure 5.6: Schematic of the electromechanical Instron® 5566 UTM with a self-aligning rig. In compression testing, the crosshead is actuated downwards such that the sample is loaded between the stainless steel platens.

A self-aligning rig was developed to compensate for misalignment of the rig, bonded by epoxy resin to the lower platen. This consisted of a lubricated 6 mm bore stainless steel spherical bearing, freely rotating on a hollow rigid tube. A platen was created by bonding a 30 mm diameter, 10 mm thick, stainless steel platen to the upper surface of the rotating bore with epoxy resin. To align the system, the alignment rig was covered with epoxy, placed *in situ*, and gently compressed by the upper platen. The rig was held for 1 hour to allow the epoxy resin to cure, preserving the angle of the rig and ensuring parallelism of the compression platens for subsequent material testing.

In this work, a 50 N and 10 kN capacity load cells were used. The force history from the load cell and the displacement and time history from the motor encoder are fed to a computer for data acquisition. Subsequent data analysis requires knowledge of the frame compliance. This is especially important for low-strength materials where small force errors constitute a greater relative uncertainty. An assumption in mechanical testing is that the loading apparatus and instrumentation have infinite rigidity and do not contribute to the strain and force data. However, the UTM exhibits finite compliance under load due to the spring represented by the overall system (load cell, platens, base plate, crosshead). The Instron® 5566 is quoted as having a frame compliance of 0.025 mm kN^{-1} .

(and crosshead positional uncertainty of 0.02 mm). This does not include the compliance of the other components in the system, so will underestimate the deflection. To compensate for this, the non-sample displacement (u_{rig}) was measured and subtracted from the total displacement reading provided by the actuator (u_{tot}):

$$u_{sample} = u_{tot} - u_{rig} , \quad (5.1)$$

where u_{sample} is the displacement of the sample only. Each of these quantities is a function of the force. u_{rig} , the compliance of the system without the sample, was measured for the two load cells and is shown in Figure 5.7 and Figure 5.8.

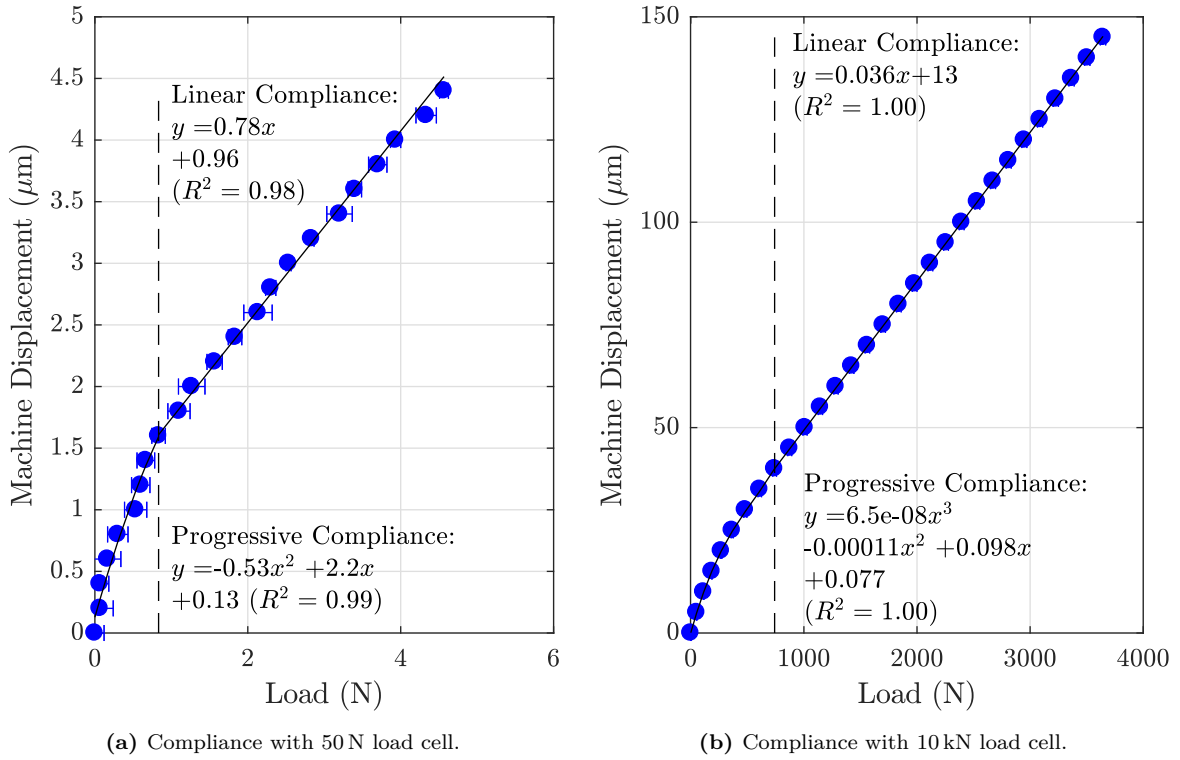


Figure 5.7: Universal Testing Machine (UTM) compliance with platens only. Errors reported as the standard error of the mean for 5 separate compressions. The uncertainty is reduced for the 10 kN load cell due to the greater magnitude of the load.

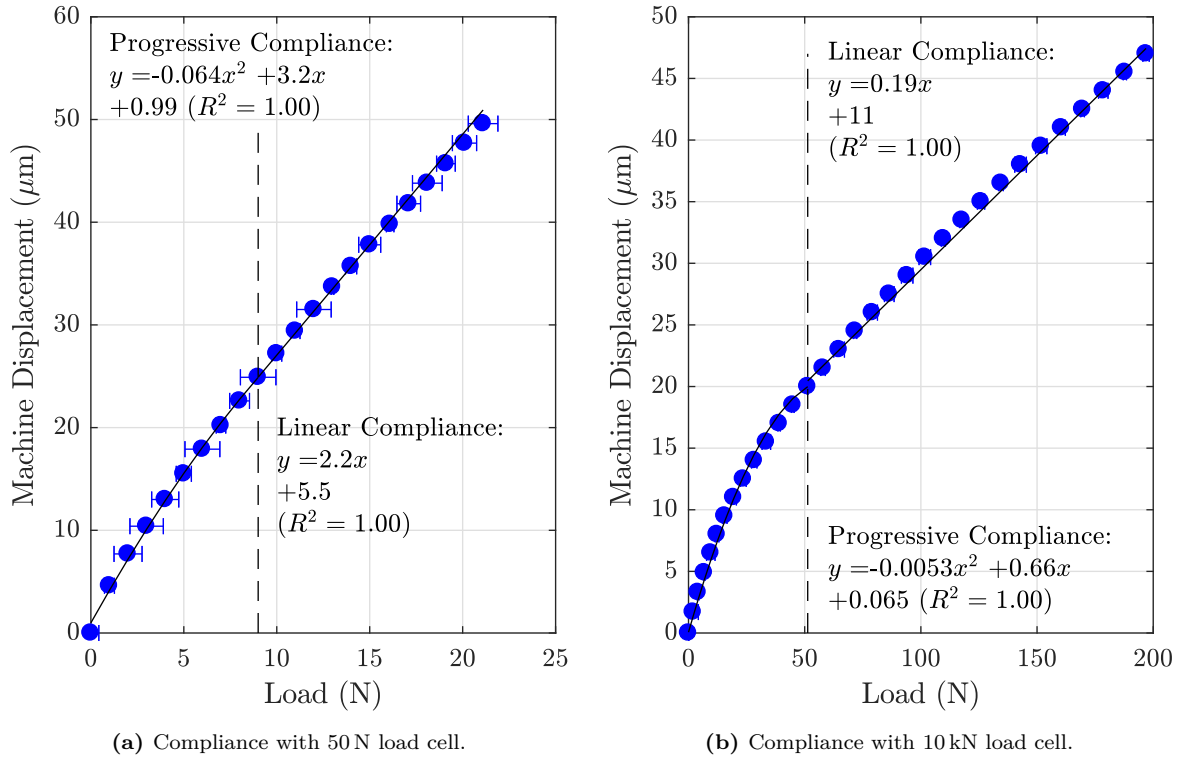


Figure 5.8: Universal Testing Machine (UTM) compliance with self-alignment rig installed. Errors reported as the standard error of the mean for 5 separate compressions. The uncertainty is reduced for the 10 kN load cell due to the greater magnitude of the load.

The initial non-linear (approximately second-order) regime of compliance, termed progressive compliance, is responsible for a large contribution of non-sample displacement at low load and is associated with the settling of the system as the platens make contact. Following this, the compliance response is linear reflecting the spring-like displacement of the combined UTM and rig system. The slopes of these linear regions exceed the 0.025 mm kN^{-1} manufacturer compliance figure as the measured values include the full system. Note that the maximum force used in the compliance test was less than that used with the platens only to avoid exceeding the maximum load of the alignment rig. It can be seen that for equivalent force values, the addition of the alignment rig introduced greater compliance.

5.3 Drop-weight

Drop-weight loading platforms are widely used for mechanical testing at strain rates of 10^1 – 10^3 s^{-1} and typically employ impact velocities in the range of 1 – 20 m s^{-1} [175]. The operating principle involves dropping an impactor mass vertically under gravity through a guidance assembly such that it impacts a sample positioned on a rigid base plate and load cell diagnostic.

Assuming the impactor has mass m , the potential energy (E_0) of the impactor is $E_0 = mgh$, where g is the acceleration due to gravity and h the drop height. During free-fall, this is converted to kinetic energy, reaching $E_k = \frac{1}{2}mv_0^2$ kinetic energy on impact where $v_0 = \sqrt{2gh}$ is the impact velocity. On impact, the impact energy will be partly transmitted to the sample and partly unloaded into the impactor as it rebounds with velocity v_r . As such, the energy absorbed by the sample is given by $E_s = E_k - \frac{1}{2}mv_r^2$. However, this treatment assumes a perfect system that is infinitely rigid, aligned and where there is no energy loss of the impactor. In a real system, the impactor will lose energy through a combination of viscous drag from the air and solid friction with the guiding walls. As a result, it is not practical in the laboratory to accurately infer the energy delivered to the sample from the drop height. Rather, it is necessary to use the appropriate instrumentation to directly measure the mechanical parameters of interest. Indeed, the load cell allows the force history $F(t)$ to be measured and the displacement history can be derived from this by applying Newton's Second Law. Given that the acceleration history is $a(t) = \frac{F(t)}{m}$, the velocity, $v(t)$, and displacement, $u(t)$, can be expressed as:

$$v(t) = v_0 - \int_0^t a(t)dt, \quad (5.2)$$

$$u(t) = \int_0^t v(t)dt. \quad (5.3)$$

Finally, the energy delivered to the sample can then be expressed by the integral:

$$E_s(u) = \int_0^u F(u)dt. \quad (5.4)$$

The impact velocity v_0 is conventionally measured by laser-gate velocimeter. With the force and displacement histories, the stress-strain relationship of the sample can then be computed. It is assumed that the motion of the striker is equated with that of the sample on impact. An additional practical inaccuracy may arise from the fact that the striker impact velocity is sampled by the velocimeter gates a finite distance before physical impact, leading to an underestimation of the measurement.

In the drop-weight experiments carried out in this work, an alternative method for displacement measurement was sought. The original drop-weight system was designed by Dr. D. R. Sory and Dr. T-T. N. Nguyen. A schematic of the system is shown in Figure 5.9.

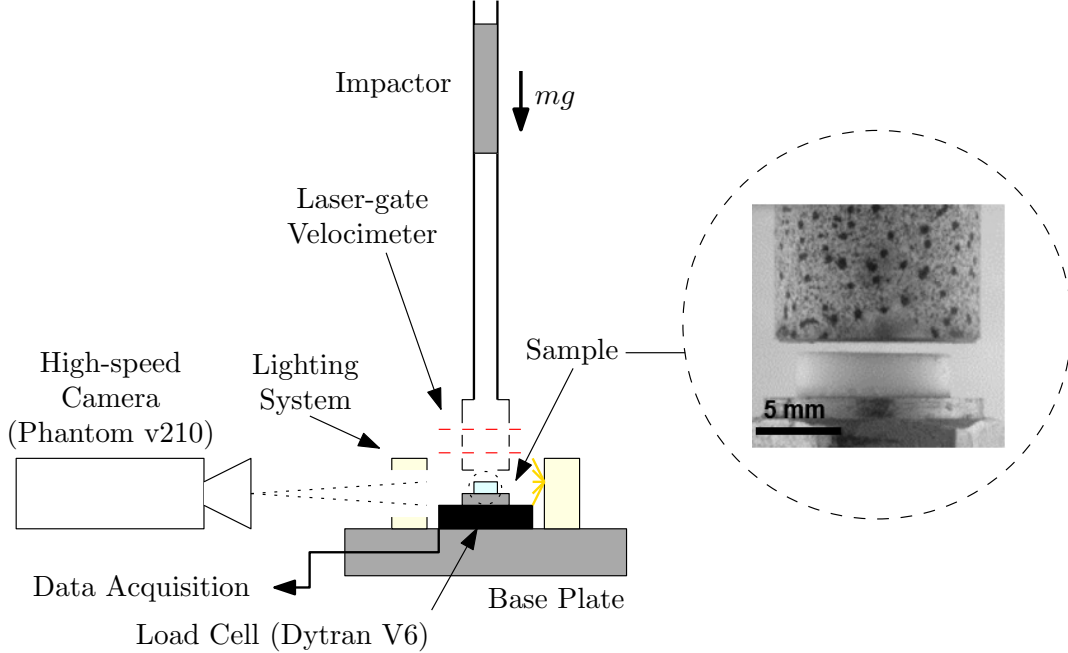


Figure 5.9: Schematic of the drop-weight system. mg : Weight of impactor, where m is the impactor mass and g the acceleration due to gravity.

The system allowed masses of up to 400 g to be dropped from heights up to 1 m. The bore of the guiding rail accommodated impactors of 12.7 mm diameter, where the rail

was constructed of stainless steel and designed to be maximally rigid along with the base plate. Another important function of this rail was to minimise friction with the impactor while allowing the impactor to repeatably impact samples without significant energy loss to the walls during free-fall. The force measurement diagnostic was the load cell positioned beneath the sample (Dytran[®] 1051V6), bolted to the base plate. The load cell had a platen diameter equal to the impactor of 12.7 mm. This transducer was connected to an oscilloscope (Tektronix[®] DPO3054) via a current source. In order to obtain the impact velocity, the system was equipped with light-gates. However, in this work, a high-speed camera (Phantom v210) was used to directly measure this as well as the sample displacement as an alternative to employing Eq. (5.2). In order to accurately track the motion of the striker, Digital Image Correlation (DIC) was implemented by applying a speckle pattern to the impactor as can be seen in Figure 5.9. A detailed discussion of this optical technique can be found in Section 5.5. In order to illuminate the sample, a high-intensity lighting system was designed in collaboration with Mr. G. Lacresse and Mr. B. Verdun. This system utilised 10 60 W LEDs, generating a total luminous flux of 5×10^4 lm with an aluminium construction, heat sinks and fans to manage heat dissipation.

There are several experimental caveats to the drop-weight technique. Firstly, due to the rigid construction of the system, structural vibrations can occur in the system on impact of the projectile. This may cause noise in the force data, so experimenters have recommended applying a low-pass filter with a cut-off frequency in the range of 4–10 kHz to mitigate the resonant frequencies introduced by the projectile motion [176]. In this work, a 10 kHz cut-off was used to condition the load cell data. An additional important consideration is that of strain rate, conventionally required to be constant for mechanical testing. In a drop-weight experiment, the displacement rate and loading rate are not constant and depend upon a wide range of factors that are both system and sample dependent, such as sample geometry, mechanical properties, impact velocity, impact energy and system compliance [177]. A measure to mitigate this is to deliver significantly more energy to the sample than it absorbs (such that there is a significant

rebound), as this renders the strain rate dependence on the sample negligible (particularly for soft samples). As guidance for this, it is recommended to have an order of magnitude greater energy stored in the impactor than will be absorbed by the test sample, given by Eq. (5.4) [177]. To achieve near-constant strain rate, it is preferable to derive the impactor energy from its mass rather than the drop height *i.e.* high mass and low height [177].

5.3.1 Calibration

In order to calibrate loading duration and amplitude to drop-height, the single-loading stress waveforms for the drop-weight system were measured for an impactor mass of 150 g at drop heights of 30 mm, 50 mm, 70 mm, 90 mm (see Figure 5.10).

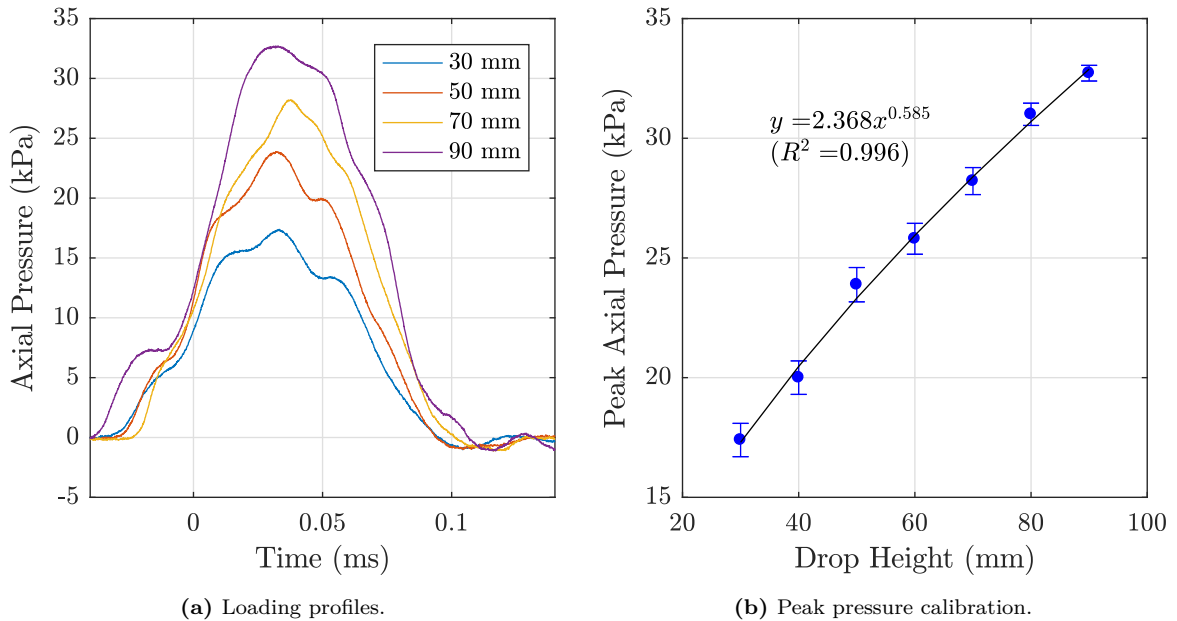


Figure 5.10: Drop-weight axial loading conditions for varying drop height of a 150 g mass impactor. In (b), each data point represents an average of 5 measurements, with the error computed as the standard error of the mean. This error reduced with increasing drop height due to reducing solid friction with free-fall velocity.

For this system, it was observed that the loading duration was approximately independent of the drop height, while the peak pressure could be adjusted by varying the drop height. From a least-square regression, this relationship was well-described by a power law ($R^2 = 0.996$).

5.4 The CBIS Split-Hopkinson Pressure Bar Systems

In this work, two main SHPB setups were utilised. This included a conventional SHPB (as discussed in Section 2.5) appropriate for the higher strength syntactic polyurethane samples, and a high-sensitivity system appropriate for the low impedance open-cell polyurethane and hydrogel samples.

5.4.1 Conventional System

The CBIS SHPB system was originally designed by Dr. D. J. Chapman and has been developed since by Dr. D. R. Sory. The system accommodates 12.7 mm diameter bars and consists of a 4 m long frame which holds a single-stage gas gun of length 1.5 m and inner bore diameter 12.8 mm operated by a high-speed solenoid valve. The frame utilises V-blocks to support and enable coaxial alignment of the bars, while allowing sliding of the bars in the axial direction. A light-gate velocimeter is used at the exit of the gas gun to measure the striker impact velocity. This is composed of laser beams separated by a distance of 44 mm illuminating respective photodiodes, connected to a digital oscilloscope (Tektronix® DPO3054). Aluminium 7068 alloy bars were used. These bars were instrumented with semiconductor (type AFP-900-050, Kulite) strain gauges located at the bar midpoints. The gauges were single-element with two opposing terminal and P-type doped with an approximate resistance of $500\ \Omega$. Each strain gauge (on both the input and output bars) was connected with a strain gauge diametrically opposite to compensate for any bending of the bar. Semiconductor strain gauges are effective for measuring small strain due to their piezoresistive nature as compared to foil gauges, as well as greater linearity and fatigue life. In this work, the aluminium bars were utilised as they exhibit an appropriate impedance mismatch from polyurethane-based materials to yield an OB transmitted signal with satisfactory Signal-to-Noise. A 560 mm length

striker was utilised, yielding a pulse duration of approximately 250 μ s. An illustration of the CBIS system is shown in Figure 5.11.

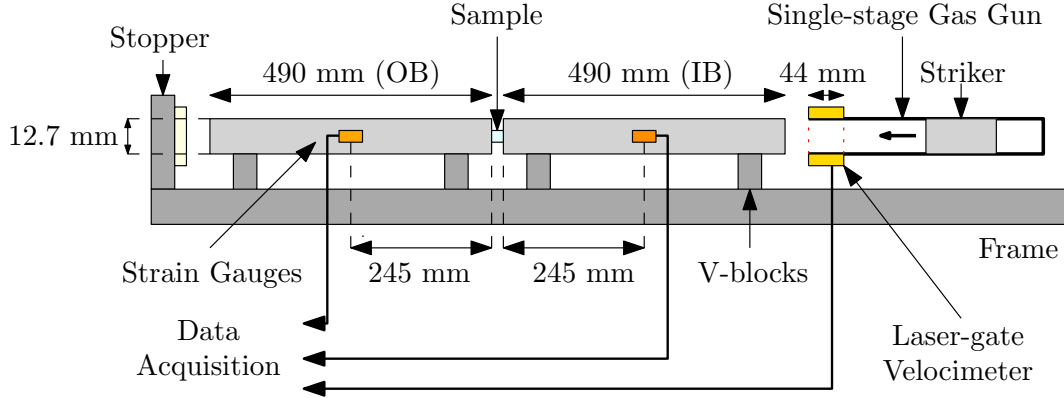


Figure 5.11: Schematic of the Aluminium 7068 alloy Centre for Blast Injury Studies (CBIS) Split-Hopkinson Pressure Bar (SHPB) system. The sample was held by friction between the Input Bar (IB) and Output Bar (OB). Pre-strain of the sample would lead to systematic underestimation of the sample strain which was mitigated by minimising visible barrelling of the sample.

As sample recovery was not required in this work, a momentum trap was omitted from the setup.

5.4.1.1 Calibration

In order to compute force from the measured voltage output of the strain gauges, a voltage-force calibration of the system was carried out. Given that the loading pulse has an average constant force of $\bar{F}$, assuming momentum conservation, the impulse $\bar{F}T$ introducing by the striker at impact is conserved along the IB and measured at the IB strain gauge. From impedance considerations, the average force measured at this strain gauge can be expressed as:

$$\bar{F} = \frac{2mvZ_B}{2m + Z_BT}, \quad (5.5)$$

where Z_B is the impedance of the bar material in the case that the striker and bar materials are identical, m is the striker mass, and v is the impact velocity, measured by the light-gates. Given that the measured voltage is proportional to the force, the temporal pulse length T can be expressed by equating the strain gauge impulse $\int V(t)dt$

to the average initial contact impulse $\bar{V}T$:

$$T = \int \frac{V(t)}{\bar{V}} dt, \quad (5.6)$$

where $\bar{V}$ is the average plateau amplitude of the strain gauge measured signal $V(t)$. Substituting, Eq. (5.6) into Eq. (5.5), the force history $F(t)$ can be related to the voltage signal by:

$$F(t) = k V(t) [1 + b V(t)], \quad (5.7)$$

where k and b are system-dependent calibration constants. These constants can be determined by plotting $\bar{F}/\bar{V}$ against $\bar{V}$ for a range of impact velocities, yielding a linear plot with intercept k and slope kb . By using a ‘bars together’ setup without a sample, these values can be obtained for both the IB and OB.

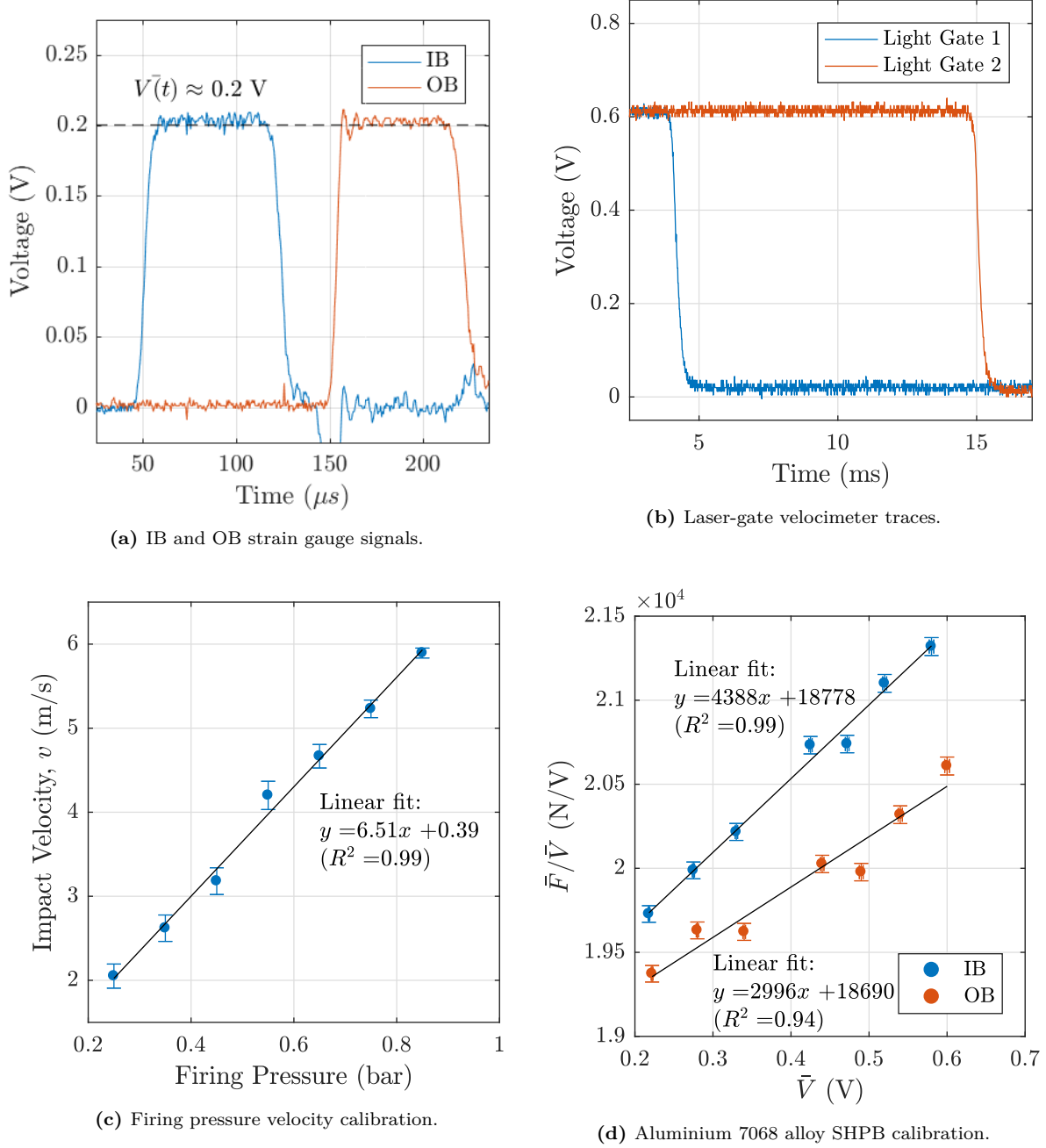


Figure 5.12: Data acquired to calibrate the aluminium 7068 alloy Split-Hopkinson Pressure Bar (SHPB) system. (a): Identification of average plateau voltage $\bar{V}$ from the strain gauges for the Input Bar (IB) and Output Bar (OB). (b): Example light-gate acquisition. (c): Calibration of impact velocity to firing pressure. The uncertainty is given by the standard error of the mean for 5 repeat shots at each firing pressure. A reduction of uncertainty at faster impact velocity can be observed due to reduced friction with the gas gun bore. (d): Calibration of strain gauge average plateau voltage to the average corresponding force, $\bar{F}$. Again, the uncertainties are computed as the standard error of the mean over the 5 repeat shots.

In Figure 5.12(a), example strain gauge voltage signals are shown for a calibration bars together shot. The equivalence of the waveform and amplitude for the IB and OB indicates negligible energy loss and good coaxial alignment of the system. The average

plateau voltage $\bar{V}$ is visible with Pochhammer-Chree oscillations about this value due to wave dispersion. From the Figure 5.12(d) plot and consideration of Eq. (5.7), the dimensionless calibration constants k and b were computed and are shown in Section 5.4.1.1.

Parameter	Aluminium 7068 alloy SHPB
Density, ρ_B	2850 kg m ⁻³
Elastic Modulus, E_B	73.1 GPa
Wave Velocity, C_B	6320 m s ⁻¹
k_{IB}	18,778
b_{IB}	0.23
k_{OB}	18,690
b_{OB}	0.16

Table 5.1: Key material and calibration parameters of the aluminium 7068 alloy Split-Hopkinson Pressure Bar (SHPB). The subscripts IB and OB refer to the Input Bar and Output Bar, respectively.

5.4.2 High-sensitivity System

The conventional SHPB system discussed in Section 5.4.1 is not appropriate for soft low impedance materials such as hydrogels. This is because the large impedance mismatch between the bar material and sample will result in an undetectable transmitted signal in the OB. To address this, a quartz-crystal embedded SHPB has been developed by Dr. D. R. Sory based on the design proposed by Casem *et al.* [175, 178]. The system exhibits the same dimensions as the conventional SHPB. The setup is shown in Figure 5.13.

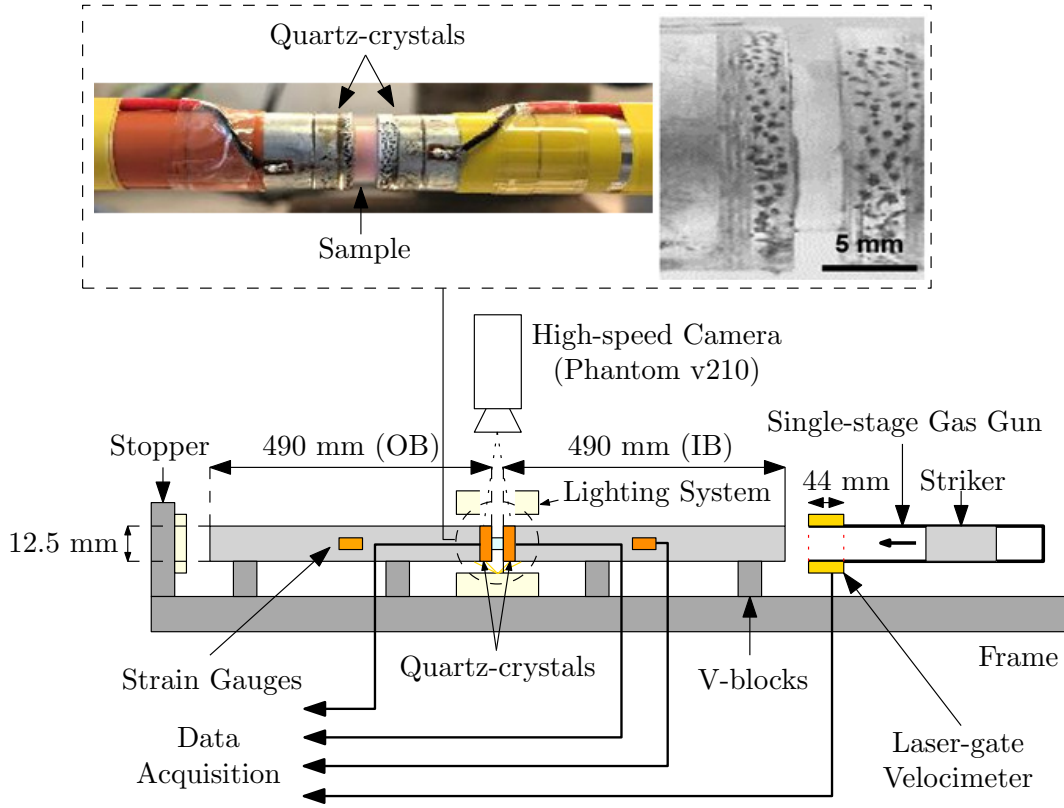


Figure 5.13: Schematic of the quartz-crystal embedded high-sensitivity Split-Hopkinson Pressure (SHPB). IB: Input Bar, OB: Output Bar.

By comparison to conventional strain gauge diagnostics, quartz crystal transducers can provide an order of magnitude greater sensitivity. Due to the fragility of the crystals, the system is appropriate for peak loads less than approximately 1 kN. As such, a tandem momentum trap was utilised, as proposed by Prot & Cloete [179], coaxially located on the IB to ensure single-pulse loading and prevent subsequent contact between the IB and OB bar ends. The system utilises 12.7 mm diameter aluminium 7068 alloy bars in which stacks of X-cut quartz crystals (Boston Piezo-Optics) are embedded (see Figure 5.14). These quartz crystal transducers exhibit a 10 MHz frequency response and dimensions of 12.700 ± 0.025 mm diameter and 0.254 ± 0.025 mm thickness. Aluminium was chosen since its acoustic impedance is close to that of the quartz crystal gauges, minimising any disturbance of the wave propagation at the aluminium-quartz interfaces. Under mechanical loading, each crystal individually experiences stress and generates a charge proportional to the applied force. The transducers were connected by coaxial low-noise cables (Techni Measure 6019A10) to a 3-channel charge amplifier (Dytran® 4020) in

piezoelectric mode, used to convert the output charge from the bars to a voltage signal. For this conversion, an input and output sensitivity of 40 pC N^{-1} and 50 mV N^{-1} was used, respectively. The signal was then recorded by digital oscilloscope (Tektronix® DPO3034) at a sampling rate of 25 MS/s . The quartz crystal configuration is shown in Figure 5.14.

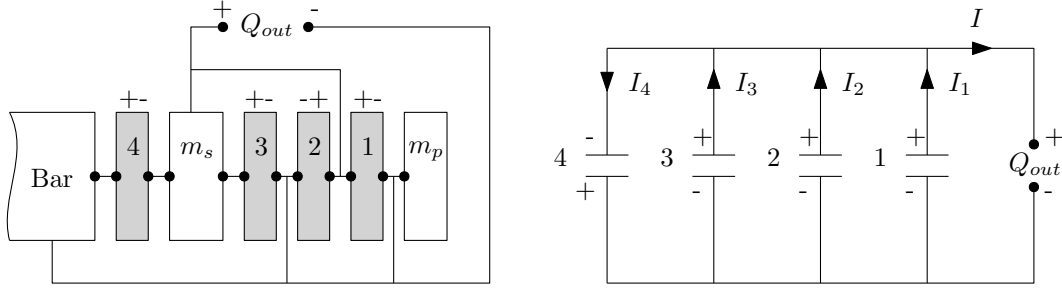


Figure 5.14: Configuration of Input Bar (IB) and Output Bar (OB) ends for the modified Split-Hopkinson Pressure Bar (SHPB) using quartz crystal embedded aluminium bars. m_s and m_p represent a seismic mass and the platen mass, respectively. The bar, seismic mass and platen are all constructed from aluminium 7068 alloy with the components bonded with conductive epoxy adhesive. Four quartz crystals (grey regions) are utilised. Under mechanical loading, each crystal individually experiences stress and generates a current proportional to rate of change of the applied force. The polarity of transducer 4 is chosen such that its current is subtracted from that of the crystals 1-3. As such, the combination of transducer 4 and the seismic mass act to compensate for the additional unwanted inertia introduced by the stack of crystals 1-3. Adapted from [175].

In operation, data reduction could be accomplished without applying classical SHPB theory as is the case for a conventional strain gauge instrumented system. The quartz transducers gauges allowed direct measurement of the sample force history at the IB and OB interfaces. Force equilibrium was directly checked by observing the equivalence of the forces at these two interfaces. The sample displacement and engineering strain were computed from high-speed photography of the sample deformation. The camera was mounted above the sample viewing downwards with the lighting system as used for the drop-weight (see Figure 5.9) illuminating the sample. In order to do this, as for the drop-weight, the bar end platens were speckled and Digital Image Correlation (DIC) used to track the bar motion (as shown in Figure 5.13). See Section 5.5 for a discussion of the implementation of DIC.

5.4.2.1 Calibration

To obtain the force-voltage relationship for the IB and OB quartz-crystal transducers, the transducer voltage had been previously calibrated against the output of a commercial sensor by Dr. D. R. Sory [175]. To do this, the IB and OB were each uniaxially aligned and loaded against a calibrated dynamic force sensor (Dytran[®] 5061V5) over 30 repeated loading pulses at force intensities up to approximately 900 N. The IB and OB were found to exhibit highly linear responses satisfying $< 3.2\%$ Full Scale Output (FSO). The FSO quantifies the maximum deviation of the sensor response from an idealised straight line as a percentage of the maximum output of the range being considered. The sensitivities of the IB and OB were 12.8 mV N^{-1} and 16.8 mV N^{-1} , respectively.

5.5 Digital Image Correlation

Digital Image Correlation (DIC) is a widely-used optical non-contact technique for determining material deformation. It is a highly robust method that can be applied to a wide range of length scales and materials, including biological materials, metals, polymers and foams. The practical basis for the technique is the application of a speckle pattern to the material that is being tracked and videoing the evolution of the pattern during the experiment. The DIC method employs image registration algorithms [180] to compare the relative displacement of all speckle features in each frame to a reference image. This reference is conventionally the initial undeformed image of the material prior to the beginning of the experiment.

For the material characterisation experiments carried out using the drop-weight and quartz-crystal SHPB platforms, DIC was employed to determine the sample strain by tracking the displacement of the drop-weight striker or IB and OB sample interfaces. For the drop-weight, the striker was speckled, and for the SHPB, the input and output bar end platens were speckled with a high-speed camera (Phantom v210) viewing the event. Frames rates of 28,000 FPS and 35,000 FPS were used for the drop-weight and

SHPB, respectively, reflecting the faster loading of a Hopkinson bar experiment. From the displacement of the striker and bar ends, respectively, the sample strain could be accurately determined given no deformation of the bars. This was valid as the stresses involved were an order of magnitude less than the elastic limits of the materials comprising the loading systems. This indirect technique for computing the sample strain (rather than direct speckling of the sample) yielded the advantages that the speckle patterns could be applied with ease, high contrast and over a large area, increasing the accuracy of the measured displacement for a given frame. This method provides greater accuracy than a traditional contrast-based bar edge detection algorithm since the average movement of a larger pixel region is being tracked. As a result, sub-pixel displacement resolution can be achieved through DIC.

The open-source two-dimensional DIC MATLAB software Ncorr v1.2 developed by Blaber *et al.* [180] was used to analyse high-speed photography. This software computes the displacement of a Region-of-Interest (ROI) for each frame relative to the initial reference. The analysis is carried out entirely in real-space. In principle, Ncorr computes displacement by breaking each image down into subsets of pixels where each subset has a monochromatic reading of intensity. These readings for the reference frame are subtracted from each subsequent frame and the Sum of Squared Differences (SSD) over all pixels is recorded. For each subset, when the SSD is minimised, the algorithm returns the displacement required for the initial subset to have translated to the new location [180]. This technique is capable of capturing affine changes such as rotation or shear, however, this was not necessary as DIC was used in this work only to track the gross movement of an object. Indicative ROIs for the drop-weight and high-sensitivity SHPB are shown in Figure 5.15 for an example agarose sample.

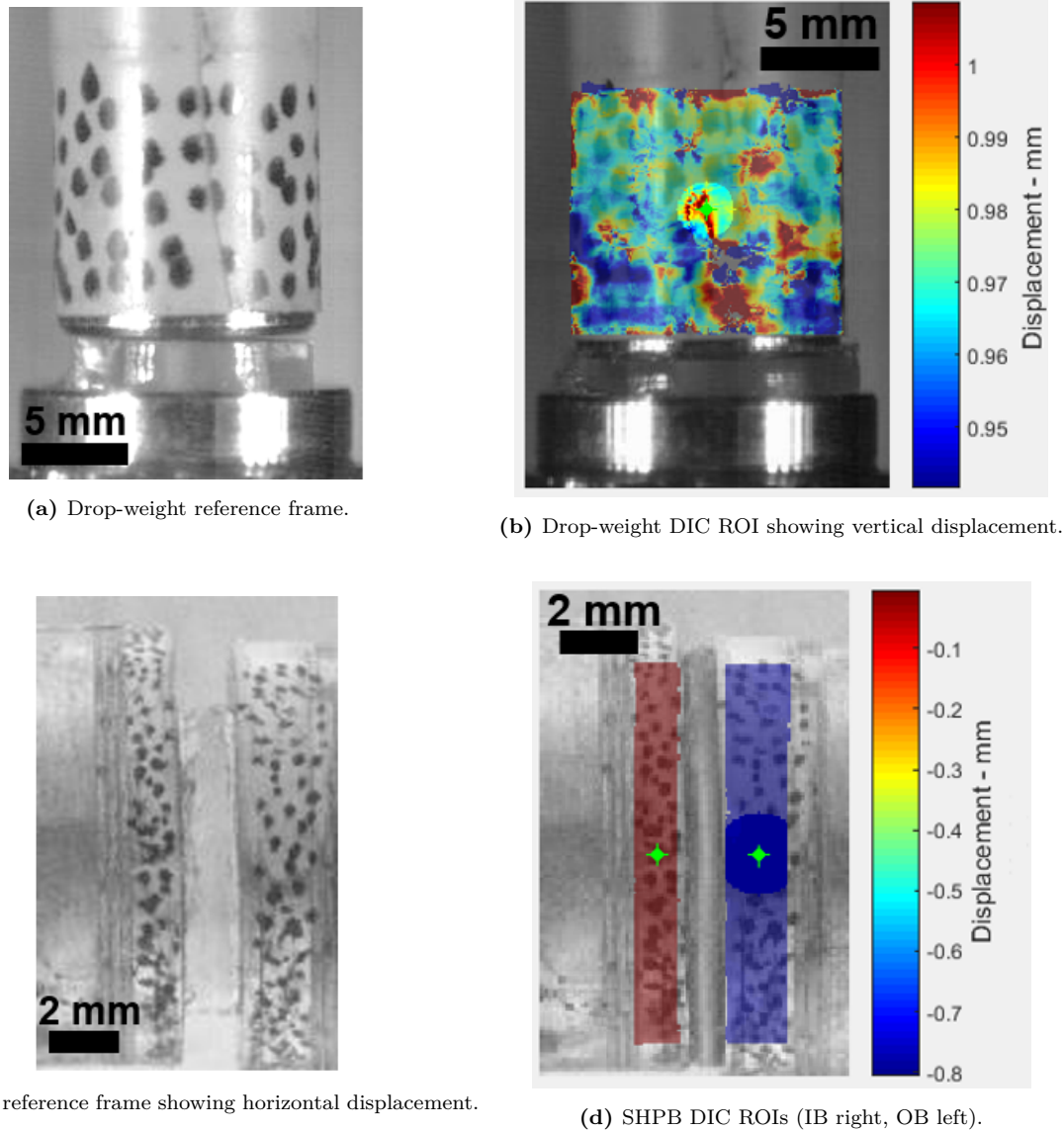


Figure 5.15: Digital Image Correlation (DIC) analysis of drop-weight and Split-Hopkinson Pressure Bar (SHPB) high-speed videos using Ncorr [180]. The random speckle patterns utilised features with a minimum size of 5 pixels. In the Ncorr Graphical User Interface (GUI) visible in (b) and (d), Regions of Interest (ROIs) were defined and the subsequent Eulerian displacement (displacement with respect to the start position) of these regions were tracked. Displacements are shown in the direction of the loading axis, vertical (positive downwards) for the drop-weight and horizontal (positive rightwards) for the SHPB. In (d), the displacement is negative as the Input Bar (IB) moves towards the left. The Output Bar (OB) can be observed to remain relatively stationary compared to the IB.

The random speckle patterns were created by applying white tape to the area (to add minimal mass) and then applying black marks (for maximal contrast) to the tape with a permanent marker. As a guideline for DIC, each speckle feature must be at least 5 pixels in diameter [180]. Hence, the speckle pattern depends on the pixel density of the video, which itself is primarily governed by the optical setup, camera settings and lighting system. Note that greater illumination was required for the SHPB due to its demand for

a faster frame rate. For both the drop-weight and SHPB high-speed photography, the pixels to physical distance were approximately 12 pixels/mm. Hence, the speckle features were made a minimum of 0.5 mm diameter as shown in Figure 5.15.

In Figure 5.15, the Eulerian displacement fields are predominantly uniform due to the fact that there no relative movement of the speckles (as the materials are not deforming). In the case of the SHPB, it can be seen in Figure 5.15(d) that the IB moves while the OB remains close to stationary. This is a result of the fact that in the first stage of an SHPB shot, it is the IB that advances on the sample the waves have not yet transmitted into the OB. In order to compute the ROI displacement history, the output data from Ncorr was exported and analysed outside the software. For each frame, the displacement of the ROI was estimated as the average value of the Eulerian displacement field. The result of this analysis is shown in Figure 5.16

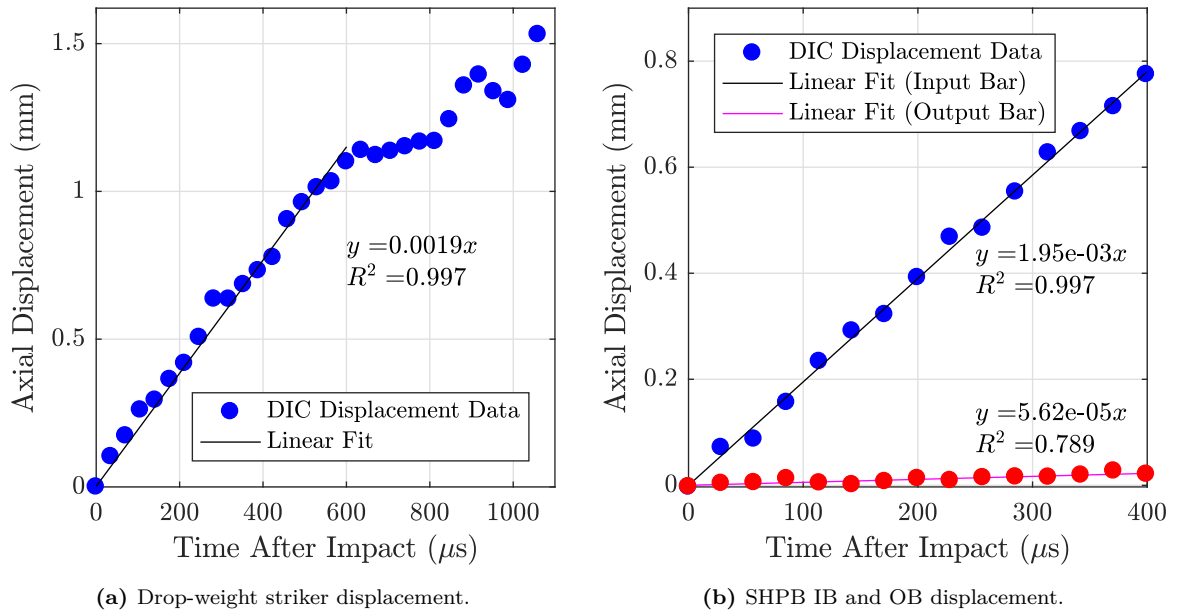


Figure 5.16: Computation of axial displacement histories for the (a) drop-weight and (b) modified high-sensitivity Split-Hopkinson Pressure Bar (SHPB) using Digital Image Correlation (DIC). In (a), the drop-weight impactor displacement is linear with time up to approximately 600 μs , implying constant sample strain rate. Following this, the impactor displacement is non-linear due to deceleration by the sample. Each data point in (a) and (b) represents a video frame.

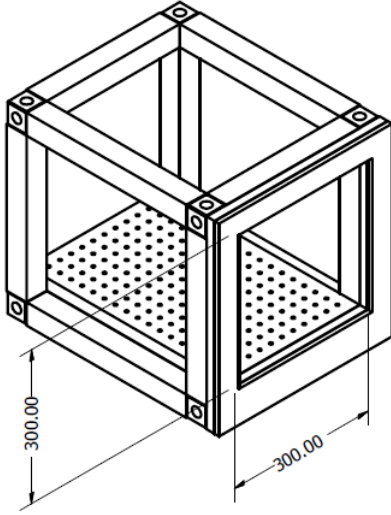
From Figure 5.16, a physical displacement resolution of approximately 0.05 mm was achieved, corresponding to a 0.6 pixel resolution. This sub-pixel precision was due to the combination of applying DIC to a large ROI region and averaging the displacement field

for each frame. The linearity of the displacement or constant speed of the strikers was a key aim for the drop-weight and SHPB experiments to ensure constant sample strain rate. For the drop-weight, it can be seen that the impactor displacement was linear with time (constant velocity) up to approximately 600 μs . This linear region corresponded to initial compression of the sample at constant strain rate. At later time, the displacement became non-linear due to deceleration of the impactor by the sample. For the SHPB, though the OB displacement is small, sample strain was computed from the difference between the IB and OB displacement lines. The stress-strain mechanical characterisation results are discussed in Chapter 6.

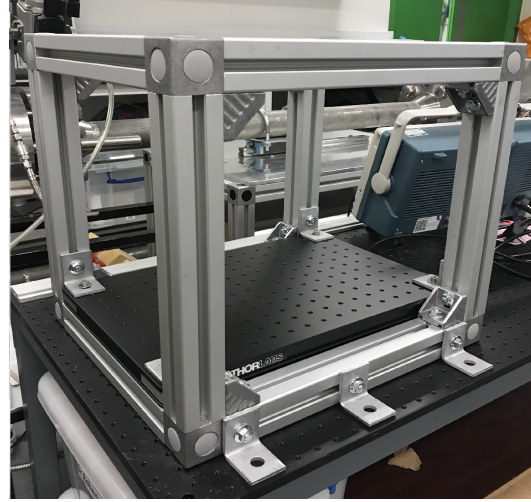
5.6 Large-scale Shock Tube Rigs

5.6.1 Shock Tube External Mount

In order to carry out large-scale blast mitigation experiments using the shock tube, a mount was required to enable the sample and thoracic surrogate materials to be free-standing. See Chapter 9 for discussion of these experiments. The mount was required to accommodate sample sizes up to $30 \times 30\text{cm}$, not corrode with extended contact with water, and be robust enough to withstand repeated exposure to shock tube shots at firing pressure up to 12 bar. The design consisted of an extruded aluminium frame (45 mm Rexroth, Bosch) and a breadboard base (PBG51523 M6 breadboard, Thorlabs[®]) to enable additional optical or structural components to be incorporated into experimental setups. The mount could be secured with 9 M6 bolts to a heavy optical table at the appropriate height for the shock tube muzzle. The rig is shown in Figure 5.17.



(a) CAD.



(b) Laboratory.

Figure 5.17: Shock tube external mount rig for large-scale shock tube blast mitigation experiments (units in mm).

5.6.2 Shock Tube Expansion Adaptor

For large-scale shock tube experiments, it was necessary to develop a method of loading samples mounted to the external mount (see Section 5.6.1) of the order of the size of the thorax with an open muzzle. Given the small 60 mm diameter of the shock tube and the rapid exponential decay of the blast wave in air (see Eq. (2.5)), an unmodified setup would have resulted in significant loss of peak pressure and loading duration due to losses of the blast wind to the surroundings. To address this, a conical expansion adaptor was developed to provide greater coupling of the blast energy into the target structure. The objective of the subsequent analysis was to analytically derive the necessary geometry of the expansion adaptor for the constraints of the CBIS shock tube.

A supersonic flow passing a convex corner results in an expansion wave known as a Prandtl-Meyer expansion fan, a result of the fact that the retention of a single planar shock front would violate the Second Law of Thermodynamics. As shown in Figure 5.18(a), the fan consists of an infinite number of Mach waves diverging from the corner. The flow velocity increases across the fan with the final Mach number (M_2) exceeding the initial Mach number into the expansion (M_1), while the static pressure,

temperature and density also decrease to ensure mass continuity.

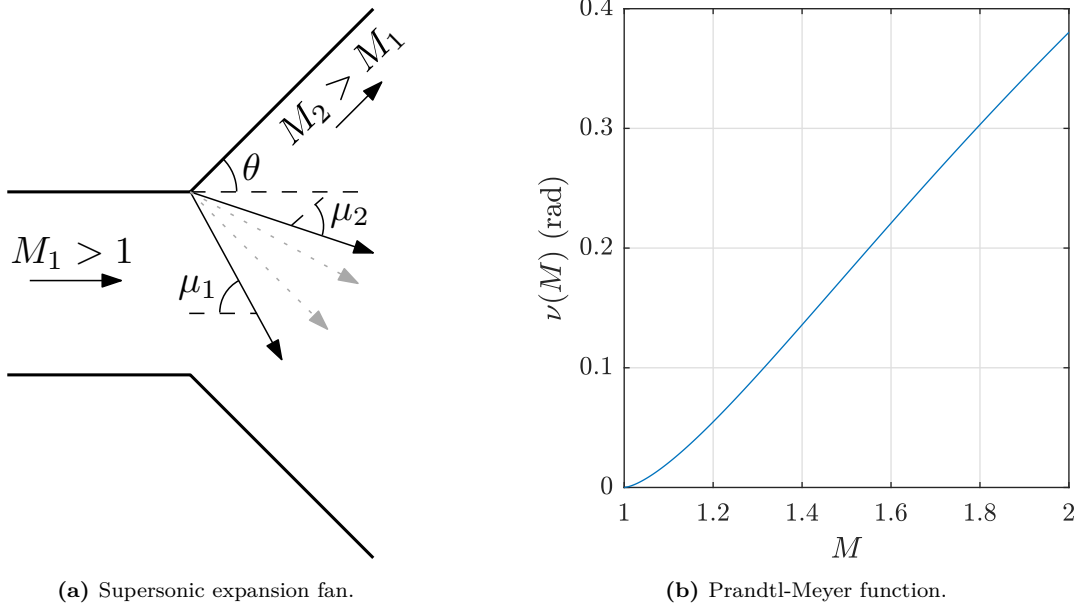


Figure 5.18: Illustration of the formation of an expansion fan when a supersonic flow encounters a sharp convex corner at a turn angle of θ . For clarity, the fan is shown for one point in two-dimensions. The maximum angle the flow can turn through is described by the Prandtl-Meyer function as a function of Mach number, M . M_1 : Mach number before turn, M_2 : Mach number after turn, μ_1 : Angle of head of expansion fan, μ_2 : Angle of tail of expansion fan, $\nu(M)$: Prandtl-Meyer function.

The physics of the Prandtl-Meyer expansion fan can be analytically described assuming an ideal gas and that the process is isentropic. As such, the stagnation pressure, temperature and pressure remain constant. The head and tail of the expansion fan are at angles of:

$$\mu_1 = \sin^{-1}(M_1^{-1}) \quad (5.8)$$

relative to the initial flow direction and

$$\mu_2 = \sin^{-1}(M_2^{-1}) \quad (5.9)$$

relative to the final flow direction. The Mach numbers before and after the turn are related by the turn angle:

$$\theta = \nu(M_2) - \nu(M_1), \quad (5.10)$$

where $\nu(M)$ is the Prandtl-Meyer function. This function describes the maximum angle a sonic flow ($M = 1$) must turn through to reach the Mach number M , and is derived from

conservation of mass, momentum and energy across the expansion fan. The function enables the output conditions following the turn to be predicted and is given by (see Figure 5.18(b)):

$$\begin{aligned} \nu(M) &= \int \frac{\sqrt{M^2 - 1}}{1 + \frac{\gamma-1}{2}M^2} \frac{dM}{M} \\ &= \sqrt{\frac{\gamma+1}{\gamma-1}} \tan^{-1} \left(\sqrt{\frac{\gamma-1}{\gamma+1}} (M^2 - 1) \right) - \tan^{-1} \left(\sqrt{M^2 - 1} \right). \end{aligned} \quad (5.11)$$

For the most trivial case, $\nu(1) = 0$, corresponds to a planar flow with no turn. With knowledge of the incident Mach number M_1 , Eq. (5.11) can be used to compute $\nu(M_1)$ and then $\nu(M_2)$ calculated from Eq. (5.10). From this, the Prandtl-Meyer function can be used to determine the output Mach number M_2 . The limiting behaviour of Eq. (5.11) provides further insight into the physics of the phenomenon with $\nu_{max} \equiv \nu(M \rightarrow \infty) = \pi/2(\sqrt{\gamma+1/\gamma-1}-1)$. In Figure 5.18(b), this would be visible as an asymptote at $\nu(M) = \pi/2$ for an ideal gas ($\gamma = 5/3$). Thus, for this case, Eq. (5.10) can be expressed as:

$$\theta_{max} = \nu_{max} - \nu(M_1), \quad (5.12)$$

where θ_{max} is the maximal angle the supersonic flow can turn through. $\theta < \theta_{max}$ constitutes the first design condition for the expansion adaptor as a cone angle exceeding θ_{max} would be equivalent to a free-field expansion and energy loss would occur beyond this angle and the cone wall due to turbulent mixing of the shock tube gas. An additional two constraints were defined. Firstly, reflection of the expansion waves from the cone were to be avoided as this would result in unpredictable loading from the shock tube. Secondly, given that expansions fans originate from the full circumference of the convex corner, it was necessary to minimise interaction of the expansion fans with each other, resulting in a turbulent non-simple region. From geometrical considerations, these constraints limited the value of μ_1 (see Figure 5.19).

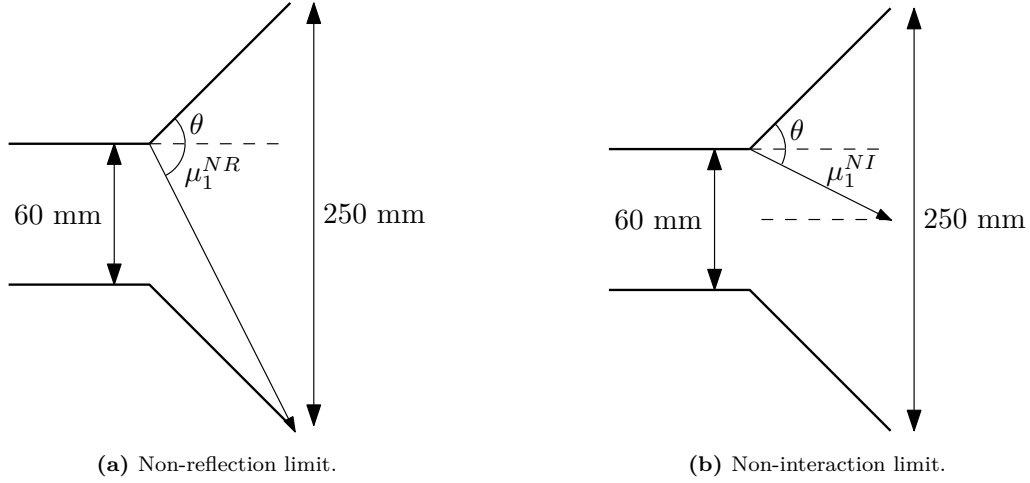


Figure 5.19: Geometrical derivation of the constraints on the angle of the expansion fan head, μ_1 , for no reflection (μ_1^{NR}) or self-interaction (μ_1^{NI}) of the expansion fans produced by the supersonic flow. In (a), it is ensured that the wave does not reflect from the cone wall before exiting the expansion while, in (b), it is ensured that the expansion fans do not interact across the centreline. θ : Adaptor turn angle.

Note that a maximum opening diameter of 250 mm was used to match the size of the opening presented by the external armour mount. For the dimensions in Figure 5.19, it can be shown that the upper limits on μ_1 are given by:

$$\mu_1^{NR} < \tan^{-1}(0.62 \tan(\theta)) \quad (5.13)$$

and

$$\mu_1^{NI} < \tan^{-1}(0.30 \tan(\theta)) . \quad (5.14)$$

The resulting design spaces for the adaptor are shown in Figure 5.20.

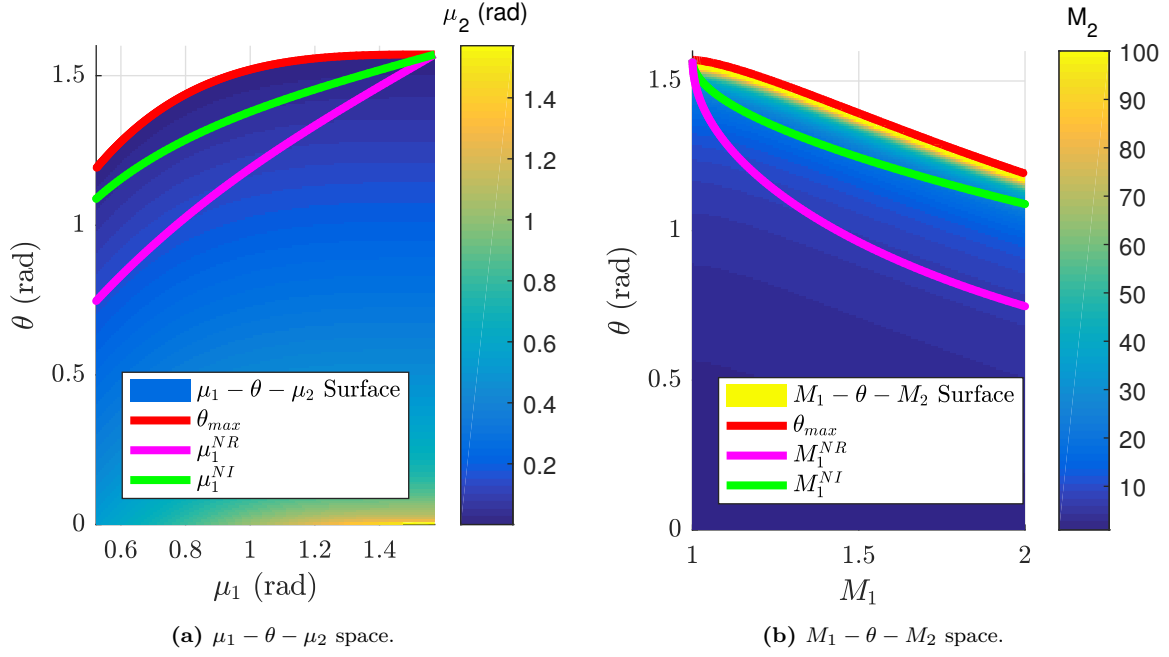


Figure 5.20: Design spaces for expansion adaptor to enable appropriate selection of the turn angle θ for the CBIS shock tube in terms of (a) leading (μ_1) and trailing (μ_2) edge angle, and (b) input (M_1) and output (M_2) Mach number. The surface of (a) is transformed to (b) by applying Eq. (5.8). Super-scripts NR and NI denote non-reflection and non-interaction constraints, respectively. θ_{max} : Maximum supersonic flow turn angle.

From Figure 5.20(b), it can be seen that the non-interaction condition places a greater constraint on μ than the non-reflection constraint, geometrically visible from Figure 5.19. For a given adaptor turn angle θ , the available working range between the non-interaction and θ_{max} conditions is narrow. To determine the Mach number range in this region applicable to the CBIS system specifically, the incident Mach number M_1 was experimentally measured. Calculation of M_1 from the time-of-arrival of the blast wave between Sensor 1 and Sensor 2 would only provide the average shock speed in the final half of the driven section. As an alternative, it is possible to determine the Mach number of the flow from a point measurement of the peak pressure using the Eq. (2.35) Rankine-Hugoniot relation. This enabled the flow Mach number at the shock tube outlet to be measured, providing an estimate of the input Mach number into a subsequent expansion adaptor. This was carried out for the full range of possible firing pressures from 2–17 bar and is shown in Figure 5.21.

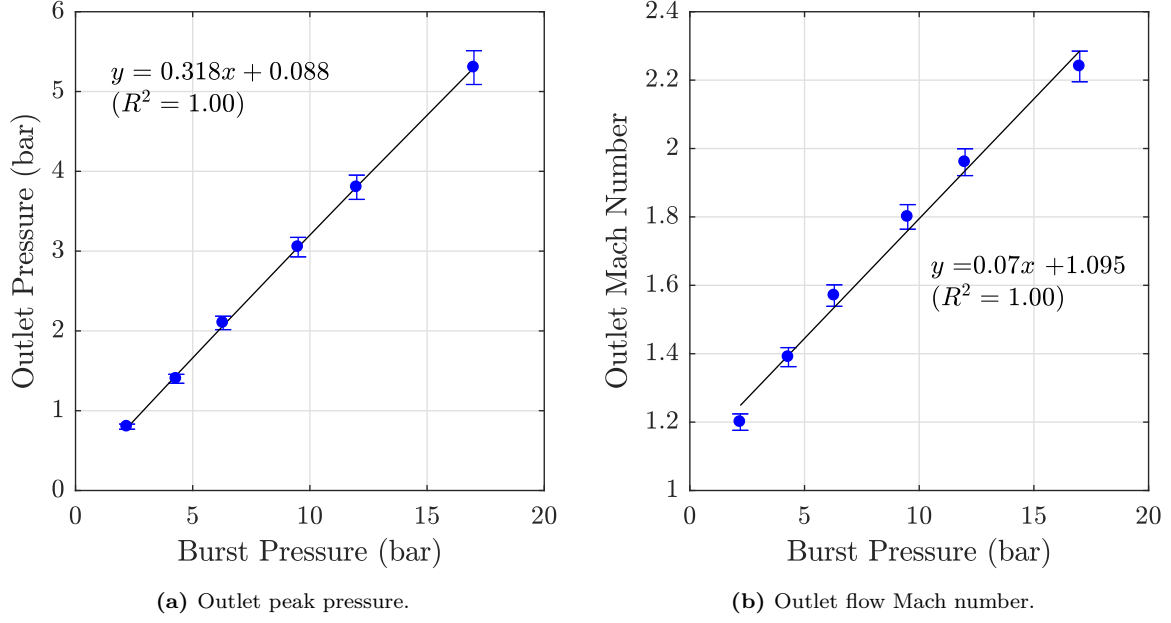
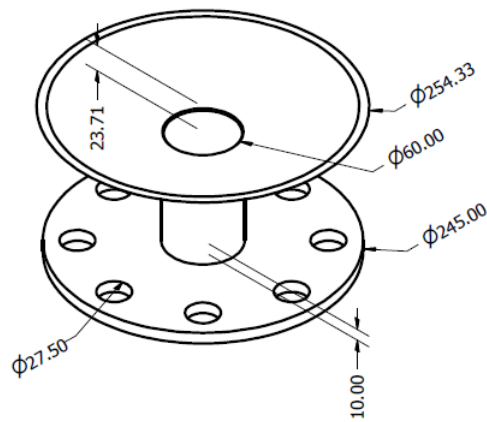
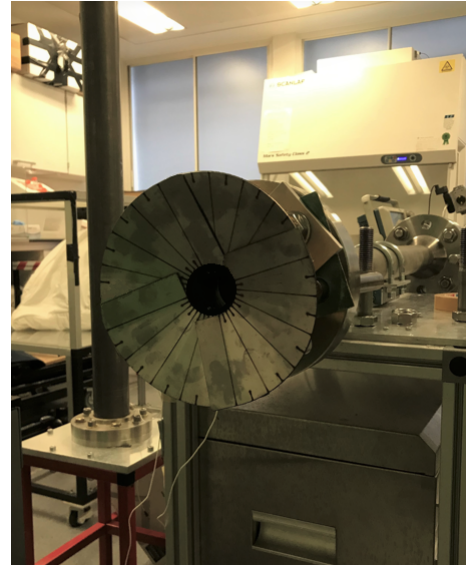


Figure 5.21: Measurement of outlet peak pressure (a) and calculation of corresponding Mach number (b) from application of the Eq. (2.35) Rankine-Hugoniot relation. The outlet pressure and blast Mach number followed a linear dependence on the diaphragm burst pressure. Each data point represents 5 repeated shots with the uncertainty given as the standard error of the mean.

From these measurements, the incident Mach number range achievable with the system was approximately, $1.20 < M_1 < 2.20$. From consideration of Figure 5.20(b), the value of θ chosen to maximise the M_1 range accessible within the constraints was $\theta = 1.3 \text{ rad} \approx 75^\circ$, which corresponds to a working Mach number range of approximately $1.25 < M_1 < 1.80$. Note that, while this choice of θ minimised turbulent interaction, in use, the target material was positioned at stand-off distance from the adaptor outlet of 20 cm, resulting in a smaller input Mach number working range. This was investigated numerically in Chapter 9. The conical component of the rig was manufactured by steel rolling (Albany Metal Spinners) methods and is shown in Figure 5.22.



(a) CAD.



(b) Laboratory.

Figure 5.22: Shock tube expansion adaptor for large-scale shock tube blast mitigation experiments (units in mm).

As this result was derived from analytical considerations only, prior to manufacture, the choice of turn angle was checked using an experimentally validated computational model for an expanding supersonic flow. The results of this are discussed in Chapter 7.

5.7 PVDF Sensors

Polyvinylidene Fluoride (PVDF) is a type of flexible and reproducible piezoelectric polymer that is widely used for *in situ* pressure measurement in dynamic experiments. Such films have low impedance (approximately $2 \times 10^6 \text{ kg m}^{-1}$) close to water, making them useful for biomedical applications. Combined with the thinness of such sensors (typically $25 \mu\text{m}$ thickness), the sensors interfere negligibly with the wave propagation when embedded in soft materials. In electrical current mode, the sensor is connected to a low-value Current Viewing Resistor (CVR) and produces a signal that scales with the rate of change of stress. Signal conditioning with a charge amplifier acts to integrate this signal, allowing a voltage signal proportional to the stress acting on the sensor to be measured. PVDF exhibits a low piezoelectric coefficient, so does not voltage saturate under impact or shock loading. Studies by Yu *et al.* showed that PVDF sensors exhibit high impact

resistance and fast nanosecond response time [181]. Further work by Meng & Yi demonstrated the reliability of such sensors under repeated dynamic loads [182]. This contrasts with Piezoceramic materials, notably Lead Zirconate Titanate (PZT), which suffer from brittleness and poor impact resistance.

In this work, a PVDF sensor (Piezotech S2501) was used to measure the pressure transmitted to a gelatine thoracic surrogate under shock tube loading (see Chapter 9). These sensors are manufactured from 25 μm film using the method proposed by Bauer [183]. The method involves biaxially stretching the material then applying a strong electrical field (poling) where the sensor response time is limited by the thickness of the film. The biaxial stretching renders the axial stress component perpendicular to the stretching directions dominant so that the uniaxial contribution can be measured provided the sensor is mounted to avoid flexure. For mechanical protection, the sensors were laminated with a 55 μm polyester layer on both sides. The gauges exhibit thermodynamic stability up to 90 $^{\circ}\text{C}$, nanosecond response time, linearity across a broad stress range (kPa to GPa), and a sensitivity of 4 $\mu\text{C cm}^{-1}$. The sensors were electrically shielded to prevent anomalous readings previously observed for embedding such gauges in polar materials (given that gelatine is polar) due to interference from shock induced polarisation [183].

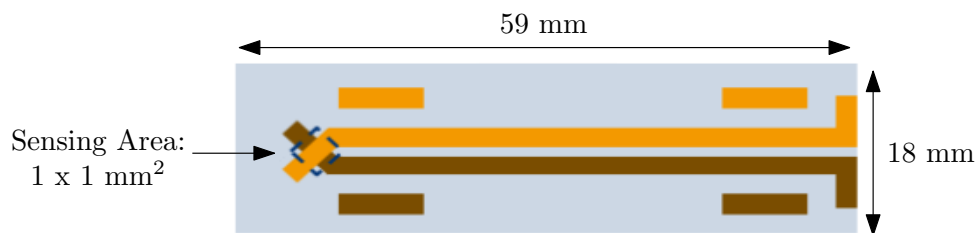


Figure 5.23: Piezotech S2501 Polyvinylidene Fluoride (PVDF) sensors used for gelatine pressure measurements in shock tube blast injury mitigation experiments.

5.7.1 Calibration

Two PVDF sensors were calibrated using the drop-weight (see Section 5.3) with an impactor mass of 150 g from heights in the range of 30–90 mm. The sensors were connected by a low-noise FCI 4-way socket to a Dytran[®] 4020 signal conditioner and their peak

force measurement calibrated to that of the Dytran[®] 1051V6 drop-weight load cell. The boundary conditions replicated the configuration of the sensor in the large-scale shock tube experiments where the sensor was mounted with a rigid steel rear support. This negated non-axial stress components from contributing to the signal.

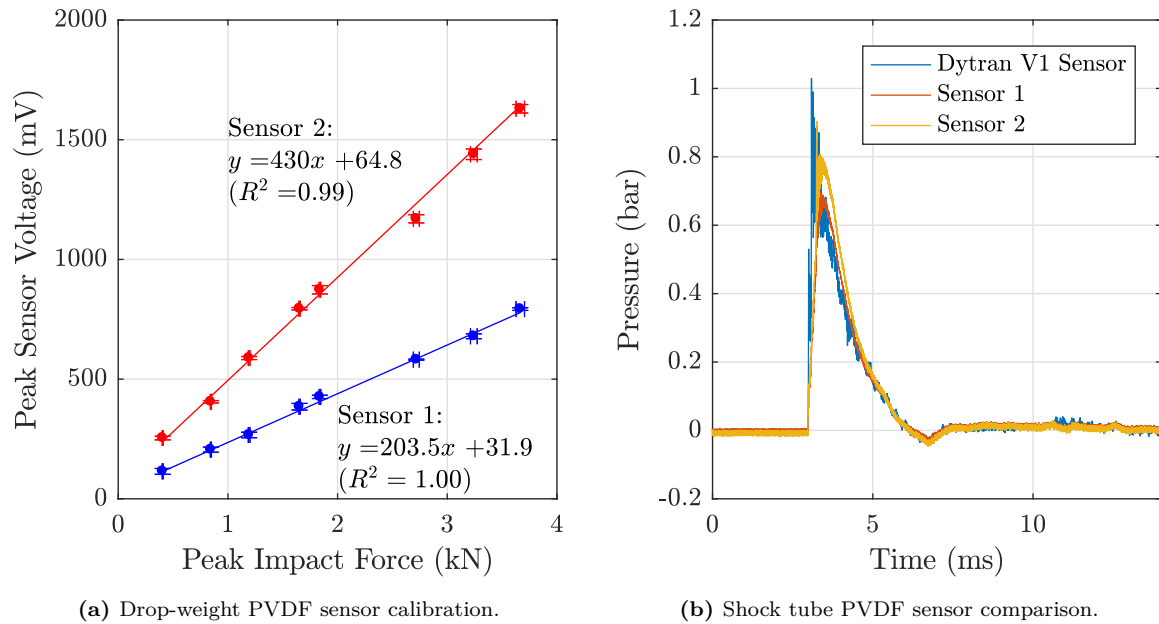


Figure 5.24: Calibration data for the Piezotech S2501 Polyvinylidene Fluoride (PVDF) sensors. Two sensors were calibrated (Sensor 1 and Sensor 2). In (a), the data represents the mean of 6 repeated drop-weight shots with the uncertainties computed as the standard error of the mean.

The sensors exhibited a highly linear response to loading with calibration factors of $204 \pm 3 \text{ mV kN}^{-1}$ and $430 \pm 5 \text{ mV kN}^{-1}$ for Sensor 1 and Sensor 2, respectively. Using these calibration factors, the calibration was checked under shock loading using the shock tube. The sensors were each attached with high adhesive and thermal resistance (Kapton[®] tape) to the end-wall flange of the shock tube with an embedded Dytran[®] V1 sensor behind the test gauge for comparison. From the results shown in Figure 5.24(b), it was observed that the calibrated sensors closely replicated the shock tube Dytran[®] V1 sensor diagnostics with good Signal-to-Noise, response time, and rise time.

Chapter 6

Mechanical Characterisation

Uniaxial compressive stress-strain response of the mitigation materials and gelatine surrogate material (see Chapter 4) were investigated at room temperature ($23 \pm 2^\circ\text{C}$) over strain rates ranging from $0.01\text{--}1600\text{ s}^{-1}$. Quasi-static testing, while not necessarily applicable to blast, served two purposes. Firstly, it is important for design and ergonomic considerations as it gives an indication of the material's load-bearing capacity. Indeed, appropriate armour must be capable of supporting its own weight and withstanding compression from daily use. Secondly, it allows the strain rate sensitivity of the material to be investigated over a wider range of strain rate regimes, where some materials may have an intermediate rate (of the order of 100 s^{-1}) dynamic response that is well-approximated by its quasi-static response. Dynamically, as blast can induce a wide range of stress and strain rate loading conditions depending on target distance, orientation and surroundings, it is necessary to characterise the materials at different loading rates. This can also enable appropriately robust constitutive material models to be identified.

6.1 Methodology

In this work, compressive material characterisation was carried out such that the loading conditions were applicable to those experienced by the materials under shock tube loading.

Broadly three such scenarios were considered. This included a closed shock tube, open shock tube and an open shock tube with expansion adaptor and the target material at a stand-off distance. These scenarios corresponded to loading pressures on the materials of the order of 100–1000 kPa, 60–700 kPa, and 40–250 kPa, respectively. These pressure regimes could be addressed by the drop-weight, conventional SHPB, and high-sensitivity SHPB.

For quasi-static compressive testing, the sample dimensions for the materials were 14 mm diameter and 5 mm thickness, and 9 mm diameter and 1.6 mm thickness for dynamic testing. For the Instron, crosshead velocities of 0.005 mm s^{-1} , 0.05 mm s^{-1} and 0.1 mm s^{-1} were used, corresponding to strain rates of 0.001 s^{-1} , 0.01 s^{-1} and 0.02 s^{-1} , respectively. As the only non-casted materials, the reticulated polyurethane foams were cut to these sizes using a hot wire foam cutter at 250°C (Engel Type-C). The smaller diameter for dynamic compression allowed for increase of the sample cross-sectional area without immediately exceeding the force sensing element's diameter (the load cell and bars for the drop-weight and SHPB, respectively). This aspect ratio also complies with the guideline aspect ratio relative to the bar diameter required to minimise sample inertia [124]. Inertia was further reduced for the soft hydrogel samples using sample coring (see Figure 6.6). To minimise shear and pre-strain on SHPB samples, the samples were gently sandwiched between the IB and OB with the bar ends lubricated with a thin layer of petroleum jelly. The coaxial alignment of the sample was verified by checking the high-speed camera view. As a pulse shaper, a thin layer of vacuum grease was applied to the impacted end of the IB to encourage faster force equilibrium and reduce inertia due to acceleration. For the intermediate rate, from consideration of the drop-weight force calibration (Figure 5.10), a drop heights of 40 cm, 60 cm and 90 cm were used such that the delivered forces were in the range of 40–90 kPa, applicable to the large-scale shock tube experiments (see Chapter 9). Finally, while the quartz-instrumented SHPB was used for the soft reticulated foams, hydrogels and gelatine, the aluminium conventional system was used for the syntactic samples due to their greater stiffness resulting in a stress output greater than the calibrated capacity of the quartz system (approximately 1 kN). A

MATLAB code was written to automate the analysis of SHPB data given system-specific input parameters.

6.2 Stress-strain Calculations

The stress-strain parameter space can be defined in nominal terms, known as engineering stress and strain, or according to true stress and strain. The engineering stress (s) and strain (e) are computed as:

$$s = F/A_0, \quad (6.1)$$

$$e = \Delta L/L_0, \quad (6.2)$$

where F is the applied force, A_0 is the initial sample cross-sectional area, ΔL is the change in sample length due to the stress, and L_0 is the initial sample length. These engineering parameters are valid for small deformation, but neglect the variation of sample length and cross sectional area during the test. As an alternative, true stress-strain curves account for these changes by considering the instantaneous dimensions of the sample. The true stress (σ) and strain (ϵ) are given by:

$$\sigma = \frac{F}{A} = s(1 + e), \quad (6.3)$$

$$\epsilon = \int_{l_0}^l \frac{dl}{l} = \ln\left(\frac{L}{L_0}\right) = \ln(1 + e), \quad (6.4)$$

where A and l are the instantaneous area and length, respectively, and dl is an infinitesimal length change. The expression of the true stress-strain parameters in terms of the engineering parameters is valid under the assumption that the sample volume is conserved such that $LA = L_0A_0$ (see Figure 6.1).

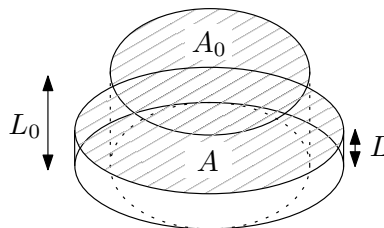


Figure 6.1: Conservation of sample volume under compression (positive Poisson's ratio).

6.3 Polyurethane Foams

For the syntactic polyurethane foams, an example data acquisition for the conventional SHPB showing the stages of data reduction is displayed in Figure 6.2 based on the classical treatment outlined in Section 2.5.

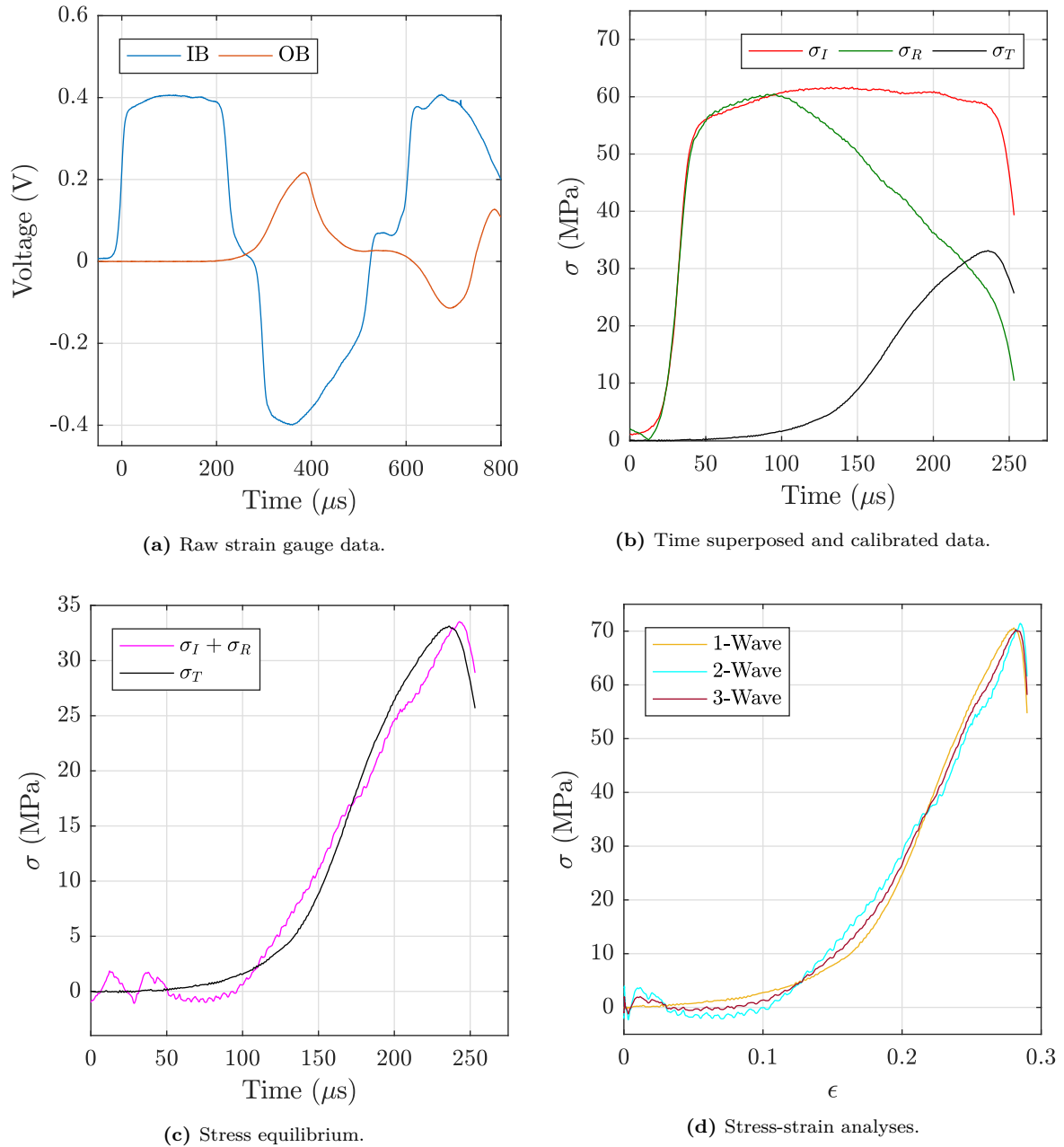
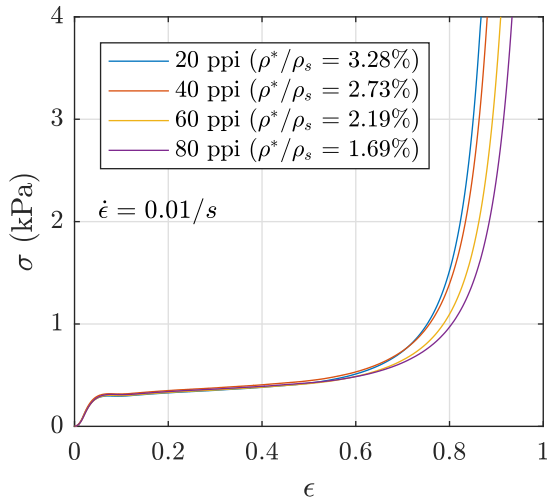


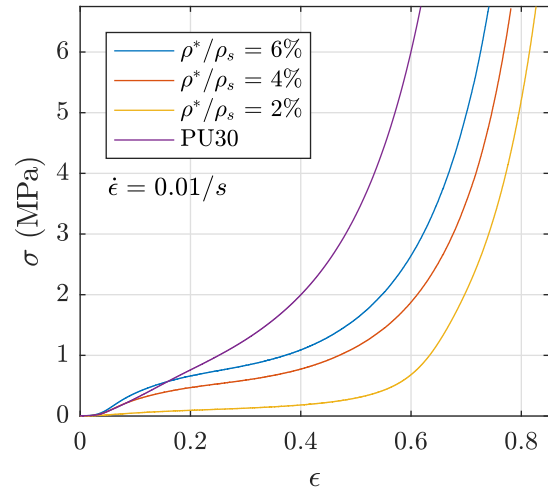
Figure 6.2: Stages of Split-Hopkinson Pressure Bar (SHPB) data reduction for a bulk Shore hardness 30 A polyurethane (PU30) sample at a strain rate of 1600s^{-1} . In (d), the sample true stress-strain relations are shown under the assumption of conserved sample volume. Computed 1-wave, 2-wave and 3-wave stresses correspond to calculation of the sample stress from the transmitted signal only, incident and reflected signal, and finally the incident, reflected and transmitted signals, respectively (see Section 2.5.2). IB: Input Bar, OB: Output Bar, σ_I : Incident stress signal, σ_R : Reflected stress signal, σ_T : Transmitted stress signal.

In Figure 6.2, the lack of PC oscillations in the IB raw signal was due to the use of petroleum jelly pulse shaping material placed on the impacted end of the IB. To begin analysing the data, the start of the incident, reflected and transmitted waves were identified with the duration of the time window determined by the temporal pulse length (Eq. (2.21)). The absolute values of these waves were time superposed and force equilibrium checked by comparing the difference between the incident and reflected waves to the transmitted wave. In the PU30 example shown in Figure 6.2(c), it can be seen that the stresses measured at the IB and OB ends are approximately equivalent with no evidence of an initial inertia peak. The lack of noise in the OB transmitted signal is due to filtering of the high-frequency incident wave components by the sample. Similarly, in the true stress-strain results, the 2-wave and 3-wave analyses feature this noise due to their calculations incorporating IB waves (see Section 2.5). As is conventional, in this work, the 1-wave stress-strain curve was used.

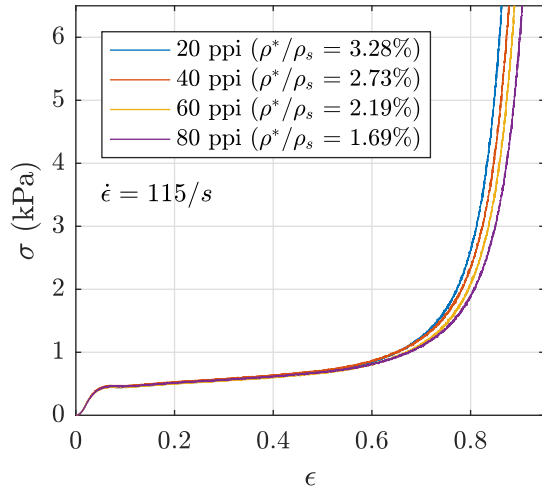
The results of the compressive mechanical characterisation for the polyurethane materials are summarised in Figure 6.3 and Tables 6.1 and 6.2.



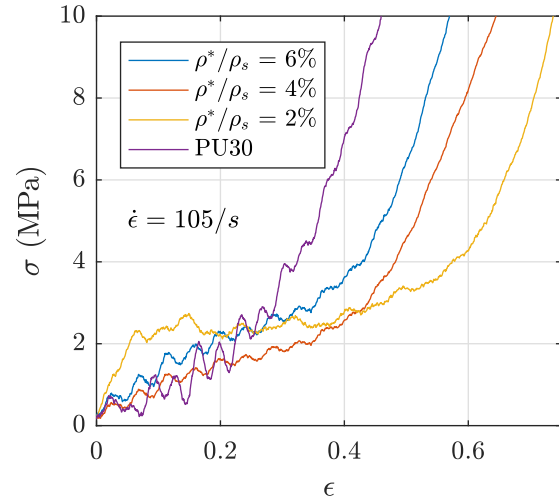
(a) Reticulated polyurethane foam (quasi-static).



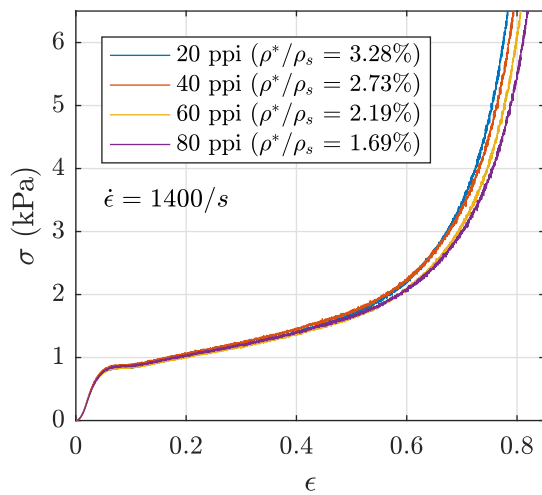
(b) Syntactic PU30 foam (quasi-static).



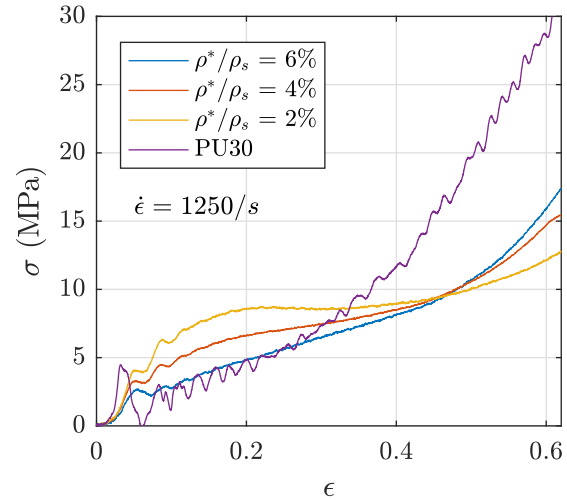
(c) Reticulated polyurethane foam (drop-weight).



(d) Syntactic PU30 foam (drop-weight).



(e) Reticulated polyurethane foam (SHPB).



(f) Syntactic PU30 foam (SHPB).

Figure 6.3: Quasi-static and dynamic characterisation of polyurethane reticulated and syntactic cellular materials. ppi: Pores per inch, SHPB: Split-Hopkinson Pressure Bar, PU30: Shore hardness 30 A polyurethane, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.

Porosity (ppi)	ρ^*/ρ_s (%)	$\dot{\epsilon}$ (s ⁻¹)	E^* (kPa)	σ_{el}^* (kPa)	W (J m ⁻³)
20	3.26 ± 0.07	0.01	8.50 ± 0.43	0.38 ± 0.02	280 ± 14
		115	11.96 ± 0.60	0.54 ± 0.04	336 ± 17
		1400	23.42 ± 1.18	1.22 ± 0.07	522 ± 26
40	2.71 ± 0.07	0.01	8.48 ± 0.39	0.39 ± 0.03	288 ± 14
		115	11.85 ± 0.62	0.53 ± 0.04	340 ± 17
		1400	23.45 ± 1.26	1.30 ± 0.07	529 ± 26
60	2.19 ± 0.05	0.01	8.51 ± 0.39	0.38 ± 0.02	297 ± 15
		115	11.99 ± 0.72	0.56 ± 0.03	329 ± 16
		1400	23.67 ± 1.89	1.34 ± 0.07	568 ± 28
80	1.62 ± 0.04	0.01	8.47 ± 0.54	0.41 ± 0.02	292 ± 15
		115	11.82 ± 0.67	0.54 ± 0.03	349 ± 17
		1400	23.21 ± 1.16	1.38 ± 0.07	572 ± 29

Table 6.1: Summary of reticulated polyurethane foam quasi-static and dynamic compression moduli (E^*), elastic plateau stresses (σ_{el}^*) and energy absorbed per unit volume (W). These material parameters were calculated as the mean value from the stress-strain curves of 3 repeat experiments, with the uncertainty expressed as the standard error of the mean. ppi: Pores per inch, $\dot{\epsilon}$: strain rate, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.

ρ^*/ρ_s (%)	$\dot{\epsilon}$ (s ⁻¹)	E^* (MPa)	σ_{cr}^* (MPa)	W (kJ m ⁻³)
6.0 ± 0.3	0.01	0.54 ± 0.03	0.81 ± 0.04	225 ± 18
	105	7.42 ± 0.37	2.25 ± 0.10	660 ± 53
	1250	64.21 ± 3.25	5.65 ± 0.27	2025 ± 162
4.0 ± 0.3	0.01	3.70 ± 0.22	0.57 ± 0.04	160 ± 13
	105	4.81 ± 0.28	1.59 ± 0.08	512 ± 41
	1250	88.34 ± 4.51	7.03 ± 0.34	2450 ± 196
2.0 ± 0.2	0.01	5.69 ± 0.30	0.121 ± 0.01	38 ± 3
	105	32.19 ± 1.71	2.31 ± 0.13	792 ± 63
	1250	98.01 ± 4.78	8.59 ± 0.45	3210 ± 257

Table 6.2: Summary of syntactic polyurethane foam quasi-static and dynamic compression moduli (E^*), brittle plateau stresses (σ_{cr}^*) and energy absorbed per unit volume (W). These material parameters were calculated as the mean value from the stress-strain curves of 3 repeat experiments, with the uncertainty expressed as the standard error of the mean. ppi: Pores per inch, $\dot{\epsilon}$: strain rate, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.

In Figure 6.3, the characteristic stress-strain response of cellular materials can be observed from the initial linear elastic response, followed by a long stress plateau corresponding to buckling of voids, and finally densification as the skeletal material is compressed. For dynamic loading, strain rates in the range of 60–150 s⁻¹ were achieved with the drop-weight. For the drop-weight, this strain-rate was constant for each given material due to the use of an impactor mass and drop height combination that delivered over an order of magnitude more energy than was absorbed by the material. For the conventional and quartz-crystal instrumented SHPB systems, strain rates were in the range of

1000–1600 s⁻¹.

For the reticulated foams, the deformation was purely elastic with the material recovering its initial length following the test. The linearly elastic regime ended at approximately 5% strain for all strain rates, with densification occurring at approximately 60% strain. Therefore, the reticulated foams exhibited a relatively long stress plateau region characteristic of elastomeric cellular materials. This plateau was relatively flat, followed by a rapid increase of stress upon densification. This approximates idealised foam behaviour (see Figure 2.10 phenomenological model) and may therefore satisfy the assumption of such a material by Wadley *et al.* for the theoretical backing foam thickness required for a ballistic plate (Eq. (2.20)) [116]. This was experimentally investigated in Chapter 9.

By comparison the PU30 syntactic foams exhibited a shorter plateau regime from approximately 5% strain to 50% strain at all strain rates. In terms of stress, while the reticulated foams collapsed at pressures of the order of kPa, the syntactic foams were of the order of MPa due to the stiffer matrix material and microballoons. In Figure 6.3, the PU30 matrix material stress-strain response has also been included. It can be seen that on densification, the rate of stress increase tends to that of the bulk material due to the void space having been removed. This densification occurred at increasingly larger strains with syntactic foam porosity as greater porosity required greater strain to crush all of the pores. There was also greater porosity dependence compared to the reticulated foams, with statistically significant differences existing between 2%, 4% and 6% relative density. This is a likely consequence of the fact these porosities correspond to significant differences in the number density of glass microballoons in the material and the resulting contribution from the crush strength of the microballoon walls. Indeed, the syntactic foams were an example of a brittle crushing cellular material. This can be seen in Figure 6.4 where a syntactic foam underwent a further two compressions following the initial deformation.

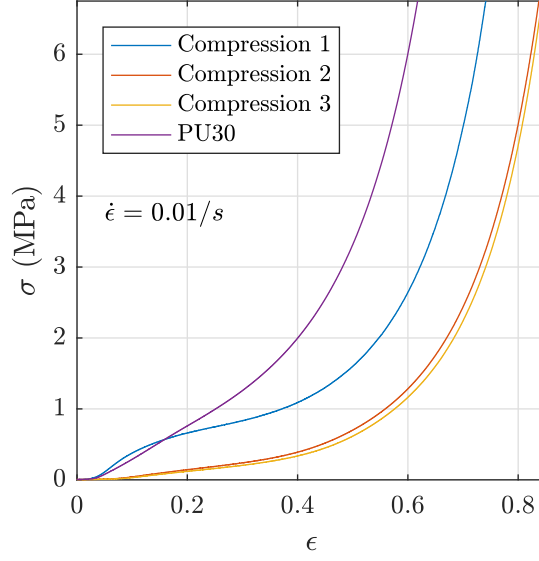


Figure 6.4: Repeated quasi-static loading of $\rho^*/\rho_s = 6\%$ relative density syntactic foam. The material undergoes brittle crushing under the first compression. ρ^* : Foam bulk density, ρ_s : Foam skeletal density.

In the first compression, the stress plateau is visible due to crushing of the microballoons. In subsequent compressions, the foam is analogous to a classical closed-cell material with the crush strength and plateau stress significantly lower due to the lack of supporting microballoon walls ($\sigma_{cr}^* = 0.2 \text{ MPa}$ compared with $\sigma_{cr}^* = 0.9 \text{ MPa}$ for the first compression). These subsequent stress-strain responses are equivalent due to the lack of brittle crushing processes taking place following the first loading. The brittle crushing nature of the foam is also evident in the dynamic stress-strain profiles. While at quasi-static rates, the stress-strain relationship appears smooth due to progressive crushing of voids, under dynamic loading, oscillations are visible in the stress-strain response due to rapid formation of microballoon irregularities (such as clusters), yielding randomly. As such, the dynamic response is analogous to the phenomenological example of a brittle crushing foam proposed by Gibson & Ashby in Figure 2.9 [75].

The strain rate sensitivity of key foam parameters of interest in the work, the plateau stress and energy absorbed per unit volume, calculated according to Eq. (2.13) and Eq. (2.14), is shown in Figure 6.5.

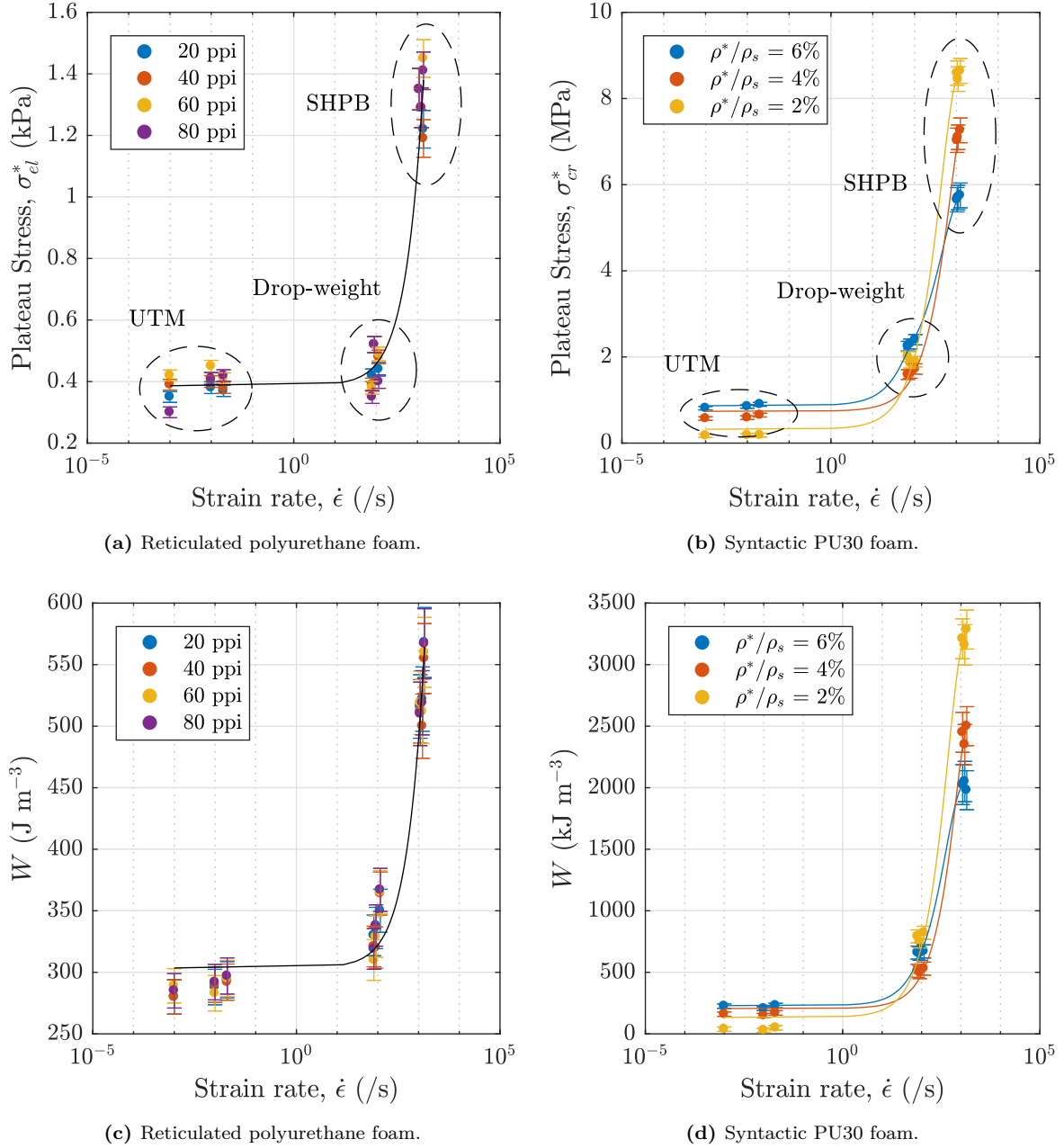


Figure 6.5: Semi-log plots of polyurethane foam plateau pressure (a) and energy absorption (b) strain rate sensitivity. Each data point represent the mean of 3 independent measurements, with the error computed as the standard error of the mean. UTM: Universal Testing Machine, SHPB: Split-Hopkinson Pressure Bar, ppi: Pores per inch, PU30: Shore hardness 30 A polyurethane, σ_{el}^* : reticulated foam plateau stress, σ_{cr}^* : syntactic foam plateau stress, W : foam energy absorbed per unit volume up to densification, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.

Both the reticulated foams and syntactic foams are examples of viscoelastic materials and exhibited strain hardening. The reticulated foams were found to exhibit a near quasi-static response up to $1000 s^{-1}$ strain rate with the drop-weight response being similar to the Instron results. This is in direct agreement with the findings of Ouellet *et al.* for open-cell polymeric foams [184]. This behaviour did not depend on the foam porosities,

with the stress-strain behaviour being independent of the considered porosity range of 20-80 ppi. The elastic moduli E^* , elastic plateau stress and energy absorbed per unit volume were equivalent across ppi values over all strain rates. This highlights that it is the foam's relative density that is crucial in determining the material's mechanical behaviour. Indeed, as shown in Table 4.1, although there is a fourfold range of pores per inch, the relative density variation is less than 2%. At rates beyond 1000 s^{-1} accessed with the SHPB, the greater stiffness of the reticulated foams is visible from the increased elastic moduli and plateau stress. As such, this increase of stiffness implies that foams exhibit greater energy absorbing capability under higher rate loading.

A greater strain rate dependence was observed for the syntactic foams with distinct plateau stresses measured in each of the three strain rate regimes. It is also notable that under dynamic loading, the plateau stress exceeded that for PU30, showing that the addition of microballoon hollow particulate increases the energy absorbed by the material up to the densification strain compared to its bulk counterpart. However, under quasi-static loading, the syntactic foams remain under the bulk PU30 curve. As such, the energy absorbed at high-rate exceeds that under static conditions by a factor of approximately 20 on average for the three syntactic foam porosities. Investigations of the damage evolution in such materials by Shunmugasamy *et al.* indicate that at low-rate, the failure mechanisms are predominantly shear cracking, while at high-rate, longitudinal cracking dominates [185]. While this remains an active area of research, this may provide a physical rationale for the difference in energy absorption between quasi-static and dynamic loading. Unlike the reticulated foams, the stress-strain sensitivity to strain rate differs according to the syntactic foam porosity. For the most porous foam with 2% relative density foam, the material exhibits the lowest stress plateau under quasi-static compression and no energy absorption benefit relative to bulk PU30. However, at the highest loading rates, the loading pressure is sufficient to effectively collapse the microstructure and results in a two order-of-magnitude increase of energy absorption.

6.4 Hydrogels

The loading of soft low impedance materials at high strain rate using the SHPB requires especially careful consideration of sample inertia as the radial contribution at the start of loading can contribute to the signal. To counteract this, the 9 mm SHPB hydrogel samples were cored with a 3 mm diameter biopsy punch (Kai Medical BP-30F) to create an annular sample geometry. The effect of this is illustrated in Figure 6.6.

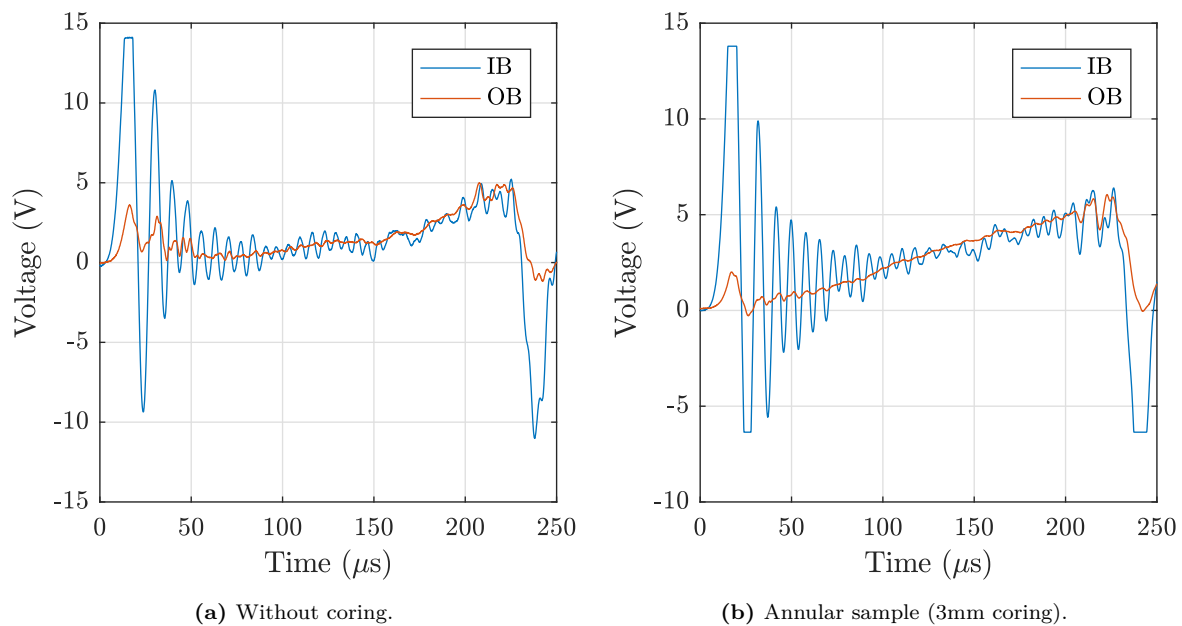


Figure 6.6: High-sensitivity Split-Hopkinson Pressure Bar (SHPB) Input Bar (IB) and Output Bar (OB) quartz-crystal voltage signals for 1.5% (w/v) agarose hydrogel without (a) and with (b) sample coring. Coring reduces initial radial inertia and reduces the time to force equilibrium.

The radial inertia contribution is visible as the initial peak in the voltage signal. It can be seen in the Figure 6.6 example that the sample coring acts to reduce this peak by approximately 50%. It can also be observed that anomalous oscillations that do not reflect the material's axial response occur at later times (after 150 μs) due to inertia as the radially displaced sample mass oscillates about the axis centre. The annular samples did not exhibit these features.

An additional important consideration for dynamic testing of soft samples is force equilibrium due to the slow sound speed in the material resulting in a long period of time for ringing up of the stress state. Prior ultrasound measurements have found that the

sound speed of such agarose hydrogels is approximately 1500 m s^{-1} [175]. It can be seen in Figure 6.6 that force equilibrium between the IB and OB is achieved after approximately $100 \mu\text{s}$. It is therefore not possible to infer the elastic modulus in this region at low strain. The uniaxial compression mechanical characterisation results for the homogeneous and two-phase hydrogels are displayed in Figure 6.7.

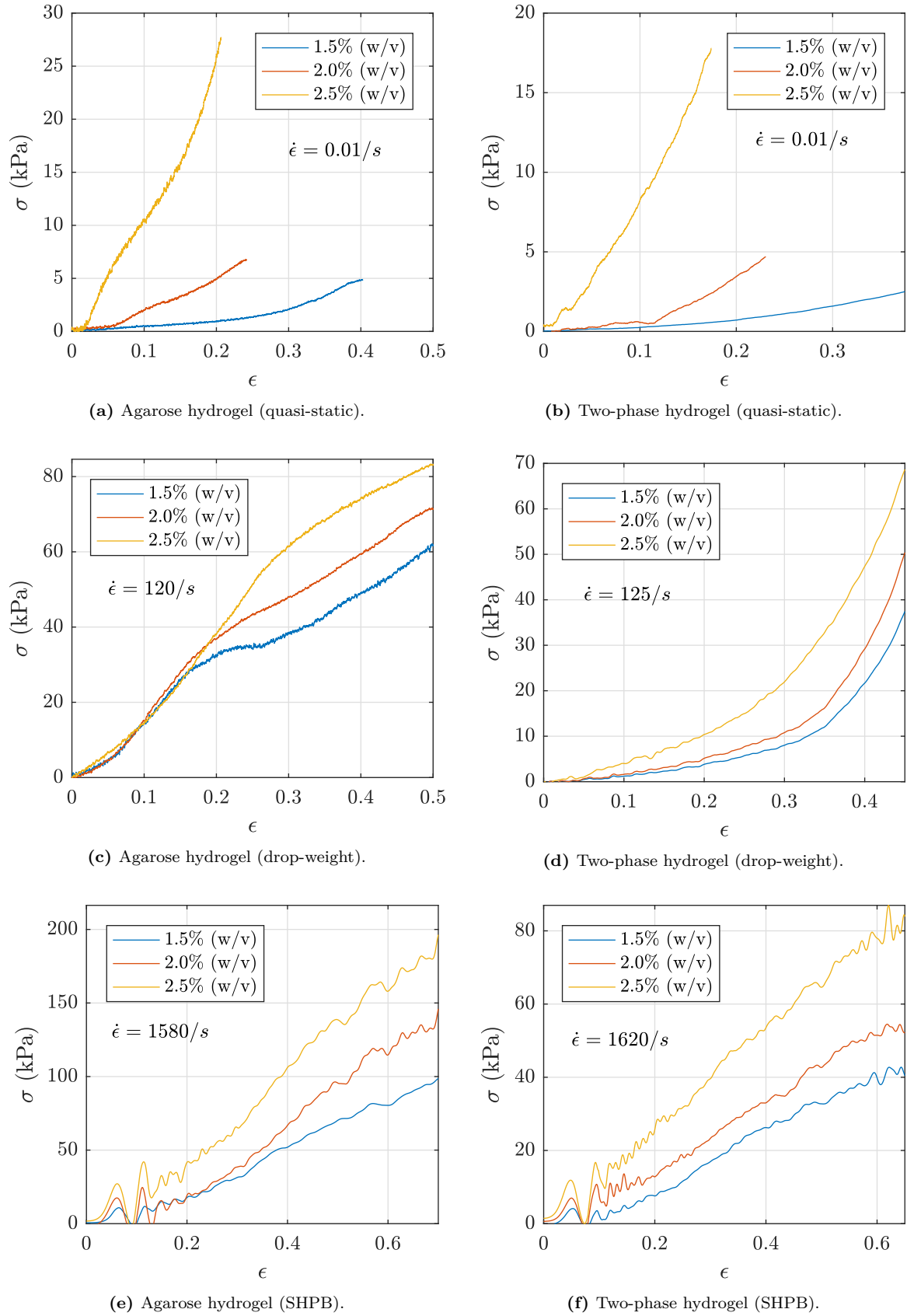


Figure 6.7: Quasi-static and dynamic characterisation of homogeneous and two-phase agarose hydrogels. The curves are plotted up to the material's failure strain. SHPB: Split-Hopkinson Pressure Bar.

It can be seen that the hydrogel response is approximately linear under quasi-static loading but increasingly non-linear with loading rate with a failure strain that correlates with strain rate. No statistically significant difference of elastic modulus was observed between the quasi-static strain rates (see Table 6.3).

Type	Hydrogel Concentration % (w/v)	$\dot{\epsilon}$ (s ⁻¹)	E (kPa)
Homogeneous	1.5	0.001	5.78 ± 1.15
		0.01	5.21 ± 1.02
		0.02	4.89 ± 0.88
	2.0	0.001	31.11 ± 6.14
		0.01	28.65 ± 5.72
		0.02	34.88 ± 6.85
	2.5	0.001	100.37 ± 21.55
		0.01	89.45 ± 19.20
		0.02	103.54 ± 22.78
Two-phase	1.5	0.001	5.87 ± 1.85
		0.01	5.46 ± 1.01
		0.02	6.26 ± 1.87
	2.0	0.001	30.30 ± 6.59
		0.01	28.18 ± 8.46
		0.02	25.30 ± 5.81
	2.5	0.001	92.02 ± 16.44
		0.01	103.65 ± 24.13
		0.02	109.47 ± 21.64

Table 6.3: Summary of quasi-static homogeneous and two-phase hydrogel compression elastic moduli (E). These moduli were calculated as the mean value from the stress-strain curves of 3 repeat experiments, with the uncertainty expressed as the standard error of the mean. $\dot{\epsilon}$: strain rate.

Though analogous results for dynamic loading could not be computed due to lack of force equilibrium, it can be seen from the dynamic stress-strain curves that the hydrogels exhibited strain stiffening under compression. Small changes of hydrogel concentration also resulted in significant changes in the mechanical response of the material. For these materials, the material stiffness and stress values scaled with the hydrogel concentration, with 2.5% (w/v) hydrogel exhibiting the greatest stiffness. The curves are plotted up to the material's failure strain, following which the stress dropped near instantaneously (see Figure 6.6) and corresponded to brittle failure of the material. Likewise, the small secondary oscillations visible in the high strain rate dynamic stress-strain plots correspond to crack formation in the material.

The two-phase hydrogels also exhibited non-linearity, high strain rate sensitivity and brittle failure but at lower failure stresses (σ_f) than their homogeneous counterparts. This is summarised in Figure 6.8.

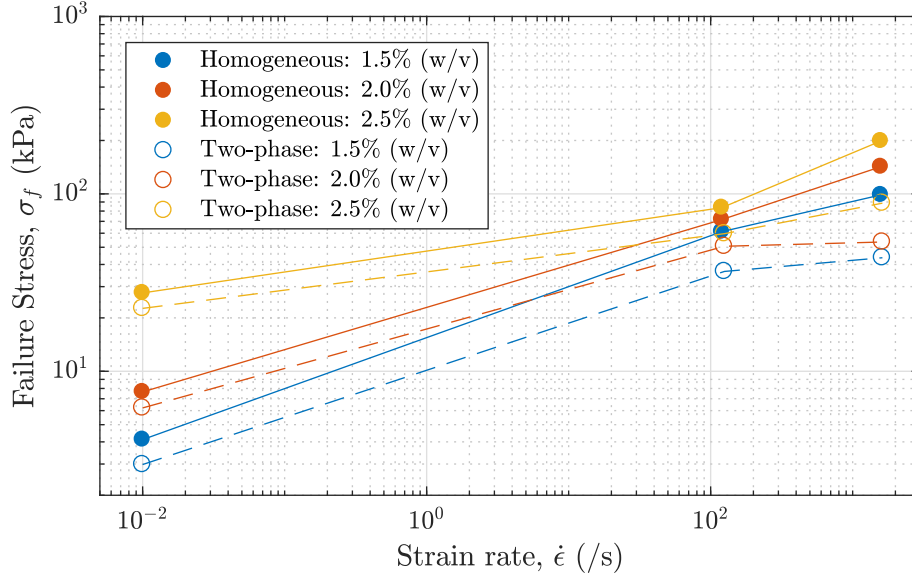


Figure 6.8: Log-log plot of hydrogel failure stresses across quasi-static and dynamic strain rates. Filled and unfilled markers denote homogeneous and two-phase materials, respectively. Failure stresses were calculated from the mean of 3 repeat measurements of the stress-strain curve for the materials, with the uncertainty computed as the standard error of the mean. Within these error bounds, the distinction between the homogeneous and two-phase materials for a given matrix concentration remained significant. Due to the logarithmic vertical axis, these error bounds are not displayed here for clarity.

The difference in failure stress between the homogeneous and two-phase hydrogels was found to strongly increase with strain rate. Under quasi-static conditions, the two material types exhibited very similar stress-strain response. Indeed, the elastic moduli in this loading regime are equivalent within error bounds in Table 6.3. However, the material ultimate compressive stresses diverge with strain rate. Indeed, under SHPB loading, it can be seen that the lowest concentration 1.5% (w/v) hydrogel exhibits a higher failure stress than the highest concentration two-phase hydrogel. Physically, the two-phase response is dominated by the matrix material under quasi-static loading with the addition of agarose beads only distinguishing these materials from the homogeneous sample at high strain rate. At such high strain rates, the heterogeneities of the structure promote faster crack formation and a lower ultimate compressive strength. This can be exploited for blast mitigation purposes as the two-phase material can be made to fail at lower stresses than its homogeneous counterpart under dynamic loading while maintaining

sufficient structural integrity under static loading to enable practical manufacture of a self-supporting panel.

6.5 Gelatine

It is important to understand the constitutive behaviour of tissue simulants as it is often not possible for experimenters to utilise mechanically reproducible animal or human tissues due to their inherent inhomogeneities and differences in storage or preparation protocols. There is currently a paucity of data characterising ballistic gelatine over a wide range of dynamic loadings, with notable prior work carried out by Caillou *et al.* [186], Juliano *et al.* [187] and Koene *et al.* [188]. This is likely a result of the lack of testing platforms that cover the wide range of strain rate regimes required by a viscoelastic material, and the experimental challenges of testing soft and low impedance materials at high strain rate. The stress-strain results utilising the UTM, drop-weight and quartz-crystal embedded SHPB are shown in Figure 6.9. The corresponding elastic moduli near zero strain are summarised in Table 6.4.

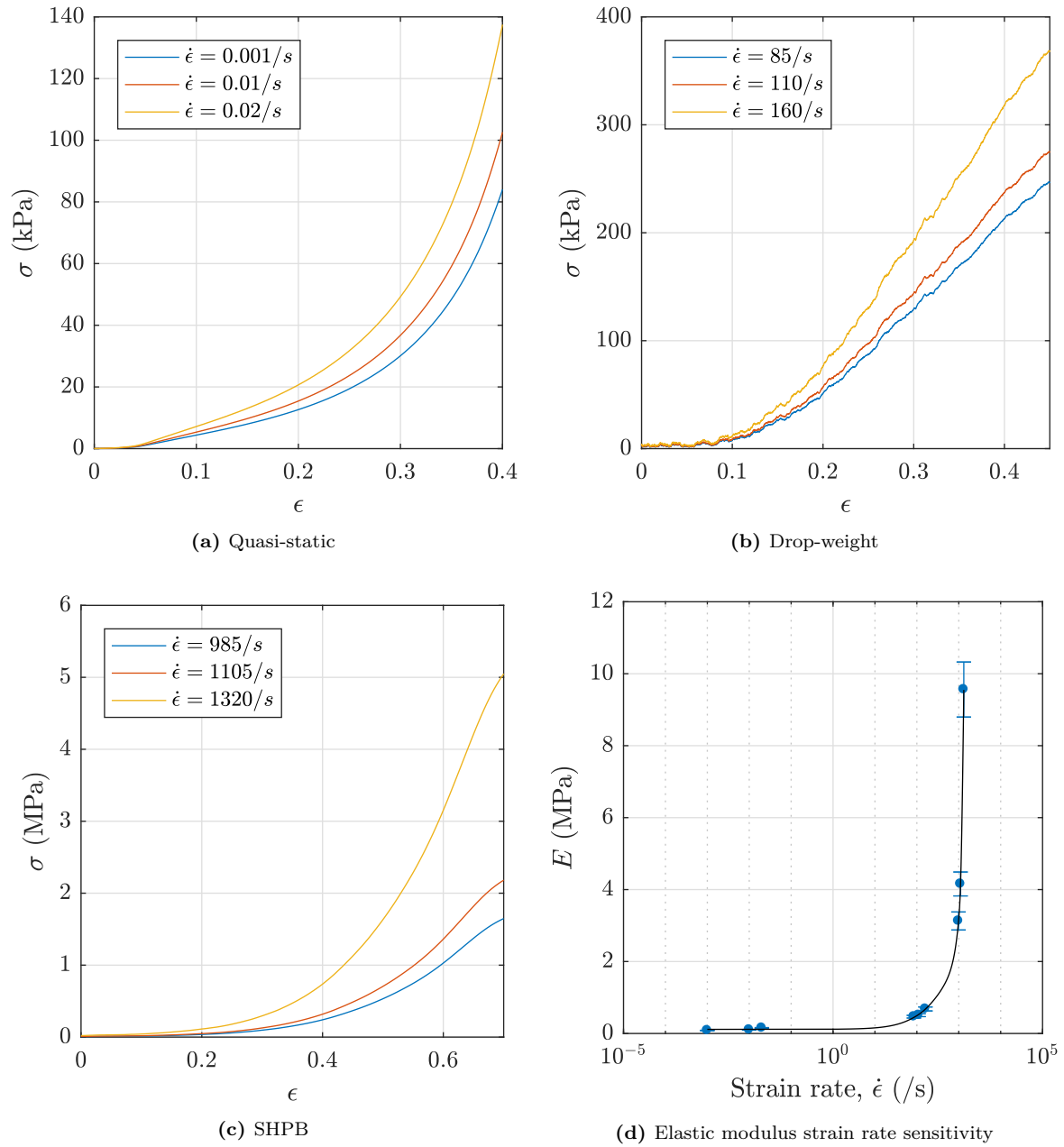


Figure 6.9: Quasi-static and dynamic characterisation of Type A 300 Bloom North Atlantic Treaty Organisation (NATO) specification 20% (w/w) ballistic gelatine. Each point in the elastic modulus strain rate sensitivity (d) represents the average of 3 independent measurements with the uncertainty computed as the standard error of the mean. SHPB: Split-Hopkinson Pressure Bar.

$\dot{\epsilon}$ (s ⁻¹)	E (MPa)
0.001	0.084 ± 0.007
0.01	0.103 ± 0.008
0.02	0.156 ± 0.011
85	0.467 ± 0.039
110	0.522 ± 0.046
160	0.682 ± 0.052
985	3.128 ± 0.259
1105	4.156 ± 0.368
1320	9.569 ± 0.797

Table 6.4: Summary of gelatine quasi-static and dynamic compression moduli (E). These moduli were calculated as the mean value from the stress-strain curves of 3 repeat experiments, with the uncertainty expressed as the standard error of the mean. $\dot{\epsilon}$: strain rate.

The stress-strain results indicate that gelatine is a strongly viscoelastic and also non-linear material with high strain rate sensitivity. The quasi-static elastic moduli are in agreement with the measurements by Juliano *et al.* [187] of $E = 96 \pm 12$ kPa at 0.001 s^{-1} strain rate for the NATO specification gelatine. This also indicates that the gelatine preparation method outlined in Section 4.5 was reproducible and is representative of the gelatine preparations used by other experimenters. Similarly, at the higher strain rates imposed by the drop-weight, the increase of material stiffness to 467–682 kPa agrees with the average Young’s modulus determined by Koene *et al.* of $E = 509$ kPa at a strain rate of 118 s^{-1} [188]. At the highest rates of 985–1320 s^{-1} , the measured elastic moduli were within 20% of those measured previously by Caillou *et al.* and Koene *et al.* (using polymeric SHPB bars) [186, 188]. Koene *et al.* note that such differences may emerge at high strain rate due to the differences in gelatine preparation technique rendering some material properties stochastic and experimenter dependent [188]. Indeed, the NATO specification stipulates only the gelatine concentration while the details of the preparation method are not standardised. A physical explanation for the increasing stiffness of gelatine with strain rate proposed by Kwon & Subhash related the behaviour to internal energy dissipation mechanisms due to the cross-linked gelatine structure, as well as hydro-cluster formation [189]. Indeed, such micro-structural variations could be significant from one preparation technique or batch to another.

To assess the validity of utilising this gelatine material as a thoracic surrogate, a summary of the mechanical properties of the main constituent tissues of the thorax are shown in Table 6.5. A schematic of the range of elastic moduli for human tissues is also displayed in Figure 6.10.

Tissue	$E(\text{MPa})$	Density (kg m^{-3})	Study
Thoracic Viscera	8.42×10^{-3}	1000	[190, 191]
Skin	10	1100	[192]
Visceral Skin	5	1200	[192]
Diaphragm	10	-	[193]
Sternal Cartilage	15	1200	[192]
Blood Vessels	5	1000	[193]
Intercostal Muscles	2.5	1000	[193]
Costal Cartilage	24.5	1500	[191]
Ribs	2790	1500	[193, 194]

Table 6.5: Human elastic moduli (E) and densities of anterior-facing thoracic tissues tested under quasi-static compression.

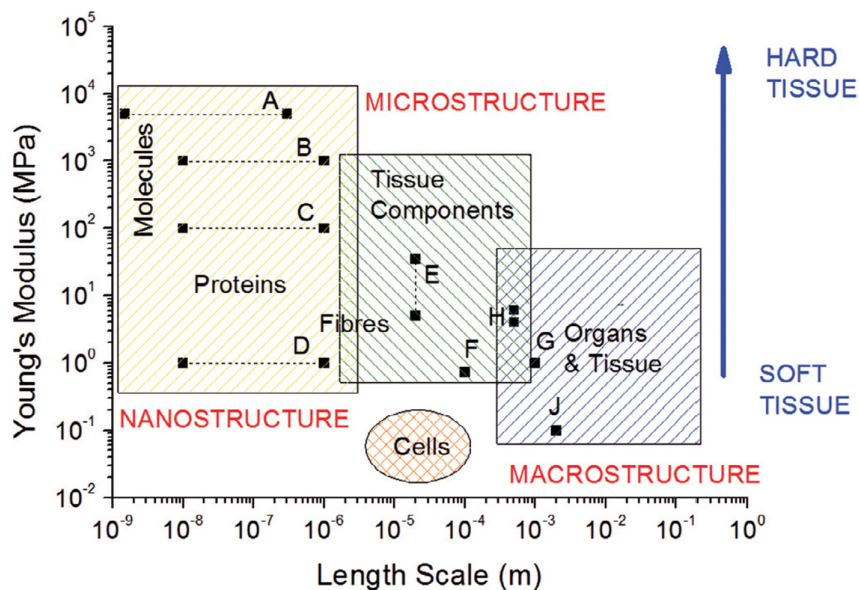


Figure 6.10: Human tissue length-scale and elastic property regimes. Reproduced from [195].

It is apparent that the tissues constituting the thorax occupy a wide range of mechanical properties, with the greatest contrast existing between the ribs and viscera (the soft organ content within the chest wall). It has been established that bone elastic modulus depends strongly on age, with young adults exhibiting a softer bone structure as the outer cortical bone develops[194]. Several researchers have worked towards developing thoracic finite element models which have required the validity of certain simplifications

to be investigated in order to reduce computational cost [191, 193]. As part of this Furusu *et al.* developed a model for side impact of the thorax at velocities of 3.3 m s^{-1} and 5.7 m s^{-1} and found that the internal cavity could be modelled as a homogeneous elastic material with good agreement to cadaveric data [191]. Given this result and also that the thoracic soft viscera constitute the greatest proportion of the chest cavity, the homogeneous NATO ballistic gelatine was considered a suitable zeroth-order approximation of the human thorax. It can be seen in the Figure 6.10 phase diagram that the gelatine for all strain rates occupies the organs and tissues region. As the objective of this work is to investigate pressure transmission, the gelatine impedance was considered to be representative of the bulk of the thorax. Indeed, its elastic modulus lies between that of the soft viscera and harder tissues, while its density (1050 kg m^{-3}) is similar to that of the biological structure.

6.5.0.1 Constitutive Modelling

A number of experimenters have investigated constitutive modelling of ballistic gelatine including Koene *et al.* [188], Ravikumar *et al.* [196], Naarayan & Subhash [197] and Pasumarthu & Tippur [198]. Such work has primarily concentrated on quasi-static data relevant to surgical strain rates, with models that can effectively replicate the strain rate sensitivity of gelatine remaining elusive. Koene *et al.* have previously noted that the Johnson-Cook strength model poorly models gelatine's stress-strain response and suggest investigation of alternative models [188].

In this work, the stress-strain data obtained from the mechanical compression experiments was used to calibrate well-established hyperelastic constitutive models. Three such incompressible and isotropic models were considered, including the Ogden, Mooney-Rivlin and Neo-Hookean models. Respectively, the three models relate sample true stress to stretch ratio $\lambda = e^\epsilon$ (with $\lambda < 1$ for compression) as:

$$\sigma_{OG}(\lambda) = \frac{2\mu}{\alpha}(\lambda^{\alpha-1} - \lambda^{-\frac{1}{2}\alpha-1}), \quad (6.5)$$

$$\sigma_{MR}(\lambda) = 2(C_{10} + C_{01})(\lambda - \frac{1}{\lambda^2}), \quad (6.6)$$

$$\sigma_{NH}(\lambda) = 2\psi\lambda(\lambda - \frac{1}{\lambda^2}), \quad (6.7)$$

where μ , α , C_{10} , C_{01} and ψ are material parameters determined by fitting of the models to the experimental data. These models describe viscoelastic material strain hardening, non-linearity and viscous flow characteristics, and are commonly available in finite element software. The fitting of these models to the Figure 6.9 experimental data for strain rates of 0.01 s^{-1} , 110 s^{-1} and 1105 s^{-1} is shown in Figure 6.11, with the fitting parameters summarised in Table 6.6.

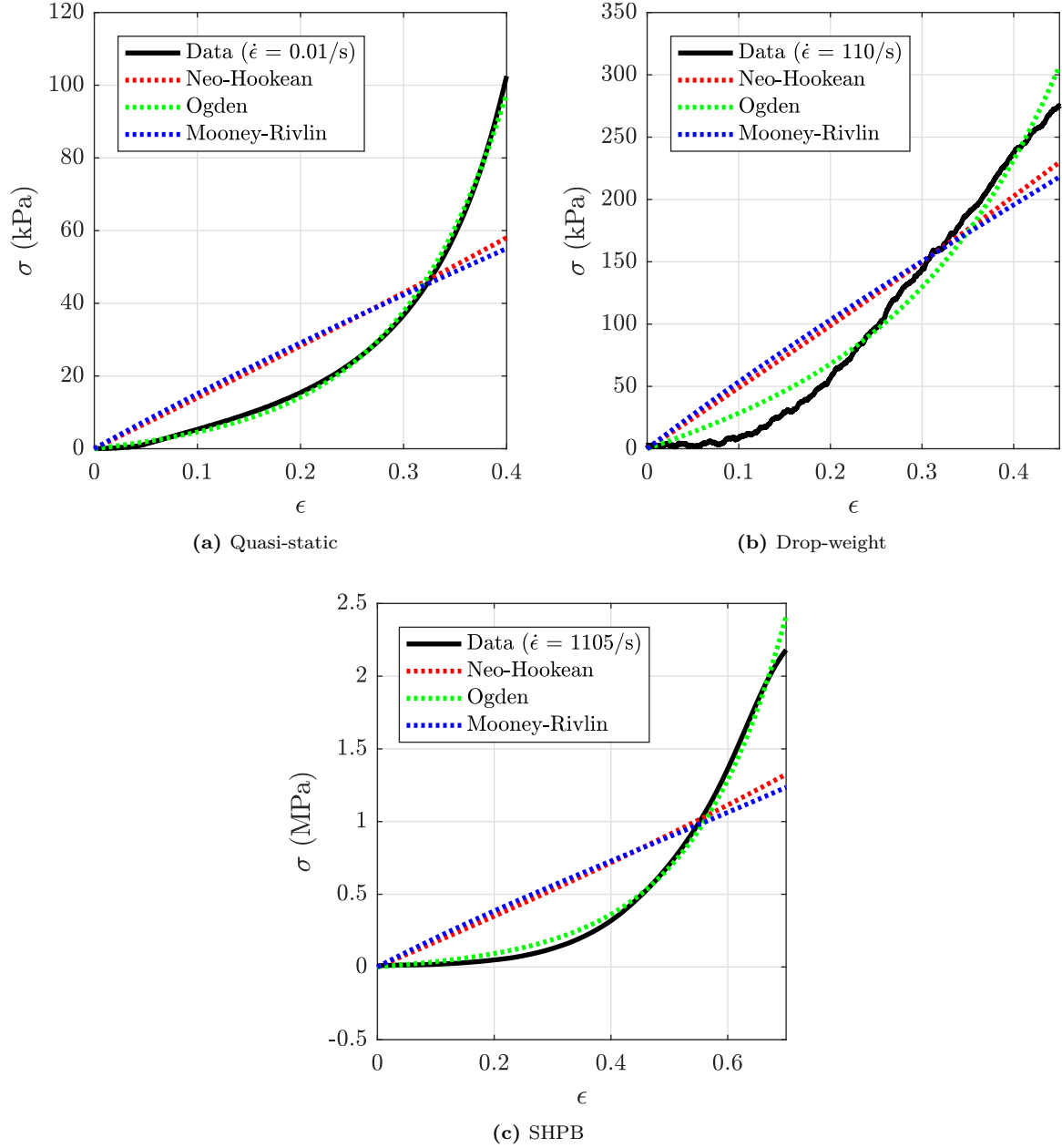


Figure 6.11: Least-squares fitting of Neo-Hookean, Ogden and Mooney-Rivlin hyperelastic constitutive models to gelatine stress-strain experimental data across quasi-static and dynamic strain rates. SHPB: Split-Hopkinson Pressure Bar.

$\dot{\epsilon}$ (s^{-1})	Neo-Hookean		Ogden			Mooney-Rivlin		
	ψ	R^2	μ	α	R^2	C_{10}	C_{01}	R^2
0.01	23.30 kPa	0.76	11.74 kPa	10.43	1.00	28.30 kPa	-1.89 kPa	0.72
110	81.34 kPa	0.88	86.13 kPa	6.46	0.98	45.90 kPa	47.95 kPa	0.84
1105	0.29 MPa	0.69	0.11 MPa	7.28	0.99	2.49 kPa	0.35 MPa	0.63

Table 6.6: Hyperelastic model parameters for fitting to experimental quasi-static and dynamic compression data. The model goodness-of-fit is quantified as the coefficient of determination.

From the least squares fittings of these models, it was apparent that the Ogden model provided a robust description of the gelatine material behaviour across all strain rates,

with the Neo-Hookean and Mooney-Rivlin models exhibiting a poor fit. The Ogden model utilises multiple constants with α appearing as an exponent of λ , controlling the function's power dependence on strain. As such, compared with the Neo-Hookean model's use of a single constant and the Mooney-Rivlin model's use of only a multiplying constant (the sum of C_{10} and C_{01}), the Ogden model provides greater versatility to reflect the complexity and non-linearity of the material. Comparing to literature, a study carried out by Forte *et al.* for FBI specification gelatine (10% (w/w)) found that such gelatine was described by Ogden parameters of $\mu = 6.21$ kPa and $\alpha = 2.64$ under quasi-static loading [199]. In this work, the values of $\mu = 11.74$ kPa and $\alpha = 10.43$ reflect the greater stiffness of the NATO specification gelatine. Based on these results, the Ogden model was used to describe the gelatine modelled as part of finite element computational simulations discussed in Chapter 7.

Chapter 7

Computational Simulations

Explicit finite element analysis of the shock tube blast loading was carried out using the commercial Abaqus/CAE code (Simulia, Dassault Systèmes). By simulating material response to this loading, insight could be gained into the dynamic material response. Several previous investigators have employed an uncoupled simulation approach using multiple software products to apply fluid domain loading to a solid domain structure. Varas *et al.* used a Computational Fluid Dynamics (CFD) code which solves the compressible and inviscid form of the Navier-Stokes equations to model the blast loading pressure and mapped this onto a head surrogate [200]. The surrogate was modelled as a purely elastic material with density 960 kg m^{-3} and modulus $E = 46.56 \text{ kPa}$. While this accurately represented the experimental pressures measured in the brain, this co-simulation technique over-predicted the impulse. A similar co-simulation approach by Roberts *et al.* using the uncoupled technique found analogous results for a head form model loaded by an open shock tube [201].

In this chapter, the methodology, results and validations are outlined for the cases of modelling shock tube loading and coupling of the loading into ballistic gelatine. Further simulations are detailed in Chapter 9 alongside the corresponding experimental results.

7.1 Coupled Eulerian Lagrangian Method

As an alternative to the co-simulation technique, the method presented here was to employ a three-dimensional Coupled Eulerian Lagrangian (CEL) model. This approach involves modelling a body of air (Eulerian domain) and allowing it to propagate into the solid (Lagrangian domain). In comparison to the CFD technique, this method uses a fluid EoS that relates pressure density and specific energy, and combines it with the compressible form of the Navier-Stokes equations [202]. Combining this with the EoS of the solid structure, the CEL method solves the FSI with the solid and fluid behaviour simultaneously for each time step. As this uses a single solver, this has the advantage of being less computationally expensive than co-simulation and not requiring a perfect match between the structural geometry in the finite element software and CFD fluid domain. On the other hand, it loses accuracy at very low and high flow speeds. For blast modelling, this technique has been proposed by Mougeotte *et al.* [203], Jablonski *et al.* [204], and Grimm *et al.* [205] as a method that may be more advantageous for modelling the complexity of the FSI (see Figure 2.14). The method requires the solver to permit partial filling of the Eulerian mesh as the air pressure, density and volume vary dynamically. The algorithm can be considered to have two phases in which all material is deformed at each time increment by a Lagrangian treatment, followed by a remapping step for the Eulerian air domain. In this second step, over-closures of the Lagrangian and Eulerian domain are governed by a general contact algorithm. Experimental validation of the Eulerian model was first carried out, followed by the more complex CEL model.

7.2 Shock Tube Loading

The Eulerian component of the model, the air domain, was first modelled to validate the ability of the method to simulate the blast loading of the open and closed shock tube configurations. These dynamic explicit simulations were run with a physical time increment of $0.5\ \mu\text{s}$ over simulation periods in the range of 10–20 ms.

7.2.1 Workflow

Define Geometry Models

For the CEL method, three models were defined as extrusions along the shock tube loading z -axis:

- Eulerian air domain equal to the CBIS shock tube dimensions. The geometry was partitioned into the driver and driven sections to later allow the high pressure initial condition for the driver to be defined independently. This treats the divide between the driver and driven sections as a perfect discontinuity. In reality, the diaphragm will exhibit bulging prior to rupture, launching a slightly non planar shock front. The geometry was also partitioned at the locations of the laboratory pressure transducers to allow positioning of sensors for comparison between experimental and numerical results.
- A dummy reference shell geometry equal in geometry to the Eulerian part. This allows the Eulerian domain volume to be initialised filled with the air material definition.
- Lagrangian domain. For modelling of the shock tube loading alone, the Lagrangian domain constituted a discrete rigid shell to represent the non-deforming shock tube walls.

Define & Assign Eulerian Model

The Eulerian air material was modelled as an ideal gas. From the analytical framework for the shock tube discussed in Section 2.6, it was found that this was a good description for the system. The air model was required to describe the ambient air conditions and would apply to the initial state of the driven section free-field, as well as the outlet pressure for an open shock tube. The initial temperature was set to the laboratory room temperature and the initial pressure equal to ambient absolute pressure. Setting this reference pressure

allowed subsequent pressure probe measurements to be measured as gauge pressure (as in the laboratory) as well as prevent a non-physical zero pressure vacuum at the Eulerian domain boundaries. The ideal gas EoS properties are summarised in Table 7.1.

Parameter	Value
Gas Constant	$287 \text{ J kg}^{-1} \text{ K}^{-1}$
Specific Heat at Constant Volume	$719 \text{ J kg}^{-1} \text{ K}^{-1}$
Initial Temperature	284 K
Initial Density	1.22 kg m^{-3}
Initial Absolute Pressure	101 kPa

Table 7.1: Ideal gas Equation of State (EoS) properties for the Eulerian air domain in Coupled Eulerian Lagrangian (CEL) simulations.

In conventional Lagrangian analyses, assignment of a material section to a part implies that the part is entirely filled with the referenced material. As a result, throughout the simulation, the geometry of the part will define the geometry of the material. However, in the Eulerian analysis, assignment of the Eulerian material section to the Eulerian geometry implies only that the reference material may be present in the part during the analysis. However, initially, the geometry is empty and does not contain the material (void space). As such, the initial condition of the part being filled with the air definition must be specified through a material assignment.

Meshing

The parts were meshed using an advancing front algorithm which generates quadrilateral elements at the mesh boundary and systematically meshes towards the interior of the part. As this method follows the seeding of the part, this algorithm was well-suited to the uniform geometry of the shock tube as it generates elements with a more uniform size and aspect ratio. By contrast, the medial axis method decomposes the part into small regions, but this was unnecessary for the shock tube geometry and created greater element variation where small elements can have an adverse effect on the minimum required time step.

The Eulerian domain was modelled as an explicit EC3D8RT thermally coupled 8-node

linear brick with a hexahedral geometry. The dummy reference part was meshed using S4R 4-node elements for thin shells, with the same mesh size as the Eulerian part to ensure 1:1 filling of the Eulerian domain with air. Finally, the shock tube walls were modelled as explicit non-deforming discrete rigid R3D4 elements. The choice of mesh size used is discussed in Section 7.2.3.

Predefined Field Initial Conditions

Three predefined fields describing the shock tube initial conditions were defined in the initial simulation step. The first was a material assignment specifying the filling of the Eulerian domain with air. This was achieved by defining a discrete field with the Volume Fraction Tool. The Eulerian part was filled with high accuracy according to the dummy reference part volume. Following this, the dummy was suppressed as it did not have an active role in the simulation.

Secondly, the initial temperature conditions for the whole model were set at the room temperature of 294 K. There is no significant temperature variation in the driver section as it was charged quasi-statically with any heating rapidly dissipated by the shock tube walls which effectively acted as a large heat reservoir.

Finally, the driver pressure was defined (gauge pressure). This was defined as a stress tensor. Pressure is simply the negative of the isotropic hydrostatic stress (σ_{hyd}) as it acts compressively. As such, the initial stress state of the driver section, the diaphragm burst pressure P_4 , was defined under equilibrium conditions as:

$$\sigma_{hyd} = \begin{bmatrix} -P_4 & 0 & 0 \\ 0 & -P_4 & 0 \\ 0 & 0 & -P_4 \end{bmatrix}. \quad (7.1)$$

The value of P_4 could be arbitrarily adjusted to reflect the diaphragm burst pressure of the experiment. Note that the driven section pressure did not have to be predefined

as the ambient pressure was inherently defined in the air material properties shown in Table 7.1. The initial pressure conditions are shown in Figure 7.1.

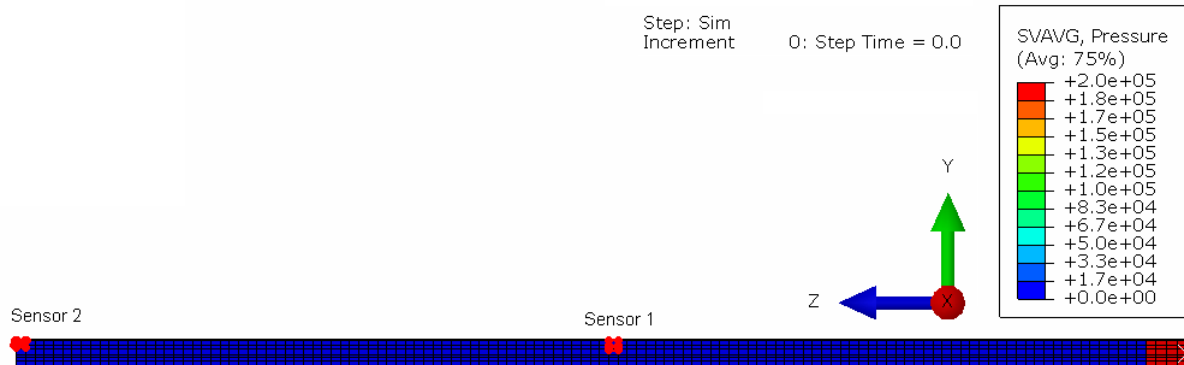


Figure 7.1: Shock tube initial driver and driven section pressure conditions for a blanking volume 2 bar burst pressure (Mylar[®] 23 μm) shot. Scale bar units in Pa. SVAVG: Volume-averaged stress components.

Create Assembly

The part instances were arranged in space. For the example of shock tube loading alone, the Eulerian, air reference dummy, and shock tube walls occupied the same volume with identical positioning. In this stage, the sensor elements were defined by selecting the appropriate sensor positions in the Eulerian domain.

Define Boundary Conditions

Two sets of boundary conditions were defined for the Eulerian domain throughout the simulation step, applying to the end-walls of the shock tube and curved walls, respectively. These are shown in Figure 7.2.



Figure 7.2: Regions for normal end-wall (a) and transverse (b) wall boundary conditions.

The transverse boundary conditions applying to the continuous curved wall of the shock tube specified zero velocities (v) in the x and y directions to provide lateral containment to the air. For the closed system, the end-wall boundaries were specified with zero z -direction velocity ($v_z = 0$), resulting in reflection of the loading wave. For the open system, the driver section end-wall was instead defined an Eulerian boundary outlet with zero inflow and outflow at zero pressure, allowing evacuation of the air in the system.

Define Output Parameters

It is necessary to define the physical parameters of interest in the analysis and the rate at which these parameters are output to the results database. Field output requests generate output across the entire model (such as pressure or temperature distribution). In this work, the Eulerian domain volume-averaged stress components and invariants (SVAVG) were requested. As this is computationally expensive across the model, this output was requested at a low evenly spaced rate of every 2000 time increments. History output requests provide data for specific elements, and so this was requested for the Sensor 1 and Sensor 2 elements for pressure every 10 time increments. This enabled comparison of the numerical and experimental pressure histories.

7.2.2 Experimental Validation

The numerical results of the Eulerian domain simulations for both open and closed boundary conditions were compared with blank shot experimental results to validate the CEL method. An example field output request for the pressure distribution in the shock tube is shown in Figure 7.3

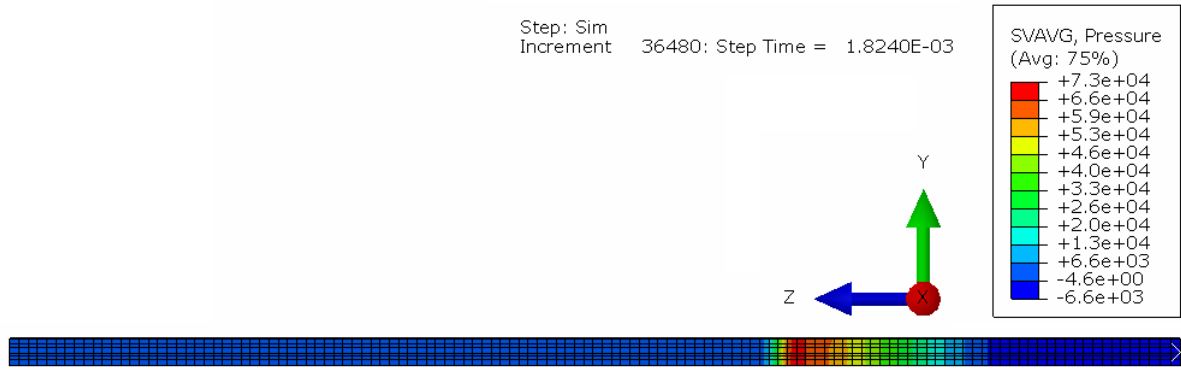


Figure 7.3: Pressure distribution in the simulated closed shock tube 1.8 ms after bursting of the diaphragm for a blanking volume 2 bar burst pressure (Mylar[®] 23 μ m) shot. Scale bar and time units in Pa and seconds, respectively. SVAVG: Volume-averaged stress components.

The variation of pressure amplitude across the rarefaction fan behind the shock front is visible. A comparison of the pressure profiles measured at the two sensor locations is shown in Figure 7.4. For the closed boundary condition, Sensor 2 was location centrally on the end-wall head-on to the blast.

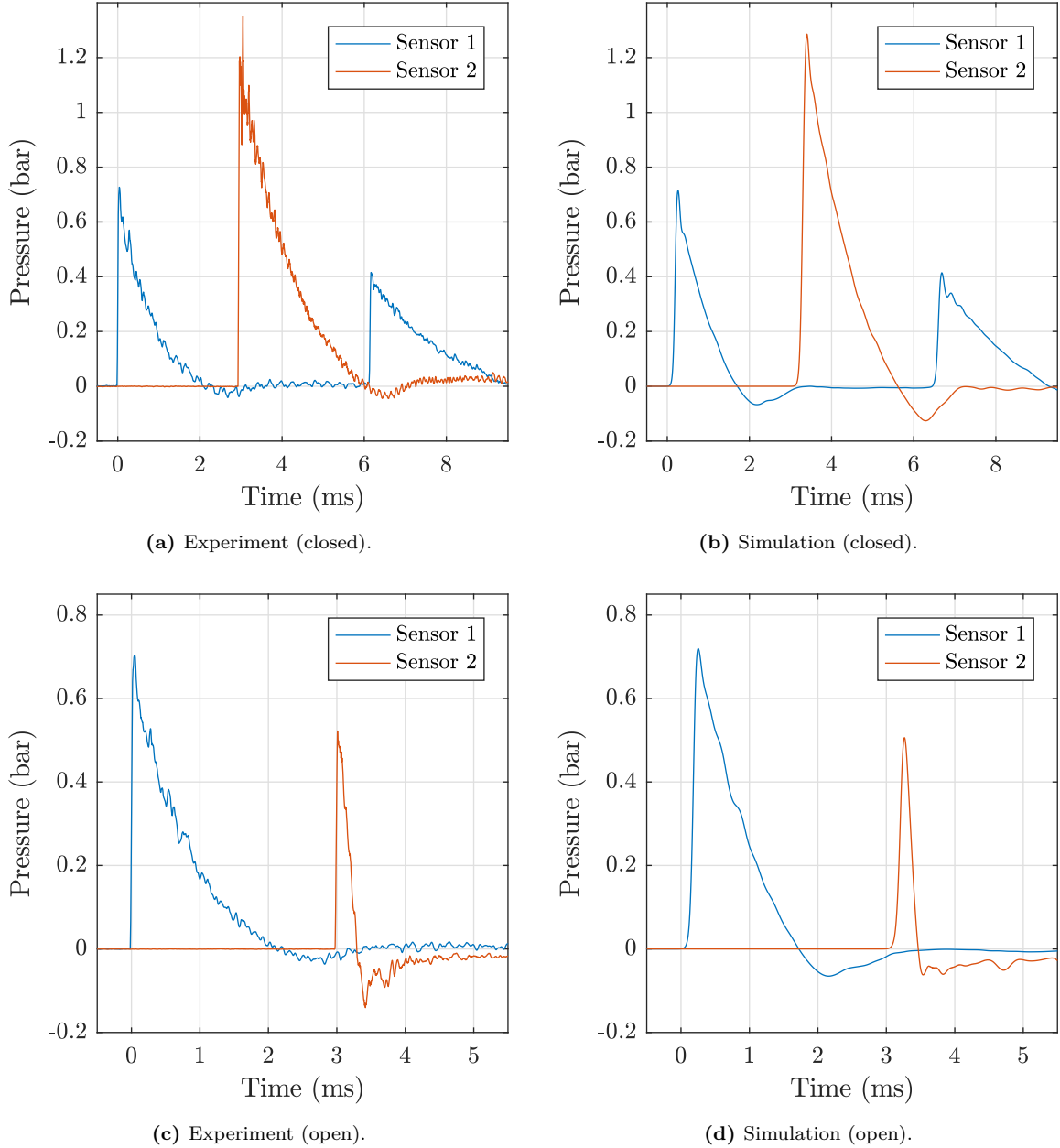


Figure 7.4: Comparison of measured and numerical pressure histories for the open and closed shock tube configurations for blanking volume 2 bar burst pressure (Mylar[®] 23 μ m) shots. A 10 mm Eulerian mesh element size has been used.

It was observed that the CEL simulations provided close agreement with the experimental profiles for peak pressure, impulse, loading duration and Mach number. A small discrepancy was evident in the open shock tube sub-atmospheric pressure regime. This was likely a consequence of the outlet Eulerian boundary condition not accounting for the details of the blast interaction and stagnation with the outside environment. It was also necessary to examine the dependence of these results on the coarseness of the Eulerian mesh to obtain a converged solution.

7.2.3 Mesh Convergence

The Eulerian mesh sensitivity of the shock tube simulations was investigated for mesh sizes in the range of 2.5 mm to 30 mm. This was necessary to obtain an appropriate compromise between physical accuracy and computational expense. A graphical illustration of the mesh sizes is shown in Figure 7.5.

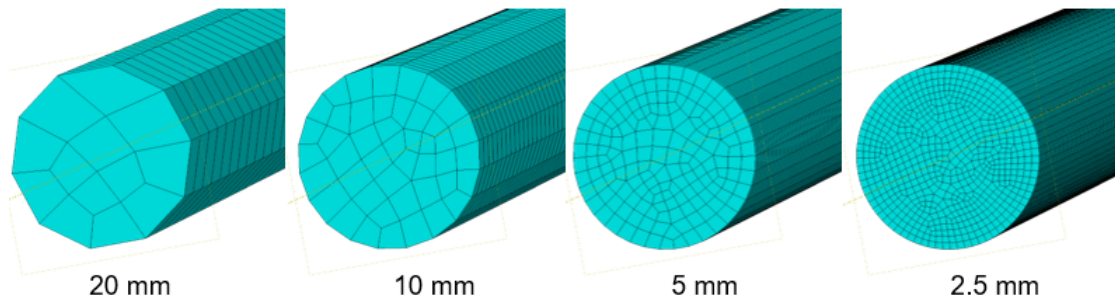


Figure 7.5: Variation of Eulerian mesh element sizes from coarse to fine.

To quantify the mesh sensitivity, the dependences of peak pressure and 10-90% rise time of the blast on mesh element size were evaluated.

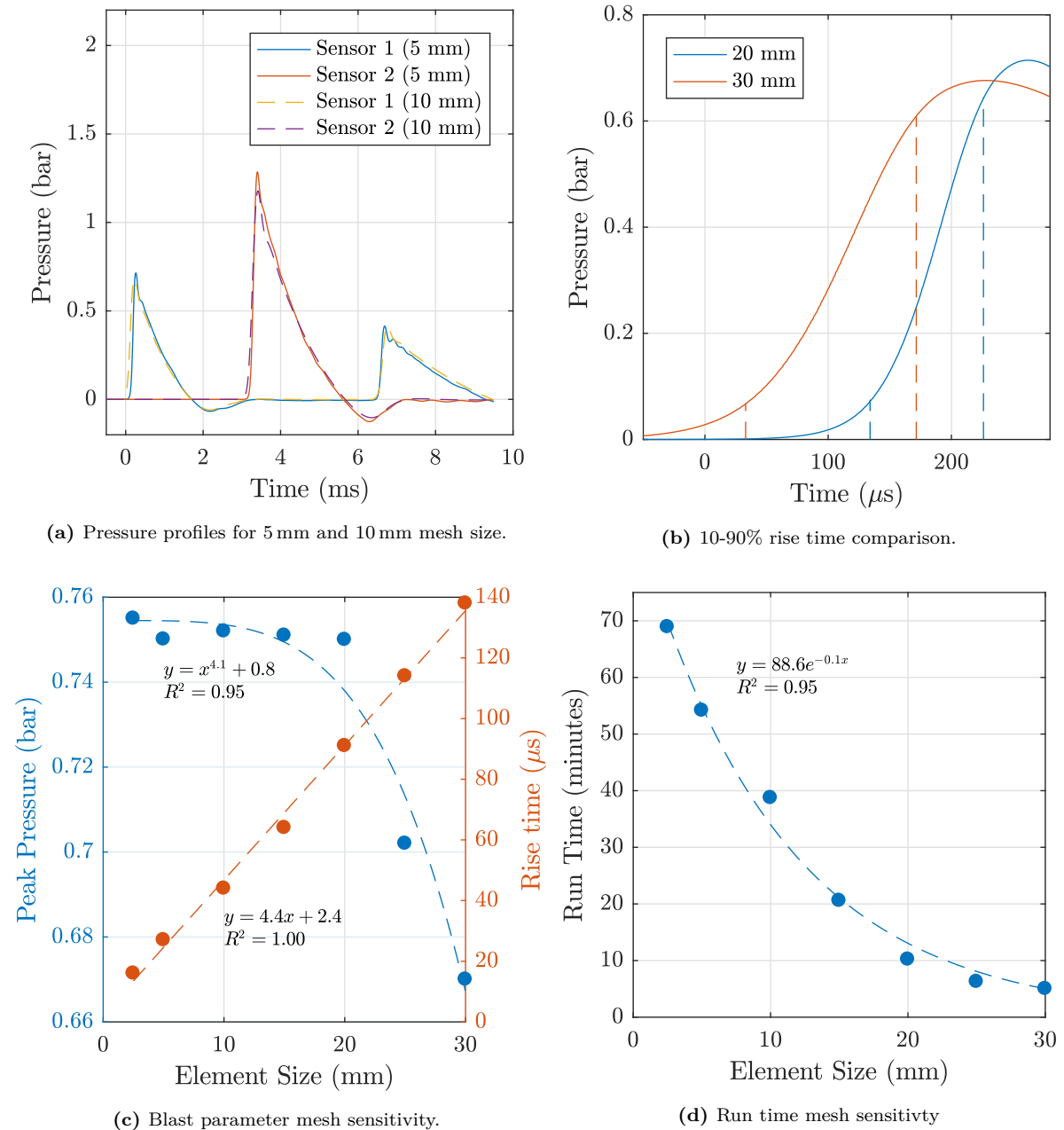


Figure 7.6: Eulerian mesh convergence sensitivity for pressure profiles, peak pressure, rise time and resulting simulation run time.

As can be seen, a doubling of the mesh size in Figure 7.6(a) did not significantly affect the gross pressure histories. However, increasing element size does lead to a slower rising blast as the pressure discontinuity is more spread in time and space. This rise time dependence on element size was linear. However, the blast peak pressure, a key parameter in this work and for blast casualty criteria, was found to obey a power law relationship, with the peak pressure dropping significantly after a critical element size of 20 mm. Smaller than 20 mm, the peak pressure was approximately mesh independent,

indicating convergence of the mesh. As such, substantially further refining the mesh did not present any significant advantage in reflecting the blast peak pressure, but did incur a penalty in the run time of the analysis. Indeed, the run time was found to exponentially increase with reducing mesh element size. As a result, it was considered that a 10 mm Eulerian mesh element size would present a valid simulation of the system with practical computational expense.

7.3 Blast Loading of Ballistic Gelatine

The coupling of the blast loading from the Eulerian fluid domain into a solid Lagrangian domain was modelled for the ballistic gelatine. The 20 mm length and 60 mm diameter gelatine sample was positioned at the end-wall of a closed shock tube configuration. The gelatine was modelled as a deformable, solid, homogeneous and isotropic material. A mesh element size of appropriately 2 mm was implemented, finer than the Eulerian domain to provide an adequately converged solution for the solid structure. The corresponding setup and boundary conditions for the simulation are outlined in the Figure 7.7.

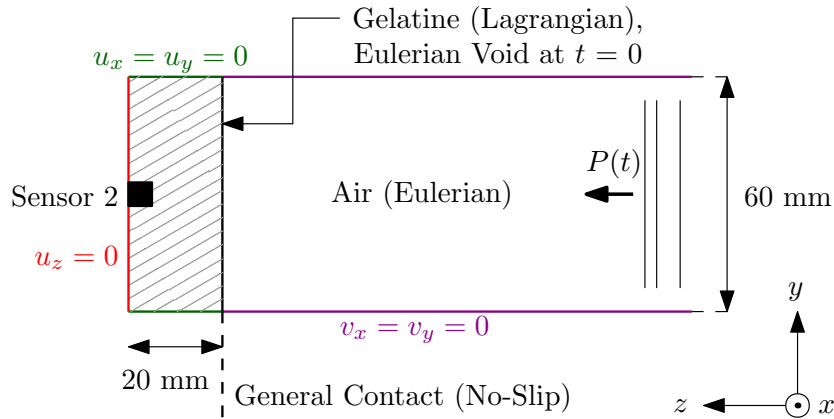


Figure 7.7: Schematic of the modelling setup for an Abaqus Coupled Eulerian Lagrangian (CEL) simulation of ballistic gelatine loaded by a shock tube in a closed configuration. A driver burst pressure of 7.2 bar was used (Mylar® 100 μ m diaphragm). As discussed in Section 7.2, zero transverse velocity, v , (magenta) boundary conditions were applied to the Eulerian region along the shock tube walls. For the Lagrangian domain, zero displacement (u) boundary conditions were defined to confine the gelatine laterally (green) and axially (red) to replicate the containment of the material in the shock tube.

To accommodate the solid domain, the reference part indicating the initial volume of Eulerian air was shortened by 20 mm. The nature of the coupling between the two

meshes was general contact with rough tangential behaviour to replicate a physical no-slip condition at the air-material interface. To match the experiment, a pressure probe was positioned centrally at the interface of the gelatine and end-wall with pressure values output every 10 solution time steps. Finally, it was necessary to define the gelatine material model. An Ogden hyperelastic model was used as this was found to robustly describe the material under uniaxial compression across a range of strain rates in Figure 6.11. The model corresponding to the drop-weight loading was used as the intermediate strain rate was an equivalent order of magnitude to the strain rates imposed by the shock tube loading. As such, the model was defined by coefficients from Table 6.6, corresponding to $\mu = 86.13 \text{ kPa}$ and $\alpha = 6.46$. It was noted that use of the higher values found for the SHPB due to the material's strain rate sensitivity did not significantly affect the numerical results discussed here. In addition, a gelatine density of 980 kg m^{-3} was used. The resulting comparison of the experimental and numerical results is shown in Figure 7.8.

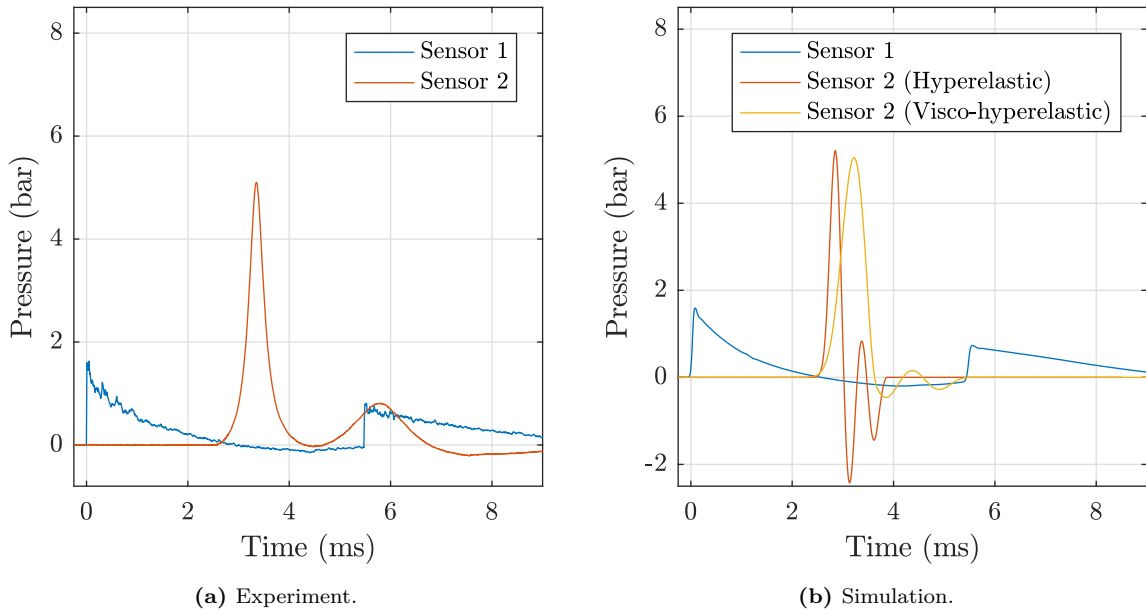


Figure 7.8: Comparison of measured and numerical pressure histories with gelatine sample in a closed shock tube. Sensor 1 measured the incident wave and Sensor 2 was positioned head-on behind the sample. In (b), both purely hyperelastic (Ogden) and visco-hyperelastic (Ogden and Prony) constitutive models are shown.

It was apparent that the simulated data replicated the prominent initial peak from the first wave transmission and the amplitude of this peak also closely matched the experimental data. It can be observed in the Sensor 2 head-on signal that there are sub-

sequent peaks, and these are more exaggerated in the numerical solution. From analysis of the stress field output for the material, it was apparent that these peaks were due to reflections within the material.

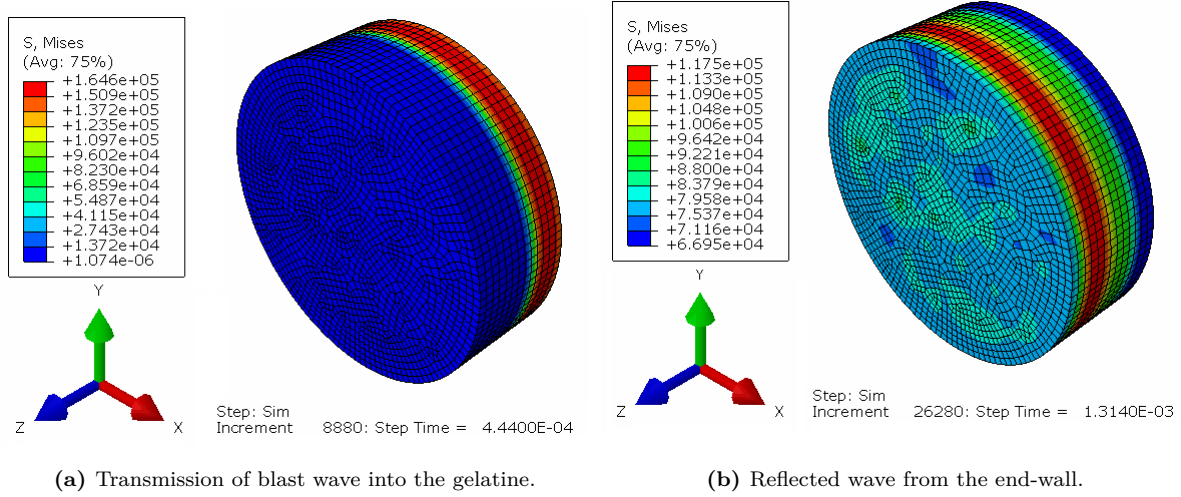


Figure 7.9: Gelatine stress wave propagation under shock tube blast loading. Scale bar and time units in Pa and seconds, respectively.

Though the hyperelastic Ogden model calibrated with the drop-weight experiments modelled the peak pressure accurately, the time dependence of the material was significantly different. It can be seen in Figure 7.8 that the wave reverberation in the gelatine occurred faster for the Ogden model with the reflected peak pressure being greater amplitude. This is a result of the fact that a purely hyperelastic treatment neglected the viscoelastic nature of the material, which (as was found in the mechanical characterisation) is more dominant at dynamic strain rates. For the purposes of this work, this will lead to significant errors in estimating the positive phase impulse. A summary of the experimental and numerical parameters is shown in Table 7.2.

Parameter	Experiment	Computational	
		Hyperelastic	Visco-hyperelastic
Peak Reflected Pressure	5.09 bar	5.21 bar	5.05 bar
Pressure Impulse	2.21 bar ms	1.51 bar ms	2.15 bar ms
Overpressure Duration	1.80 ms	0.59 ms	1.56 ms

Table 7.2: Comparison of experimental and computational pressure history parameters measured behind blast-loaded ballistic gelatine.

To address this, the Ogden description of the material's elastic component was com-

bined with a Prony series constitutive model to account for viscous damping and capture the rate-dependence nature of the gelatine. As such, the visco-hyperelastic constitutive model could be expressed as:

$$\sigma(\epsilon, t) = \sigma_0(\epsilon)g(t), \quad (7.2)$$

where $\sigma_0(\epsilon)$ was strain-dependent and described by the calibrated Ogden model. The term $g(t)$ is dimensionless and encodes the time-dependence of the material, given by the Prony series as:

$$g(t) = g_\infty + \sum_{i=1}^N g_i e^{-t/\tau_i}, \quad (7.3)$$

where g_i , g_∞ are dimensionless fitting parameters, and τ_i is a time constant determining the time-dependence. Note that $g_i = 0$ implies loss of the second term and rate-independence of the material. In accordance with dynamic ballistic gelatine material characterisation carried out by Zhou [206], it was found that a single-term $N = 1$ Prony series with $g_1 = 0.995$, $g_\infty = 0.005$ and $\tau_i = 0.1$ ms was adequate to model the material's time dependence. In Figure 7.8, it can be seen that the visco-hyperelastic model damps the secondary oscillations to a waveform closer to the experimental data. However, the behaviour of the material at later times still diverged from the data, an observation that has been noted by other investigators as ballistic gelatine modelling remains a highly active area of research [207]. Nonetheless, as can be seen in Table 7.2, the model more accurately represented overpressure duration and, therefore, impulse of the positive phase of the loading, a key metric for blast injury. As such, it was considered that the visco-hyperelastic model was an appropriate description for the thoracic surrogate considered in this research.

Chapter 8

Shock Enhancement Studies

Laboratory-scale blast mitigation studies were carried out using the CBIS shock tube for the polyurethane and hydrogel materials discussed in Chapter 4 and mechanically characterised in Chapter 6. There exists a current tension regarding the effectiveness of porous media for attenuating blast, with studies reporting both pressure mitigation [95, 96] and amplification [76, 87–94] under certain loading conditions. The aim of the present work was to additionally consider the effect of foam porosity in combination with sample length, as well as the effect of stand-off distance. The presence of an air gap between the armour and protected structure is considered to be an important ergonomic factor and may alter the role of gas dynamics in the loading. For personal applications, impact injury in such a case could contribute to blunt injury, commonly termed Behind Armour Blunt Trauma (BABT) [28, 116]. It is also instructive to consider not only peak pressure mitigation, but also impulse mitigation, as different blast injuries may be more sensitive to different blast parameters. For the brittle hydrogel materials, an additional unconfined shock tube setup was used to allow lateral expansion and momentum transport under loading.

8.1 Experimental Setups

Schematics of the shock tube setups for the confined and unconfined shock enhancement experiments are illustrated in Figure 8.1.

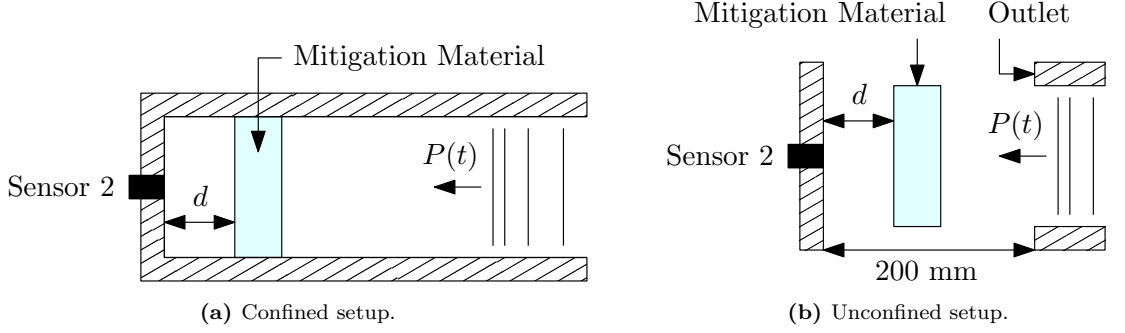


Figure 8.1: Confined (a) and unconfined (b) shock tube setups used for shock enhancement investigations. The stand-off between the mitigation material and rear boundary is denoted d . Sensor 1 (farther downstream as shown in Figure 5.1) measured the incident wave profile while Sensor 2 measured the reflected profile at the rigid end-wall. Sample diameters were equal to the shock tube diameter of 60 mm. This setup is analogous to that used by Seitz & Skews illustrated in Figure 2.12.

In single diaphragm configuration, a fixed loading duration utilising the shock tube blanking volume was used as this closely represents the Friedlander waveform and is applicable to the loading used by prior experimenters investigating the shock enhancement phenomenon. Four peak pressure amplitudes were generated utilising Mylar[®] 23 μm , 75 μm , 100 μm and 150 μm diaphragms. Using the Friedlander fitting analysis described in Figure 5.5, this corresponded to peak pressures of 0.9 bar, 2.1 bar, 3.0 bar and 3.8 bar, respectively. For variable sample length experiments, the sample length was varied from 20 mm to 140 mm in 20 mm increments. For variable stand-off distance experiments, d was varied from 0 mm to 30 mm in 10 mm increments. The maximal stand-off distance of 30 mm was used due to its relevance to armour applications (see Section 4.4). In the unconfined setup, the hydrogel samples were suspended at the required stand-off distance using Velcro[®] and is shown in Figure 8.14. This arrangement was chosen as it released from the sample immediately under loading, ensuring that the sample was minimally confined in the lateral directions.

8.1.1 Unmitigated Loading

To assess the mitigation effect of a given material, investigators have conventionally defined a mitigation metric as the ratio of a given parameter with the sample material to without the material [208]. These ratios were defined for peak pressure (τ_P) and pressure impulse (τ_I) as:

$$\tau_P = \frac{P_0}{P_s}, \quad (8.1)$$

$$\tau_I = \frac{I_0}{I_s}, \quad (8.2)$$

where P_0 and I_0 are the peak pressure and impulse, respectively, measured behind the material at the end-wall by Sensor 2, and P_s and I_s are the unmitigated peak pressure and impulse. The impulses were computed over the positive phase of the wave according to Eq. (2.15) using a code written in MATLAB. Mitigation ratios less than unity implied mitigation, while greater implied amplification of the parameter. Representative unmitigated baseline pressure profiles are shown in Figure 8.2.

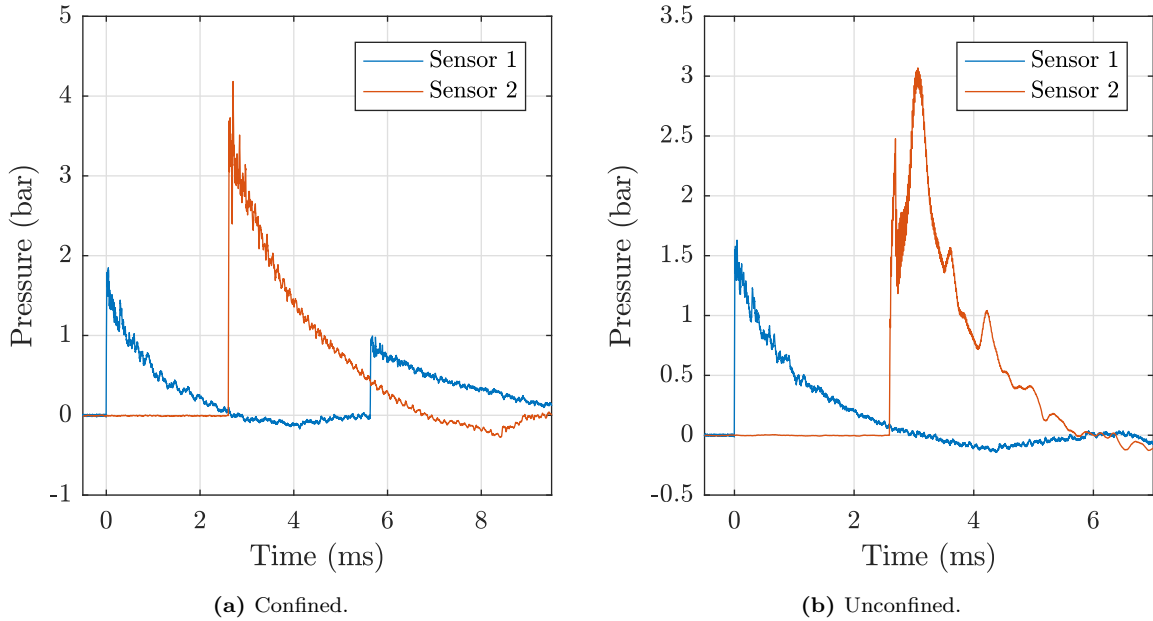


Figure 8.2: Unmitigated baseline pressure profiles for the (a) confined and (b) unconfined setups (Mylar[®] 75 μm diaphragm).

It can be seen in Figure 8.2(a) that the signal measured by Sensor 2 was double that of the incident wave. This was a result of the large impedance mismatch from

air to the stainless steel end-wall. From impedance considerations, it can be seen from Eq. (2.16) that in the limit that the second material has much greater impedance than the incident medium, the measured pressure is double that of the incident wave. For the unconfined setup, there is a reduction of the Sensor 2 signal relative to the confined shock tube due to attenuation of the blast peak overpressure in air. There are also more oscillations visible in the signal due to vibrations of the rig. For the confined and unconfined configurations in this example, the unmitigated peak pressures were $P_s = 3.6 \pm 0.2$ bar and $P_s = 3.0 \pm 0.2$ bar, respectively. For the positive phase pressure impulse, they were $I_s = 4.8 \pm 0.3$ bar ms and $I_s = 3.2 \pm 0.3$ bar ms, respectively.

8.2 Polyurethane Foams

8.2.1 Reticulated Foam Porosity

The effect of foam porosity on blast mitigation has been previously investigated for fully clamped boundary conditions with an unsupported rear surface [152, 209]. In this case, it was found that reticulated polymeric foams with porosities in the range of 65-79 ppi resulted in peak pressure attenuations of approximately 10% [152]. For the case of a rear-supported foam appropriate for the application of the material to a protected structure, the foam's dynamic compression becomes critical and results in the opposite effect. Using the Figure 8.2(a) confined shock tube setup, the pressure profiles and mitigation ratios for the reticulated foams are shown in Figure 8.3.

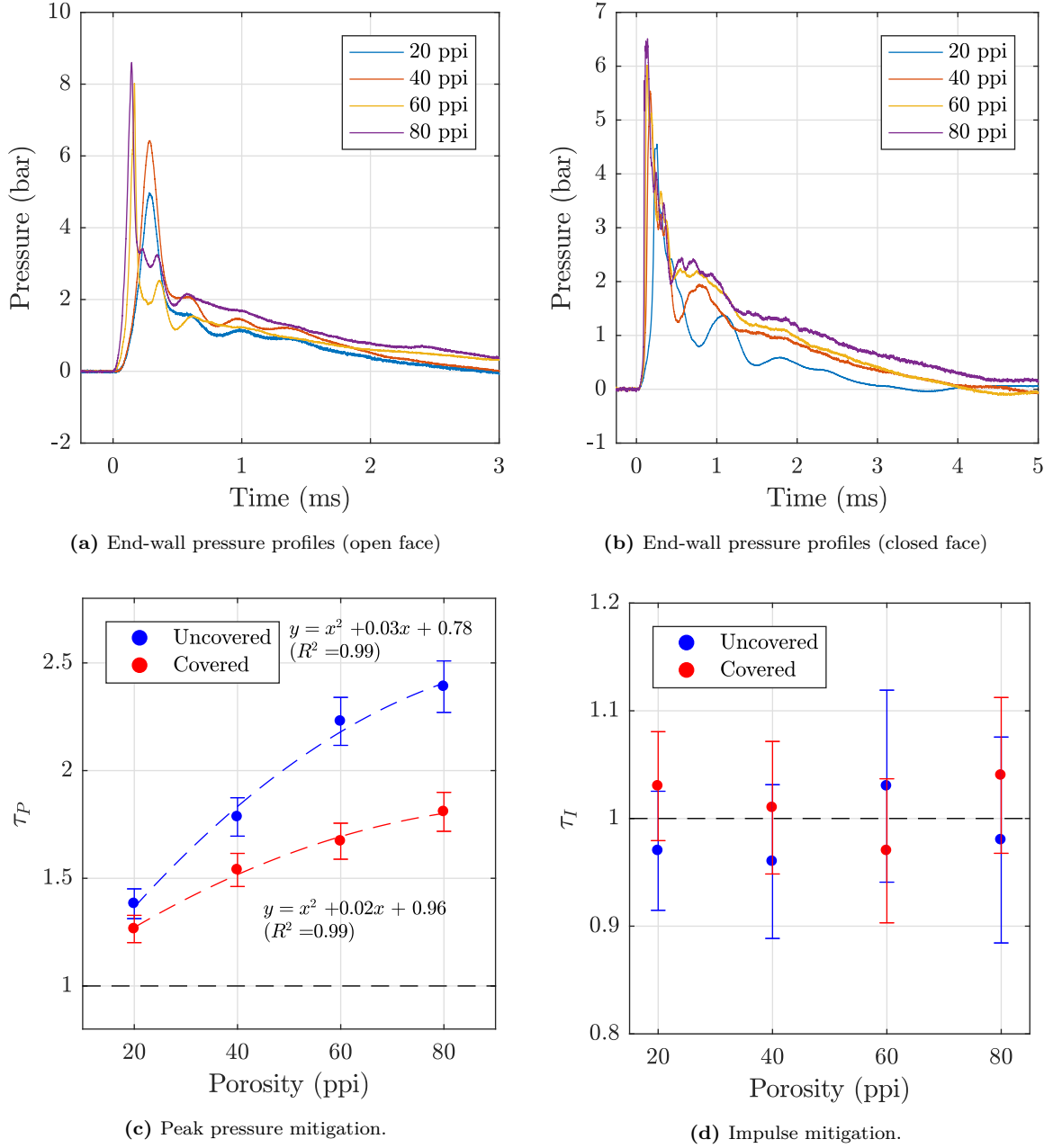


Figure 8.3: The effect of reticulated foam porosity on peak pressure and impulse mitigation for both covered (a) and uncovered (b) blastward faces when loaded using a Mylar[®] 75 μm diaphragm ($P_s = 3.6 \pm 0.2$ bar, $I_s = 3.2 \pm 0.3$ bar ms). The foam sample length is 20 mm and there is zero stand-off between the sample and closed shock tube end wall. The pressure profiles show the reflected pressure behind the samples measured by Sensor 2. Second-order polynomials were fitted by least-squares regression to the peak pressure attenuation plot (c). The uncertainties represent the standard error of the mean over 3 repeat independent measurements. ppi: Pores per inch, P_s : Blast peak incident overpressure pressure, I_s : Blast positive phase impulse, τ_P : Peak pressure mitigation ratio, τ_I : Impulse mitigation ratio.

From the Figure 8.3(a) pressure profiles, it can be seen that the peak pressures exceed the unmitigated peak pressure, representing amplification of the blast amplitude. However, the loading duration and impulse were found to be unaltered from the unmitigated profiles within error bounds. The reticulated foam deformation was elastic with no

evidence of permanent deformation. The unchanged impulse reflects the fact that negligible energy was dissipated through the deformation of the structure. This was shown by Nguyen [44], where SEM microscopy showed that there was no damage of the foam skeleton. From the dynamic uniaxial compression characterisation of the reticulated foams, the pressure impulse required to compress the foams to densification can be estimated as the product of the energy absorbed per unit volume to the total compression time (Δt) *i.e.* $W\Delta t$. Across both drop-weight and SHPB characterisations, this value for the reticulated foams is of the order of 0.01 bar ms, negligible compared to the delivered blast incident impulse of 3.2 bar ms. The rationale for the increased peak pressure is likely a combination of the fact that the higher impedance of the material compared to air results in greater stress transmission to the back wall [98, 99], and also the fact that the accelerated foam requires a greater reactive impulse from the back wall to bring the foam to rest [88, 94, 100]. From consideration of the Figure 2.13, it is understood that the foam compression takes place in four phases. Seitz & Skews identify this as initial reflection of the incident shock and acceleration of the foam face, a pseudo-steady state foam collapse, stagnation as the foam material is brought to rest at the end-wall and, finally, longitudinal expansion of the foam as it recovers [87]. It can be seen in the pressure profiles that each profile features an initial peak followed by subsequent secondary peaks. This is attributed to the final recovery phase. The drop of pressure following the initial peak corresponds to release as the foam expands again longitudinally into the shock tube. This pressure relief results in the foam internal pressure dropping below that of the external pressure acting on the foam front face, resulting in re-compression of the foam and secondary undamped oscillations. The magnitude of these secondary oscillations did not depend on the foam porosity.

Considering the influence of the foam porosity in greater detail, it can be seen that the shock amplification effect was stronger with increasing foam ppi. While the mechanical behaviour of these reticulated foams was determined by their relative density, the divergence of their behaviour under blast loading suggests that gas dynamics are important. In the first phase where the foam is compressed by the incident shock wave, it has been

shown by Skews that there is partial penetration and reflection of the shock front [103]. This depends on the open area presented to the incident shock, and therefore the porosity of the foam. Resulting wavelets reflected from the solid fraction of the foam's front face interfere to form a macroscopically complex structure, as illustrated in the Figure 8.4 shadowography.

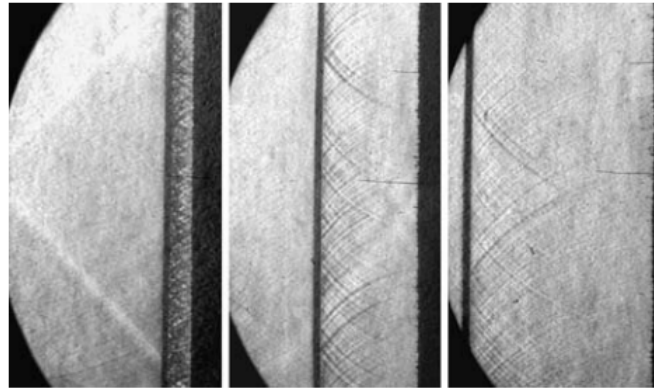


Figure 8.4: Schlieren shadowography of the reflected shock wave from a reticulated foam under shock tube loading at $100\text{ }\mu\text{s}$, $200\text{ }\mu\text{s}$ and $300\text{ }\mu\text{s}$ (from left to right) after reflection from the front face of the foam. The foam is visible as the dark region on the right side of each frame. Reproduced from [87].

Given that the impedance and mass of the different foam samples were approximately equivalent, the significant increase of amplification (83% from 20 to 80 ppi) with porosity suggests that this phenomenon may be a consequence of the complex gas dynamics. As higher porosity foams allow greater air content, it is possible that the effect is due to the greater percolation of the blast wind into the foam and the greater pressure increase caused by collapse of smaller pores.

To further investigate this, the front face of the foams were covered with an impermeable and negligible mass layer (Mylar[®] $23\text{ }\mu\text{m}$) to exclude infiltration of the blast wind into the foams. It can be seen in Figure 8.3(c) that this had the effect of globally reducing the amplification effect across all porosities, as well as the dependence of the amplification on porosity. The cover acted to separate the effects of the blast wind percolation and the compression of the initial static air volume in the foam. It remained that the highest porosity foam still exhibited the greatest shock enhancement. This may be an example of the ‘piston effect’ proposed by Skews [104]. Here, the effect of the imperme-

able cover combined with the initial compression of the front face results in an advancing solid surface that compresses the remaining air entraps between the ‘piston’ and end-wall, amplifying the pressure.

8.2.2 Reticulated Foam Material Length

The effect on the end-wall stagnation pressure of the initial length of reticulated foam is shown in Figure 8.5.

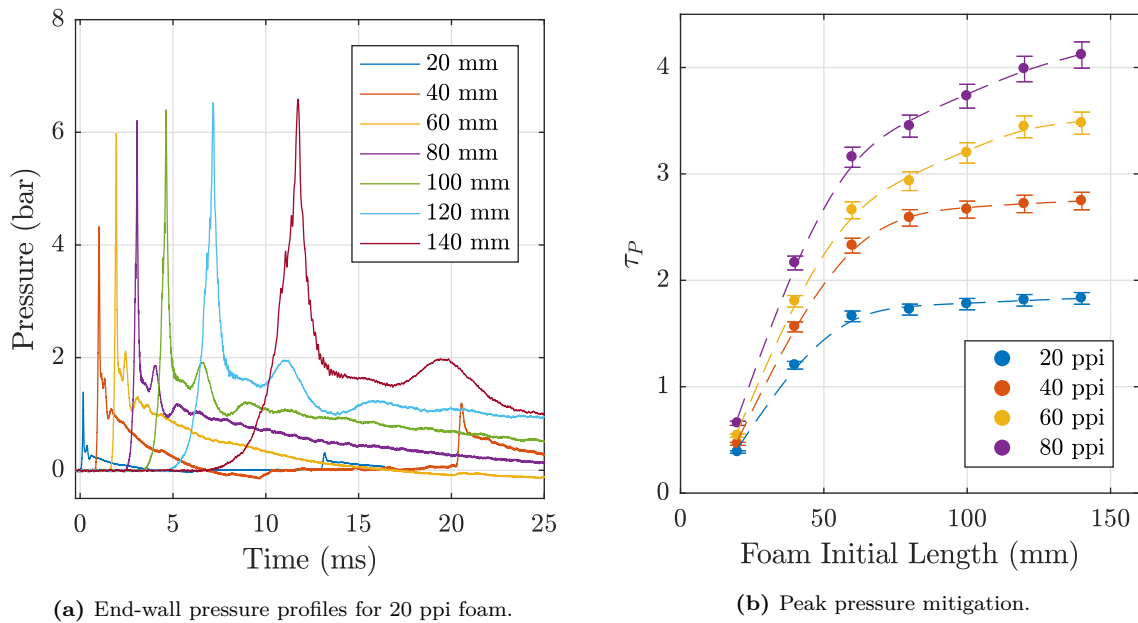


Figure 8.5: The effect of initial reticulated foam length on end-wall pressure amplification for foam porosities in the range of 20-80 pores per inch (ppi). The materials were loaded using a Mylar[®] 75 μm diaphragm (peak incident overpressure $P_s = 3.6 \pm 0.2$ bar). In (b), smoothing spline curves were fitted. It can be observed that the peak pressure mitigation ratios approached a plateau, becoming less dependent on the initial foam length. The rate at which the materials approached this mitigation independence from the initial foam length decreased with increasing foam ppi. For example, the 20 ppi foam peak pressure mitigation plateaued above an initial foam length of 80 mm within errors. Conversely, the 80 ppi foam did not reach this plateau over the investigated range, though a decreasing dependence on the initial foam length can be observed. This behaviour can be related to the stress plateau characteristic of foams under compression. The uncertainties represent the standard error of the mean over 3 repeat independent measurements.

It was observed that the peak pressure enhancement increased with foam length up to an asymptotic value of pressure beyond which further increases of foam length did not incur further amplification. There was again no measured change of impulse at the end-wall. This finding agrees with those of Gvozdeva *et al.* [100]. This can be seen in the pressure profiles of Figure 8.5(a) where the increase of peak pressure is initially rapid

but tends towards a certain value. With increasing foam length, it can be seen that the time at which the signal rises is delayed linearly with length as the shock front reached the longer samples first. The duration of the loading was also proportional to initial foam length, with the greater length of material decoupling the shock front into an increasingly ramped compression wave.

In Figure 8.5, the dependence of this behaviour on the foam porosity is shown. Again, it can be seen that the amplification effect is greater for increased porosity. The critical foam length also increased with the porosity. Indeed, it can be seen that the 60 ppi and 80 ppi foams for the maximal investigated initial foam length of 140 mm had not yet reached plateau values. Since higher ppi corresponds to lower relative density and mass, it is possible that this can be attributed to such foams being easier to accelerate, reaching a greater material velocity and resulting in a higher end-wall stagnation pressure. A physical rationale for this plateau behaviour was proposed by Mazor [210]. The critical foam length represents the length at which the compression waves can coalesce into a shock front. Therefore, for lengths less than the critical length, the end-wall experiences a fan of compression waves that increase in pressure. For lengths greater than the critical length, a shock wave is formed and the end-wall is then impacted by the same amplitude transmitted wave. An alternative explanation is that is that with increasing foam length, densification of the foam is achieved increasingly later. The critical foam length could then be interpreted as the length beyond which the foam is not strained sufficiently to densify and remains at its foam plateau stress. Considering the mechanical characterisation of the foams, it was found that each foam porosity exhibited a near-equivalent plateau stress under dynamic loading of approximately 1–3 kPa (see Section 6.3). This is significantly lower than the plateau end-wall stagnation pressures measured in this experiment. This is partly due to the fact that the shock tube loading is different in nature to the drop-weight and SHPB (Friedlander waveform compared to triangular and square wave inputs). However, the increased stagnation pressure may also be attributed to the contribution of gas dynamics as the air phase of the material is compressed (see Section 8.2.1 discussion).

8.2.3 Dependence on Incident Pressure

The incident pressure dependence of porous shock enhancement was investigated for both the open-cell and syntactic polyurethane samples for 20 mm sample length.

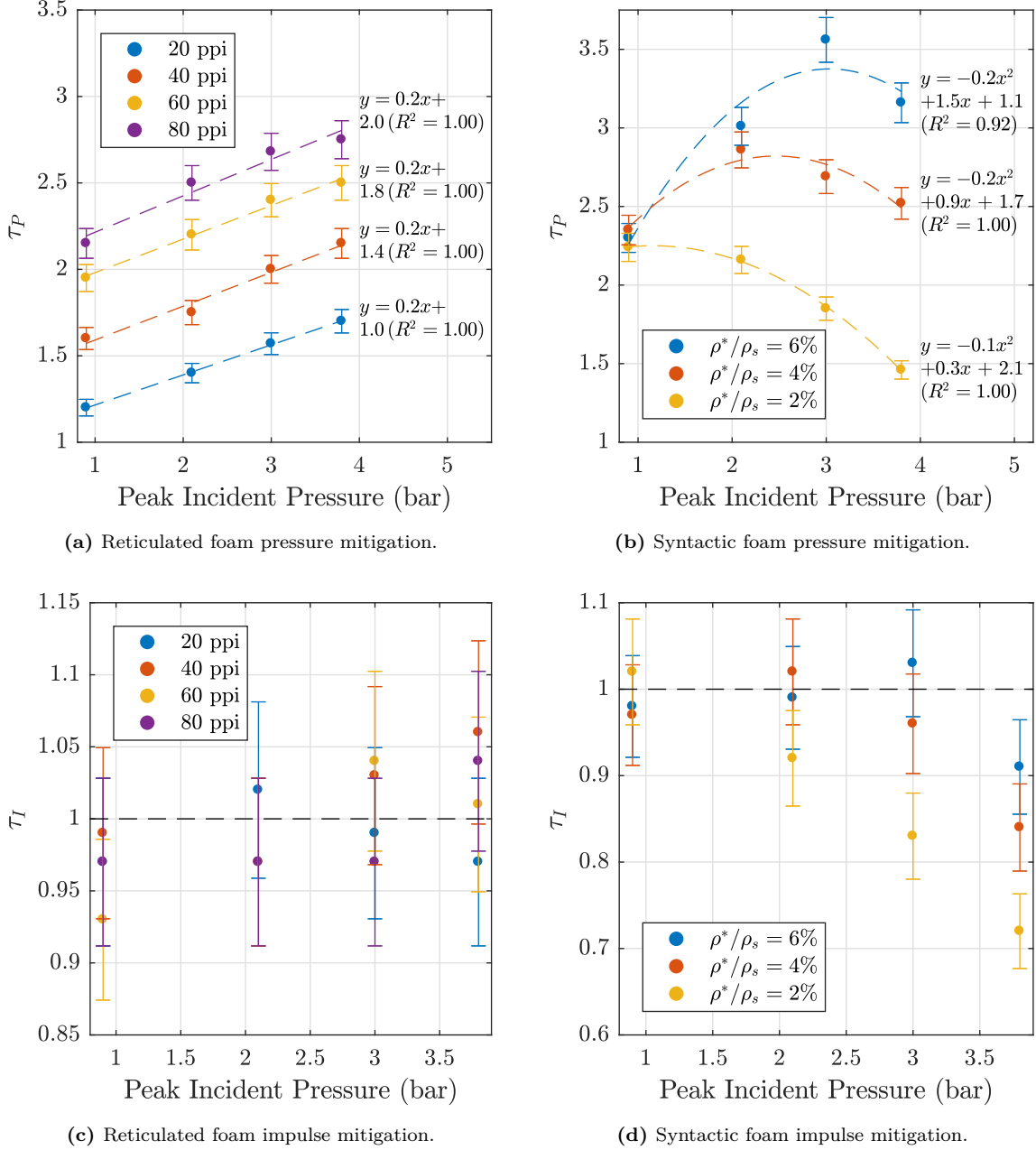


Figure 8.6: The effect of incident loading pressure amplitude on peak pressure (τ_P) and impulse (τ_I) mitigation ratios for reticulated and syntactic polyurethane foam materials. These mitigation ratios express the ratio of the parameter (peak pressure or impulse) with the mitigation material to without the mitigation material. The uncertainties represent the standard error of the mean over 3 repeat independent measurements. ppi: Pores per inch, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.

For the reticulated foams, an increase of shock pressure was associated with a linearly

increasing end-wall reflected pressure and, thus, pressure mitigation ratio. As before, higher porosity reticulated foams resulted in greater peak pressure amplification. For each porosity, the rate of amplification increase with incident peak pressure was equivalent within uncertainties, visible from the equal slopes of the linear fits in Figure 8.6(a). The increase of shock pressure was also associated with a decrease in the duration of loading. This is due to the compression and expansion fans of the incident wave moving at higher velocity, imparting a greater front-face foam velocity and reducing the transit time for reflection from the end-wall. Indeed, several investigators have measured a linear relationship between the incident wave Mach number and the velocity of the front surface of the foam [87, 97, 100].

For the syntactic foams, it was considered that due to the closed-cell impermeable nature of the material, there would be no interaction of the blast wind with the internal structure of the material. The pressure amplification effect was identical at the lowest incident peak pressure, but diverged depending on the porosity of the syntactic foam. In their elastic regime of response, an increase of incident pressure simply corresponded to an increase of the end-wall pressure due to the amplification effect of the additional air-foam interface from impedance considerations. However, beyond the elastic limit of the materials, pore collapse was (at least partially) initiated, resulting in a reduction of the peak pressure and impulse. It can be seen that the incident peak pressure required to access pore collapse of the material was lower with increasing syntactic foam porosity. Indeed, pore collapse occurred more readily in the materials containing a higher proportion of microballoons. From their dynamic mechanical characterisation, the syntactic foams exhibited plateau stresses of the order of 10 bar (see Figure 6.3), significantly greater than the pressures the foams were exposed to under shock tube loading. However, the blast loading was significantly longer in duration, potentially providing a rationale for the occurrence of pore collapse using the shock tube. It should be noted that while the syntactic foams exhibited a relative reduction of the peak pressure amplification beyond the peak pressure required to initiate plastic deformation, this effect remained lesser than the enhancement due to the impedance coupling into the end-wall with all foams remaining

in the enhancement regime ($\tau_P > 1$) over all incident pressures. However, it can be seen that the downward trend of the peak pressure occurred concurrently with a reduction of the end-wall impulse and mitigation being achieved for all three foam porosities at the highest incident peak pressure of 3.8 bar. This energy reduction was associated with the energy loss due to deformation of the foams and fracture of the microballoons, with the recovered foam samples exhibiting a permanent reduction of sample length.

8.2.4 Dependence on Stand-off Distance

With stand-off distance from the end-wall, the mitigation materials were accelerated before impacting the rear surface. For the reticulated foams, this resulted in an additional elastic inertia peak relative to the equivalent loading with zero stand-off distance. This is illustrated in Figure 8.7.

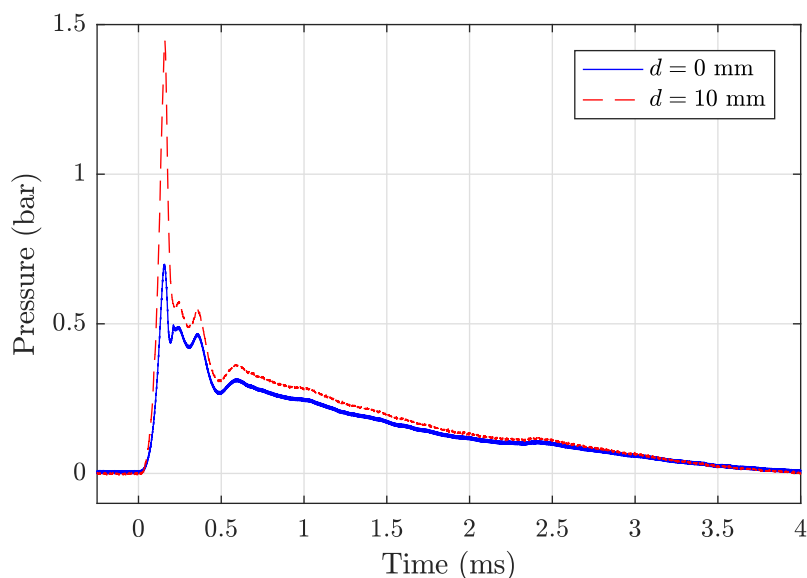


Figure 8.7: Comparison of end-wall pressure histories for 20 pores per inch (ppi) reticulated foam positioned at stand-off distance d from the shock tube end-wall, demonstrating the contribution of elastic inertia. The samples were loaded using a Mylar[®] 75 μm diaphragm (peak incident overpressure $P_s = 3.6 \pm 0.2$ bar). For visual comparison, the starts of the signals have been shifted to the same time.

The presence of the inertia peak agrees with analogous measurements carried out by Kitagawa *et al.* for reticulated foams [106]. This also implies that in the conventional case of the material being against the end-wall, the sample elastic inertia is negligible. Due to the narrow width of this additional inertia feature, the impulse mitigation for the retic-

ulated foams remained zero within uncertainties for all stand-off distances. The relative contribution of the inertia peak was approximately equivalent across all foam porosities in the range of 20-80 ppi. This implies that any pressure increase from acceleration of the foam mass against the end-wall was negligible.

Conversely, increased peak reflected pressure at the end-wall with stand-off distance may have been expected for the syntactic foams due to their greater density and mass. However, this effect was dominated by enhanced energy loss to pore collapse of the syntactic foams. This effect is shown in Figure 8.8.

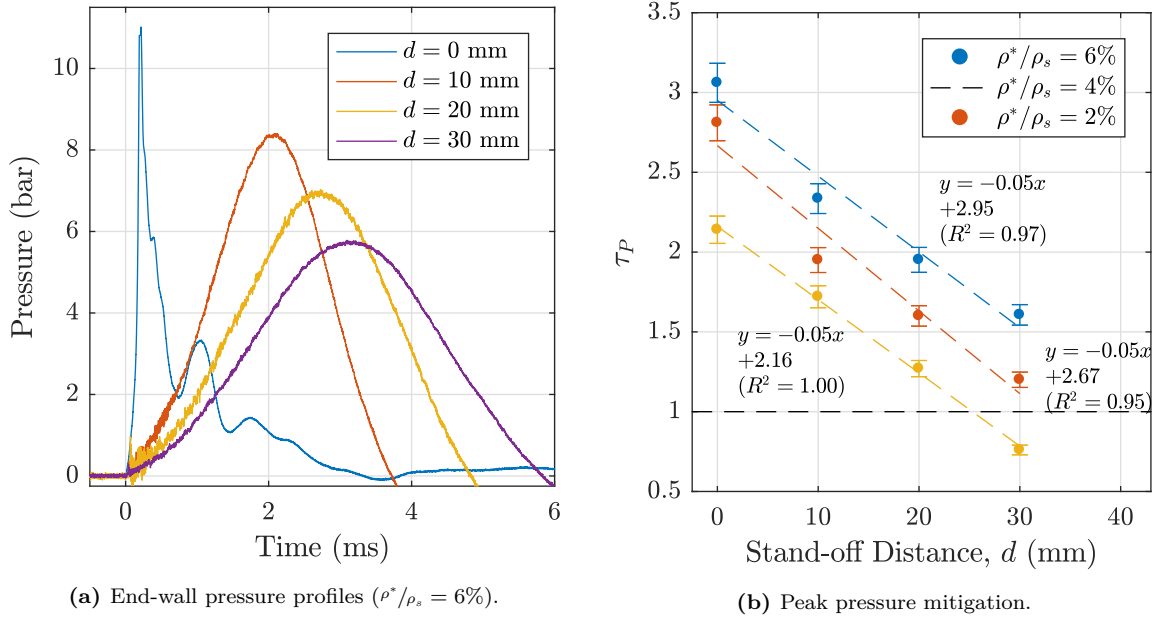


Figure 8.8: The effect of stand-off distance, d , from the shock tube end-wall for the polyurethane syntactic foams. The pressure profiles in (a) and the peak pressure mitigation in (b) are shown for the example of a Mylar[®] 75 μm diaphragm (peak incident overpressure $P_s = 3.6 \pm 0.2$ bar). The uncertainties represent the standard error of the mean over 3 repeat independent measurements. For visual comparison, the beginnings of the signals have been shifted to the same time. τ_P : Peak pressure mitigation ratio, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.

It can be seen that with increasing stand-off distance, the peak reflected pressure reduced due to energy losses through the thickness of the syntactic foams. This relationship was linear for each foam porosity. As before, the highest porosity foam exhibited the greatest peak pressure attenuation, with the most porous sample exhibiting mitigation at $d = 30$ mm in Figure 8.8(b). Unlike elastomeric reticulated foams where the effect of stand-off distance was to introduce a high-amplitude inertia peak, the plastically deform-

ing syntactic foams exhibit enhanced attenuation relative to the zero stand-off distance case shown in Figure 8.6(b).

8.3 Hydrogels

8.3.1 Dependence on Incident Pressure

End-wall pressure histories for the homogeneous and two-phase hydrogel materials are shown in Figure 8.9 for the unconfined shock tube setup.

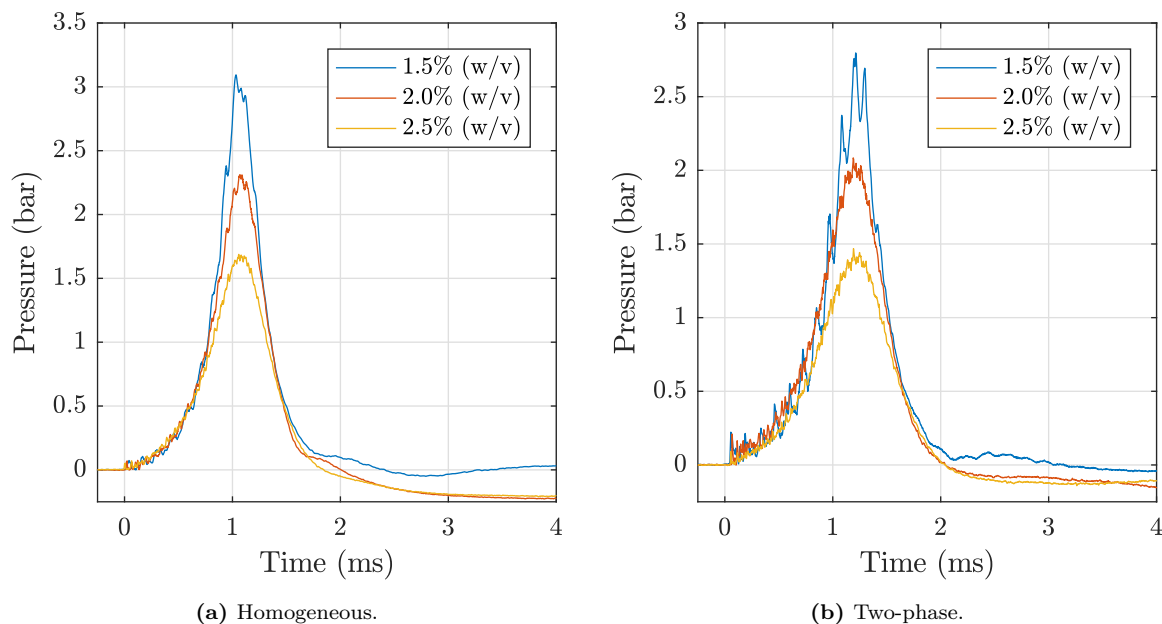


Figure 8.9: Homogeneous (a) and two-phase (b) hydrogel end-wall pressure profiles for the confined shock tube setup. The materials were loaded using a Mylar[®] 75 μm diaphragm (peak incident overpressure $P_s = 3.6 \pm 0.2$ bar).

Enhanced blast wave peak pressure attenuation was observed with increasing agarose concentration. This dependence was found to follow a linear relationship. Notably, the two-phase hydrogels exhibited greater peak pressure and impulse attenuation relative to their homogeneous counterparts. The peak pressure and impulse mitigation ratios are shown in Figure 8.10. The increased energy dissipation through the two-phase materials was considered to be a result of greater material fracture and decomposition. While the homogeneous hydrogels were recoverable and are shown in Figure 8.14, the damage and

fragmentation sustained by the two-phase samples rendered their recovery impractical. From the dynamic characterisation of these materials (see Figure 6.7), the two-phase hydrogels demonstrated significantly lower failure stresses. In the pressure profiles of Figure 8.9, the greater presence of brittle fracture is visible in the oscillatory features present in the two-phase pressure histories, due to short time-scale pressure relief from microscopic and macroscopic material failure. Physically, the presence of lower density and modulus 1% (w/v) agarose beads in the higher stiffness matrix generates additional local damage mechanisms through the material of tension and shear where the pressure wave encounters changes of impedance at the matrix-inclusion boundaries.

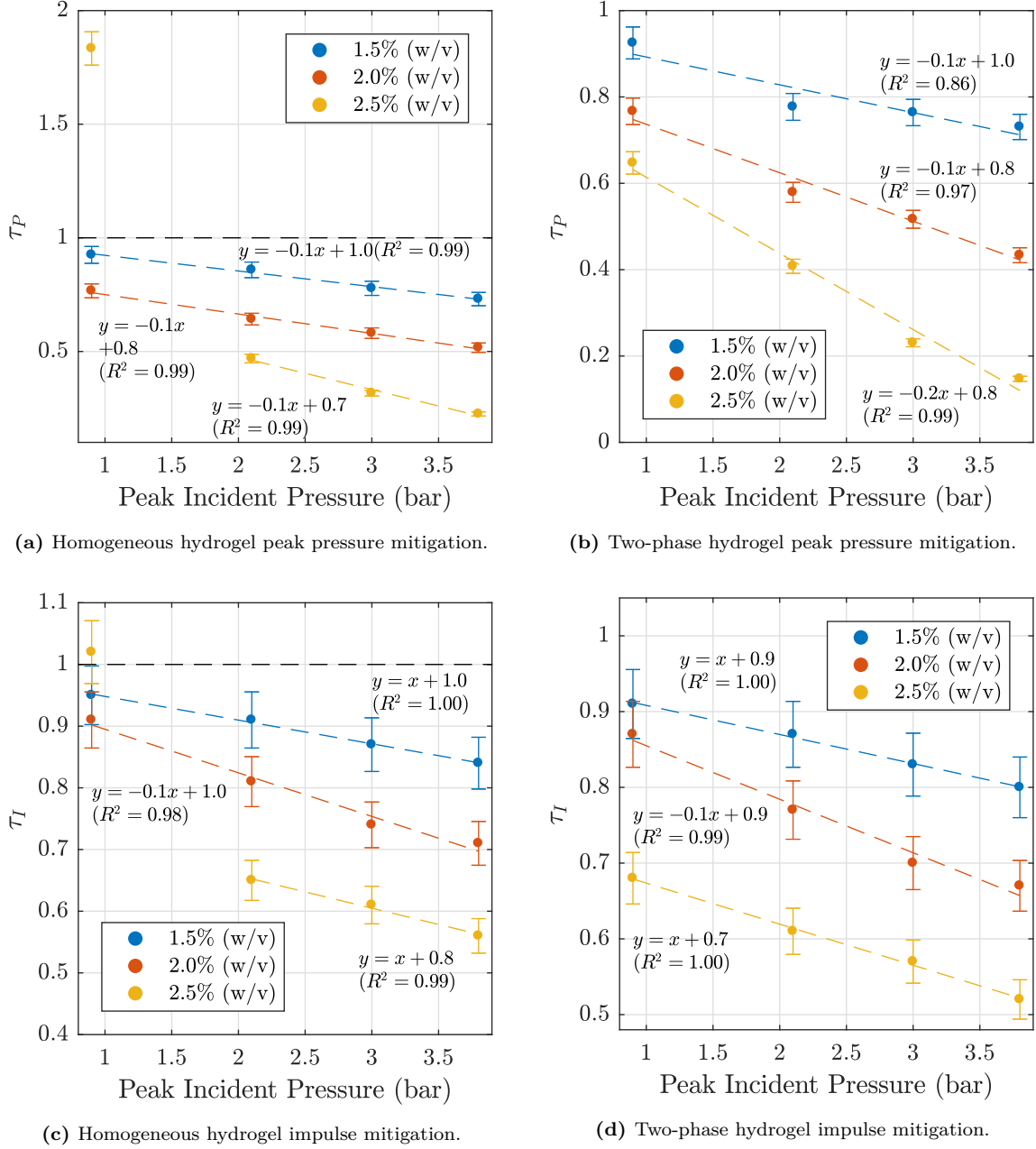


Figure 8.10: The effect of incident loading pressure amplitude on peak pressure (τ_P) and impulse (τ_I) mitigation ratios for homogeneous and two-phase hydrogels. The uncertainties represent the standard error of the mean over 3 repeat independent measurements. The dependence of the mitigation ratios on incident peak pressure was well-described by linear fits where the samples underwent fracture. The exception to this was the case of the 2.5% (w/v) homogeneous hydrogel which remained in its elastic regime and did not fragment. Visual evidence of this is shown in Figure 8.14. As such, this data point was excluded from the linear fits as the deformation mechanisms were fundamentally different to those under higher amplitude blast loading where the material failed.

Increase of peak incident pressure was found to be proportional to enhanced material damage and mitigation of pressure and impulse. It was also found that the mitigation effect increased with the hydrogel stiffness, showing that small changes of agarose concentration result in statistically significant changes of energy absorption. To relate this

to the mechanical characterisation of the hydrogels, the highest loading pressures used under shock tube loading resulted in extensive material failure and were found to be related to the relative differences of brittle failure stress. For the homogeneous hydrogels, it was found that the 2.0% (w/v) and 2.5% (w/v) hydrogels exhibited energy absorbed per unit volume (area under the stress-strain curve) values 15% and 36% greater than that of the softest 1.5% (w/v) hydrogel. These values compare with these hydrogels having $32 \pm 7\%$ and $16 \pm 6\%$ greater pressure impulse mitigation than the 1.5% (w/v) hydrogel. This proportionality of mitigation to hydrogel concentration was the case for all loading scenarios where the loading pressure exceeded the elastic limit of the material. The exception to this was the 2.5% (w/v) homogeneous hydrogel at the lowest incident pressure of 0.9 bar in Figure 8.10(a). Recovery of the material showed that it did not sustain damage, resulting in an enhancement from the impedance coupling at the additional air-hydrogel interface. To illustrate this, a comparison of the recovered homogeneous 2.5% (w/v) hydrogels following the shock tube loading is shown in Figure 8.14

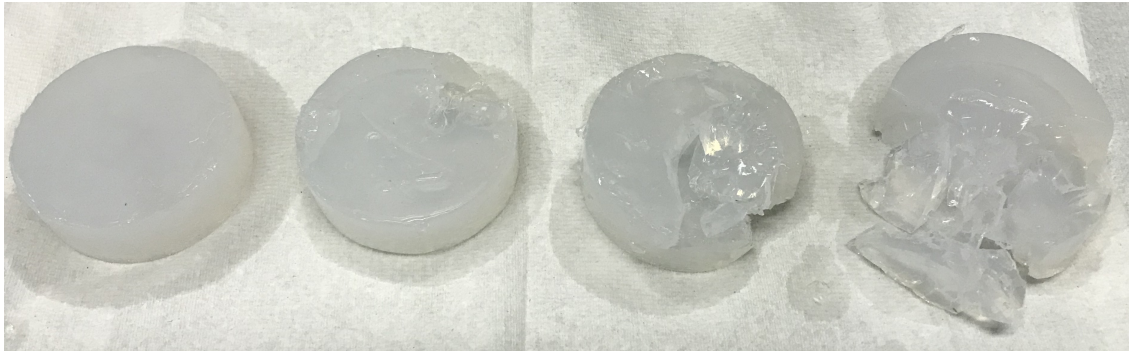


Figure 8.11: Recovered 2.5% (w/v) homogeneous hydrogel following shock tube loading at varying incident peak pressure. From left to right, the incident peak pressures were 0.9 bar, 2.1 bar, 3.0 bar and 3.8 bar. The leftmost sample was undamaged and remained in its elastic regime, resulting in shock enhancement.

By contrast, the ability of the inclusion of agarose beads to enhance material fracture under dynamic loading was apparent, with all two-phase hydrogels occupying the mitigation regime ($\tau_p, \tau_I < 1$) and exhibiting greater damage than their homogeneous counterparts. As a result, it was considered that the two-phase hydrogels may present greater potential for blast mitigation applications.

To further investigate the two-phase materials, unconfined boundary conditions were

considered with the open shock tube setup of Figure 8.1(b). It was considered that the removal of the circumferential or hoop stress would result in a lower brittle failure of stress of the material, increasing energy dissipation. This would also enable the blast energy to be redirected from the axial loading axis to the perpendicular directions as the material would be permitted to flow laterally. This is illustrated in Figure 8.12

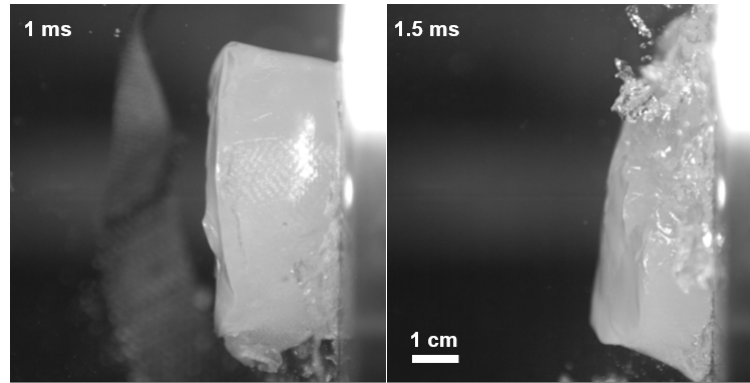


Figure 8.12: Unconfined shock tube loading of 2.0% (w/v) hydrogel under 1.8 bar incident peak pressure loading. The shock tube end-wall is visible on the right edge. The dark material (left) is the Velcro[®] releasing from the sample. Lateral material loss is permitted in the setup compared to the conventional confined shock tube shock enhancement configuration

The effect of this change of boundary conditions on the blast mitigation performance of the two-phase hydrogels is shown in Figure 8.13.

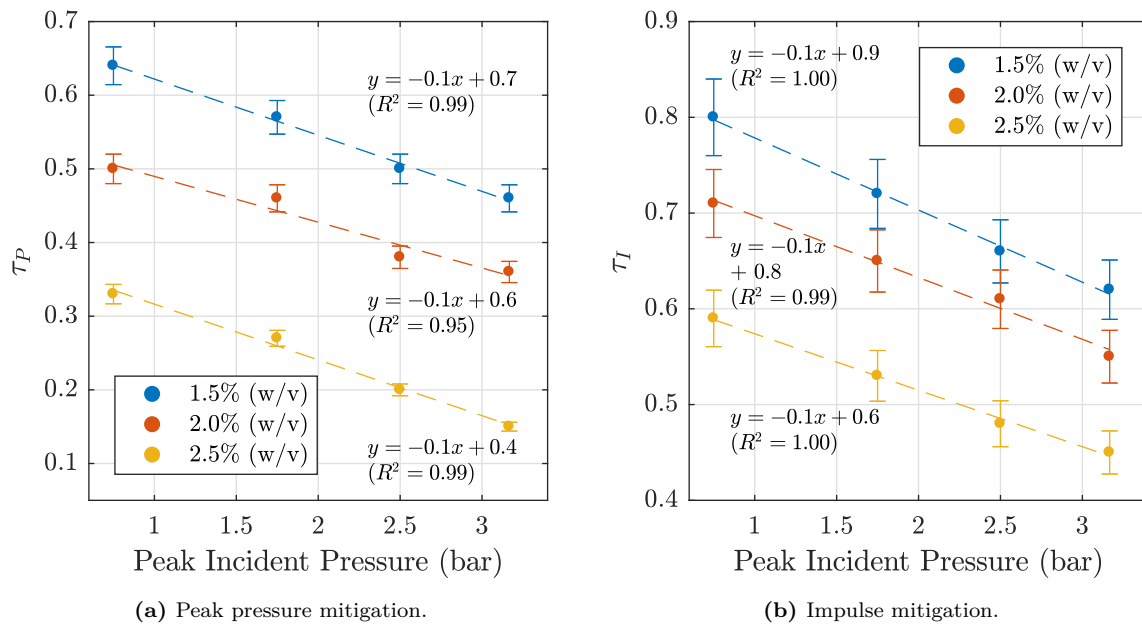


Figure 8.13: The effect of incident loading pressure amplitude on peak pressure and impulse mitigation ratios for two-phase hydrogels under unconfined loading conditions. The uncertainties represent the standard error of the mean over 3 repeat independent measurements. τ_P : Peak pressure mitigation ratio, τ_I : Impulse mitigation ratio.

Across all agarose concentrations, the unconfined blast mitigation performance was an average of $15 \pm 4\%$ and $9 \pm 3\%$ greater for the unconfined setup compared to the confined shock tube for peak pressure and impulse, respectively. This difference can be attributed to the additional mitigation mechanisms of lateral mass loss and redirection of the material away from the loading axis due to lack of circumferential support.

8.3.2 Dependence on Stand-off Distance

The combination of unconfined boundary conditions and stand-off distance from the rigid rear boundary were investigated for two-phase hydrogels. Representative high-speed photography for a 2.5% (w/v) sample is shown in Figure 8.14.

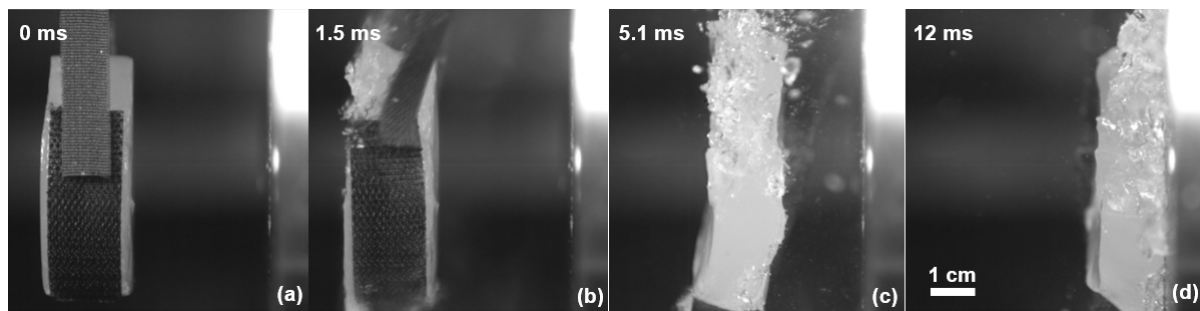


Figure 8.14: Unconfined impact of 2.5% (w/v) two-phase hydrogel against rear end-wall at an initial stand-off distance of 30 mm, loaded by a 2.1 bar peak pressure blast wave. The blast wave propagates from left to right with the (a) unloaded sample, (b) sample contact with the blast wave and Velcro[®] release, (c) free-flight of the sample towards the end-wall, and (d) initial impact with the end-wall.

It can be seen that the hydrogel partly fails in mid-flight but sustains the predominant amount of damage from impact. From the high-speed camera, the impact force could be estimated from Newton's Second Law as approximately 0.15 N, translating to 0.12 bar pressure given the sensing area of the Dytran[®] 2300V1 transducer (Sensor 2), a sample mass of 61 g and an impact acceleration of $2.43 \pm 0.50 \text{ m s}^{-2}$. However, it was found that energy loss to the dynamic deformation of the hydrogels was a dominant effect, resulting in enhanced mitigation rather than amplification with stand-off distance. This is shown in Figure 8.15.

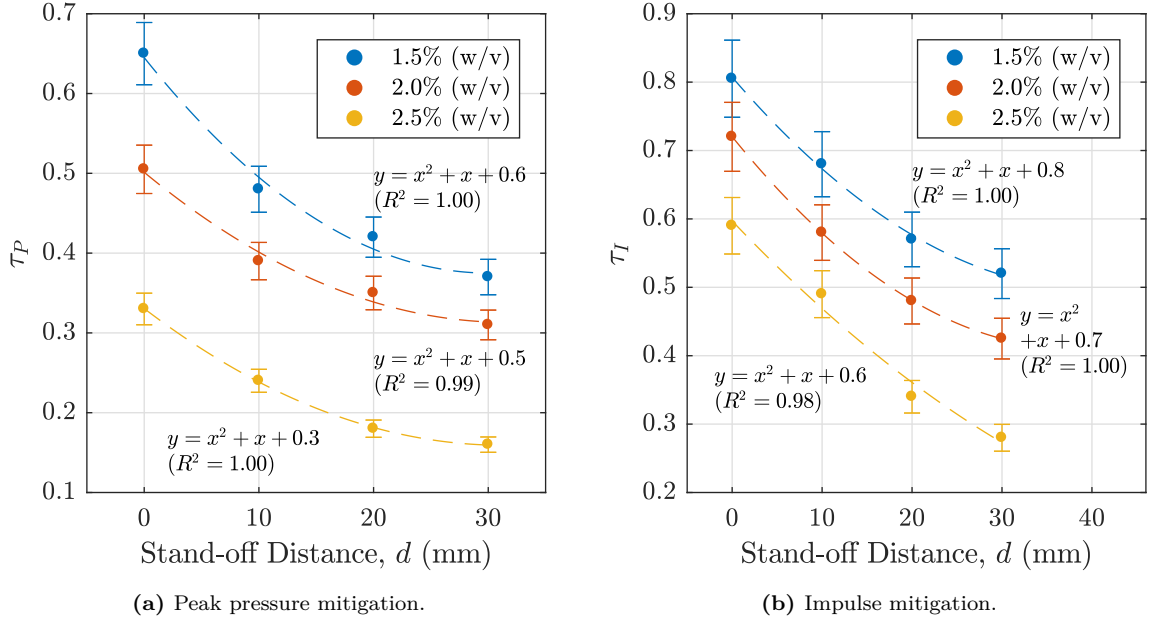


Figure 8.15: The effect of stand-off distance, d , from the rigid rear boundary for two-phase hydrogels in an unconfined configuration. The samples were loaded with a Mylar[®] 75 μm diaphragm ($P_s = 3.0 \pm 0.2$ bar, $I_s = 3.2 \pm 0.3$ bar ms). The uncertainties represent the standard error of the mean over 3 repeat independent measurements. P_s : Blast peak incident overpressure, I_s : Blast incident positive phase impulse, τ_P : Peak pressure mitigation ratio, τ_I : Impulse mitigation ratio.

There was no increase of peak pressure from the impact of the hydrogels against the end-wall. Note that the uncertainties of the mitigation ratios are greater for non-zero stand-off due to additional stochastic variations introduced from angle of impact and material loss during loading. Nonetheless, it can be seen that the peak pressure and impulse mitigation is most non-linear in this loading scenario. Indeed, the strain rate imposed on the sample on impact was linearly proportional to the stand-off distance, given that the sample acceleration for a given incident pressure was be equal. From the mechanical characterisation, this non-linearity is likely a result of the fact that the hydrogels exhibited highly non-linear failure stresses under dynamic loading with small increases of strain rate corresponding to substantial changes of elastic modulus and energy absorption per unit volume (see Figure 6.8).

8.3.3 Comparison to Water Blast Mitigation

In comparison to the mitigation values reported by investigators for supported water-based systems (typically water-filled plastic bags), it should be noted that water-based

mitigation has been found to yield peak pressure mitigation in the range of 20% to 80% as a result of strong dependence on distance of the armour from the charge. However, in the far-field, relevant to the Friedlander waves generate by the shock tube, the hydrogels presented similar shock attenuation properties. Indeed, both Marchand *et al.* [211] and Joachim and Lunderman [212] independently reported peak pressure reductions of 70% in TNT experiments, agreeing with the values found here for the hydrogels. Similarly Bornstein *et al.* found that water barriers applied to armoured vehicles, representative of the rigid confined shock tube boundary condition, resulting in impulse reduction of approximately 20%, also agreeing with the hydrogel results [213]. As such, as a matrix entrapping predominately water, it was considered that the hydrogels provided mitigation values comparable to water. Bornstein *et al.* note that the predominantly energy attenuation mechanism for water is momentum extraction through dispersion of the material [213], and this is analogous to the material transport that was observed for the hydrogels.

8.4 Conclusions

The shock tube scale investigations of the key parameters determining the blast mitigation performance of the polyurethane and hydrogel samples indicate the materials that may present promise for personal blast protection applications. As a comparison, the peak pressure and impulse mitigation ratios for the materials are summarised in Table 8.1 and Table 8.2 for the equivalent loading scenario.

Type	Porosity	τ_P	τ_I
Reticulated	20 ppi	1.61±0.06	0.98±0.06
	40 ppi	1.98±0.08	0.97±0.06
	60 ppi	2.43±0.11	1.03±0.07
	80 ppi	2.72±0.11	0.96±0.07
Syntactic	$\rho^*/\rho_s = 6\%$	3.63±0.15	1.04±0.08
	$\rho^*/\rho_s = 4\%$	2.68±0.12	0.95±0.05
	$\rho^*/\rho_s = 2\%$	1.83±0.07	0.83±0.06

Table 8.1: Polyurethane foam peak pressure (τ_P) and positive phase impulse (τ_I) mitigation ratios for confined shock tube loading with a peak incident blast pressure of 3 bar. The uncertainties represent the standard error of the mean over 3 repeat independent measurements. ppi: Pores per inch, ρ^*/ρ_s : Foam relative density, ρ^* : Foam bulk density, ρ_s : Foam skeletal density.

Type	Hydrogel Concentration % (w/v)	τ_P	τ_I
Homogeneous	1.5	0.78 ± 0.04	0.88 ± 0.06
	2	0.60 ± 0.03	0.74 ± 0.05
	2.5	0.32 ± 0.02	0.61 ± 0.04
Two-phase	1.5	0.76 ± 0.04	0.83 ± 0.06
	2	0.52 ± 0.03	0.70 ± 0.05
	2.5	0.22 ± 0.02	0.54 ± 0.05

Table 8.2: Hydrogel peak pressure (τ_P) and positive phase impulse (τ_I) mitigation ratios for confined shock tube loading with a peak incident blast pressure of 3 bar. The uncertainties represent the standard error of the mean over 3 repeat independent measurements.

For the open-cell reticulated foams, the key findings were that:

- The presence of the foam led to enhancement of the incident shock wave peak pressure.
- The enhancement effect increases with foam porosity and indicates the importance of gas dynamics in the amplification process. Indeed, the occlusion of the blast gaseous flow led to a relative reduction of the amplification.
- The enhancement effect increases with the initial foam length up to a critical length.
- Foam stand-off distance from the end-wall does not significantly affect the waveform of the transmitted pressure, with the exception of an initial elastic inertia peak.

As a result, reticulated foams are not appropriate materials for mitigation of air blast. The syntactic foams provided an even more substantial amplification of pressure and impulse. From the mechanical characterisation of the syntactic foams, it can be seen that the strength of the syntactic foams was of the order of MPa. The blast loading was not sufficient in duration or amplitude to initiate porous collapse of the syntactic foams, resulting in amplification of the blast parameters due to the impedance coupling at the additional air-material interface. For all reticulated materials, the impulse was not found to be mitigated within uncertainties as a result of the elastic deformation of the materials.

The brittle fracturing hydrogels provided significantly greater peak pressure impulse mitigation than the polyurethane foams. The modification of the homogeneous hydrogels by addition of agarose beads lowered the failure stress of the material and effectively

enhanced the blast mitigation performance by increasing energy loss through brittle fracture processes. This mitigation was increased with increasing matrix concentration, with the 2.5% (w/v) providing the highest attenuation. Comparing to the dynamic mechanical characterisation, higher agarose concentration led to increased failure stress, and the resulting relative increase of energy absorbed per unit volume was appropriately equal to the increase of impulse mitigation. In addition, the mitigation by the two-phase materials was enhanced with both stand-off distance and removal of circumferential containment. In this case, additional energy loss from lateral material transport, termed momentum extraction by Bornstein *et al.* [213], resulted in the greatest blast mitigation effect. Nonetheless, the predominant driver for blast energy dissipation appeared to be the dynamic deformation and damage of the hydrogels. The blast mitigation results for the hydrogels were comparable to literature values reported for water-filled structures, indicating that the polysaccharide matrix does not substantially alter the material's blast mitigation behaviour relative to fluid water.

From these results, the appropriate explosive scenarios for these materials can be estimated. While the open-cell polyurethane foams amplify the blast wave transmission, the syntactic foams may find useful applications under the high amplitude loading present in the blast near-field where pressures approach MPa levels (higher than can be generated with the CBIS shock tube). This has been indicated for the high strain rate SHPB loading where MPa loading stresses enabled access to the material's stress plateau and resulted in the energy absorption increasing by two orders of magnitude compared to quasi-static loading (see Table 6.2). In relation to the peak pressure thresholds defined in Table 2.2 for different injury outcomes, the syntactic foams may be effective for mitigating the most severe scenarios with pressure and impulse in excess of 5.5 bar and 0.024 bar ms, respectively. In the range of peak pressures and impulses below these values, the hydrogels may provide effective mitigation due to their low dynamic failure stresses in the range of 40–100 kPa (see Figure 6.8) enabling energy absorption through fragmentation mechanisms. See Table 10.1 for a breakdown of material recommendations for different explosive threats.

Chapter 9

Blast Injury Mitigation Studies

In Chapter 8, the open-field blast mitigation performance of the materials under investigation were established with respect to blast peak pressure and impulse. However, it is important to determine the clinical significance of the loading that would be transmitted to the protected structure. As the thorax is of interest in this work, larger scale experiments utilising a gelatine thoracic surrogate were carried out and the risk of pulmonary injury assessed by appealing to the Bowen curves [63]. As discussed in Chapter 8, the mitigation performance of the 2.5% (w/v) two-phase hydrogel was considered as well as the Dyneema[®] ballistic plate. The use of a compliant intermediate strength human surrogate contrasts with the use of a steel non-deforming surrogate by Wood *et al.* [28]. Wood *et al.* also note that the effect of stand-off distance between the surrogate and armour may be significant due to the risk of impact blunt injury [28]. As such, this was also investigated. Finally, an additional variable was the effect of confinement of the armour-surrogate system since this was found to have a statistically significant effect in Chapter 8 on the mitigation performance. For relevance to the OSPREY armour system (see Figure 4.9), Cordura[®] 1000D nylon fabric was used.

9.1 Experimental Setup

The experimental setup is shown in Figure 9.1.

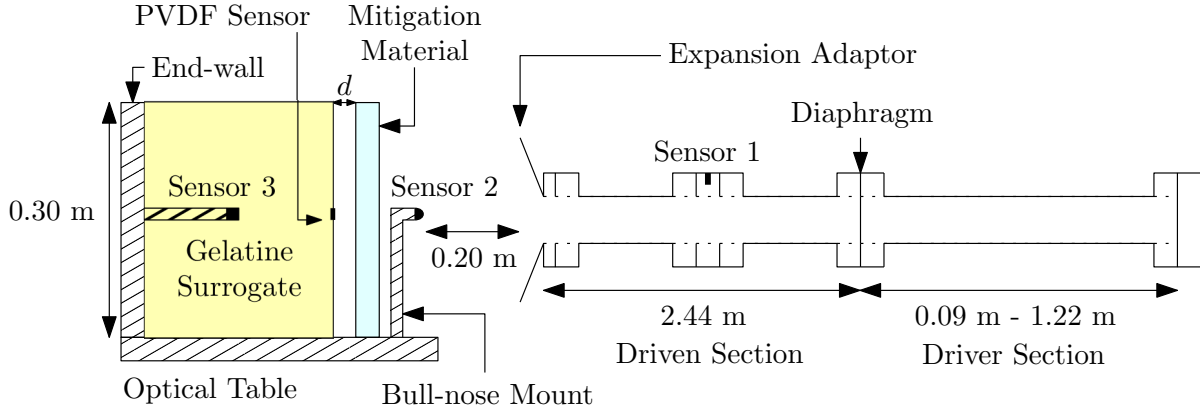


Figure 9.1: Shock tube and armour-surrogate experimental setup (not to scale). External mount (Figure 5.17) and high-speed camera not shown for clarity. The bull-nose mount for Sensor 2 utilises a round front surface to minimise its interaction with the blast wave. It was secured with three M6 bolts to an optical table which is a rigid surface with a rectangular (25 mm spaced) grid of tapped M6 holes). The gelatine was instrumented with the surface Polyvinylidene Fluoride (PVDF) sensor and internal piezoelectric Sensor 3.

To approximate the physical size of the thorax, a $30 \times 30 \times 30$ cm homogeneous ballistic gelatine block was used as a surrogate, held within the shock tube external mount (Figure 5.17). A rigid 10 mm thick stainless steel end-wall was used behind the gelatine to contain the material. Thin 1 mm thickness PTFE sheets were used between the gelatine and optical table base, as well as the end-wall, to minimise shear force exerted on the surrogate by these surfaces during dynamic loading. The surrogate was hydrated periodically with a distilled water spray. The hydrogel mitigation material dimensions were 30×30 cm, with a 2 cm thickness, equal to the thickness of material investigated in Chapter 8.

To investigate the influence of stand-off distance, the distance d between the gelatine and the rear edge of the mitigation material was varied between 0 and 30 mm in 10 mm increments. To achieve this, the plate was free-standing, and the hydrogel was secured using a steel frame and suspended from the external mount using Velcro® (see Figure 9.8). Note that there remained a finite air gap with the plate at 0 mm stand-off due its

double curved shape. The entire sample material and gelatine assembly was covered in the Cordura® 100D nylon fabric for confined shots. In terms of pressure diagnostics, Figure 9.1, Sensor 1 and Sensor 2 were the Dytran® 2300V1 sensors as used previously. The incident pressure on the surrogate was measured using the calibrated thin-film PVDF sensor (see Section 5.7) located centrally on the front face of the gelatine, bonded to the gelatine during its casting. To assess the internal pressure in the surrogate and the potential for BAPT, a Dytran® 2300C4 sensor was used in charge mode. Similarly, this sensor was embedded in the gelatine by casting it around the *in situ* sensor mounted on a 10 mm diameter head-on bracket bolted through the rigid rear end-wall. The sensor was positioned at the midsection of the gelatine (15 mm from the front surface). Both sensors were connected to an oscilloscope via a Dytran® 4020 signal conditioner.

As a visual diagnostic, and to provide an estimate of impact velocity for non-zero armour stand-off, a Phantom v210 camera viewed the event. Finally, the expansion adaptor (see Section 5.6.2), was used to couple greater blast energy into the target structure with reduced losses to the environment. The range of output peak pressures, impulse and duration possible with this modification were calibrated using Sensor 2 positioned 20 cm from the adaptor muzzle. This sensor was held in a head-on bull-nose mount, designed by Dr. T-T. N. Nguyen [44]. This mount features round front edges to minimise its interaction with the blast wave. It was secured with three M6 bolts to the optical table base of the external mount. The results of this calibration are discussed in Section 9.2.

9.2 Characterisation of Large-scale Blast Loading

It was necessary to characterise the output loading generated by the expansion adaptor. The design of the adaptor was derived from analytical considerations in Section 5.6.2. The key consideration in this was avoiding the creation of turbulent regions in the loading from self-interaction of the expansion fans generated when the supersonic shock tube flow

encounters the corners of the expansion region. As a check of these calculations prior to manufacture of the part, and to obtain an estimate of the loading profile with the adaptor, Abaqus CEL simulations were carried out. It was first necessary to validate that this computational method could accurately model the physics of an expanding supersonic flow. As such, an experiment utilising an expanded region at the midsection of the shock tube's driven section was carried out. A schematic of the setup and corresponding simulation is shown in Figure 9.2

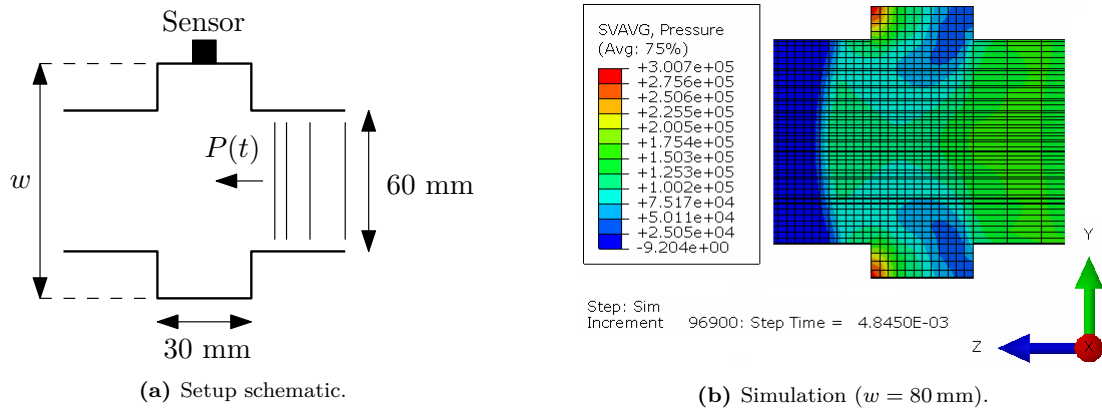


Figure 9.2: Setup (a) and simulation (b) for shock tube flow through an expansion region of diameter w . Scale bar and time units in Pa and seconds, respectively. SVAVG: Volume-averaged stress components.

In this case, the discontinuous nature of the expansion represented an instantaneous expansion of the flow. Two expanded diameters were considered, $w = 70$ mm and $w = 80$ mm, with a 6 bar driver (Mylar[®] 75 μ m diaphragm) burst pressure and full driver volume. A Dytran[®] 2300V1 pressure sensor was positioned centrally flush with the outside of the expansion region and its pressure recordings compared with that of the simulations. A finer mesh size with an approximate element size of 2 mm was used through the expansion region to provide a converged solution. It can be seen in Figure 9.2(b) that the rear edge of the expansion experiences low pressure stagnation while the front edge exhibits higher pressure due to the reflection of the blast wave. A comparison of the resulting experimental and numerical pressure profiles is shown in Figure 9.3

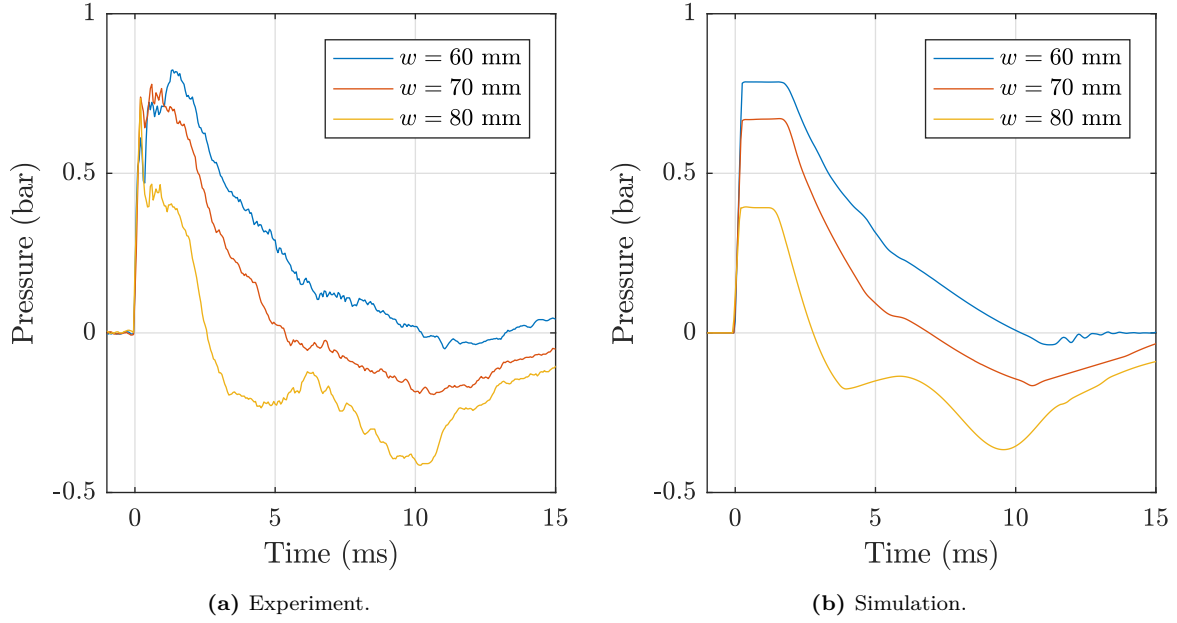


Figure 9.3: Comparison of experimental and numerical pressure histories measured centrally in the expanded region. w : Diameter of expansion region (60 mm corresponds to no expansion).

The decrease of plateau pressure with increasing expansion diameter is due to increasing physical size and duration of the low pressure stagnation region on the right side of the expansion flange (see Figure 9.2(b)). The reduction of the peak pressure amplitude is due to the pressure drop for an expanding flow. It can also be observed that a secondary peak in the negative phase of the profiles becomes more prominent with increasing expansion diameter. From the simulations, it was observed that this was due to the reflection from the front edge of the expansion region. It can be seen that the numerical results were in close agreement with the experimental results, and it was considered that the computational approach was appropriate for modelling the expansion flange output.

The aim was to numerically verify the analytical expectations for the expansion adaptor. In Section 5.6.2, the working range of the input Mach number M_1 to the expansion adaptor was found to be $1.25 < M_1 < 1.80$. As such, M_1 was varied from 1.05 to 2.50 in approximate 0.05 increments by gradually increasing the simulation driver burst pressure. This enabled the limiting behaviour of the adaptor to be explored. To do this, a 0.50 m diameter section of the surroundings was modelled at the outlet to allow free expansion of the blast wave with an outlet Eulerian boundary condition defined at the

end of the model. For a blanking volume, shot, the resulting pressure profiles measured at the adaptor outlet and at a 20 cm stand-off distance are shown in Figure 9.4. The stand-off distance allowed physical space for a target structure rig to be incorporated. This distance represented a compromise between loss of blast energy due to attenuation in the air and avoiding reflections between the target and adaptor.

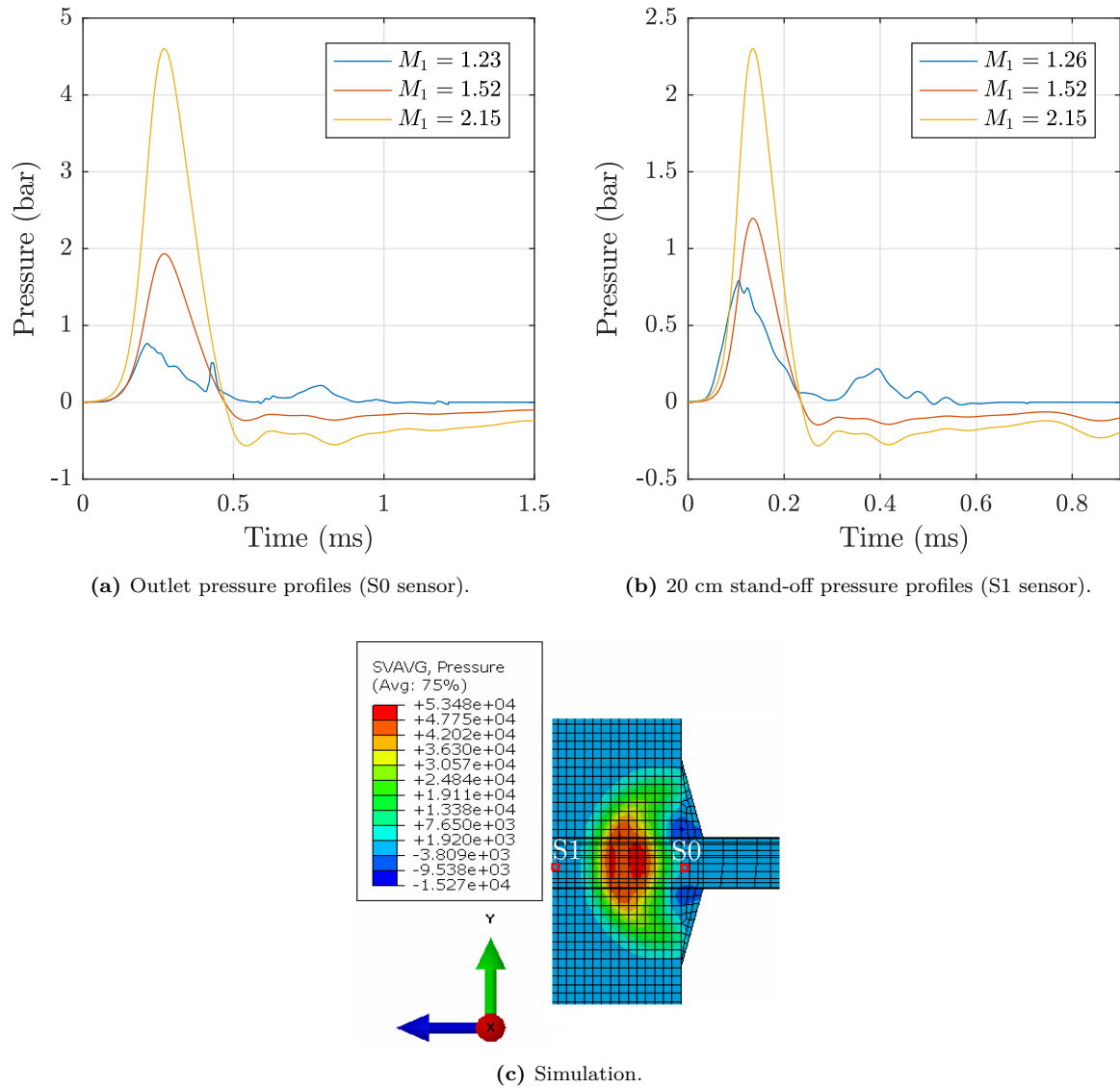


Figure 9.4: Simulation of expansion adaptor pressure output. Scale bar units in Pa. M_1 : Incident flow Mach number, SVAVG: Volume-averaged stress components.

From these simulations, it was found that the working range of the adaptor was from $1.25 < M_1 < 1.95$ at the outlet, which was similar to the analytical prediction of $1.25 < M_1 < 1.80$. For $M_1 < 1.25$, the flow was sufficiently slow that there was interaction of the expansion fans at or before the sensors resulting in a turbulent region.

In the $M_1 = 1.23$ shot of Figure 9.4(a), this was visible from the presence of additional peaks in the waveform and the lack of a blast under-pressure. For $M_1 = 1.52$, the profile was appropriately in the centre of the adaptor's working range and demonstrated the characteristic Friedlander waveform with an exponential functional drop of pressure. For $M_1 > 1.95$, this corresponded to $\theta < \theta_{max}$, so the adaptor had no effect on the peak pressure compared to the standard configuration with no adaptor.

At the stand-off distance of 20 cm, corresponding to the pressure loading that the mitigation material would experience in the setup shown in Figure 9.1, the working range was more restricted as there was greater opportunity for turbulent mixing with distance. It was found that the working range here was $1.35 < M_1 < 1.95$. Again, for $M_1 < 1.35$ in Figure 9.4(b), secondary turbulent features due to non-simple expansion fan interaction could be seen. The planarity of the blast front visible in the pressure distribution shown in Figure 9.4(c) is representative of far-field blast loading.

Since the S1 pressure was measured at a central point, it was necessary to consider the lateral pressure distribution to ensure a relatively uniform loading of the structure. This was investigated with an additional 4 sensors positioned equidistant from the central sensor inside a 30×30 cm box representing the approximate size of the target.

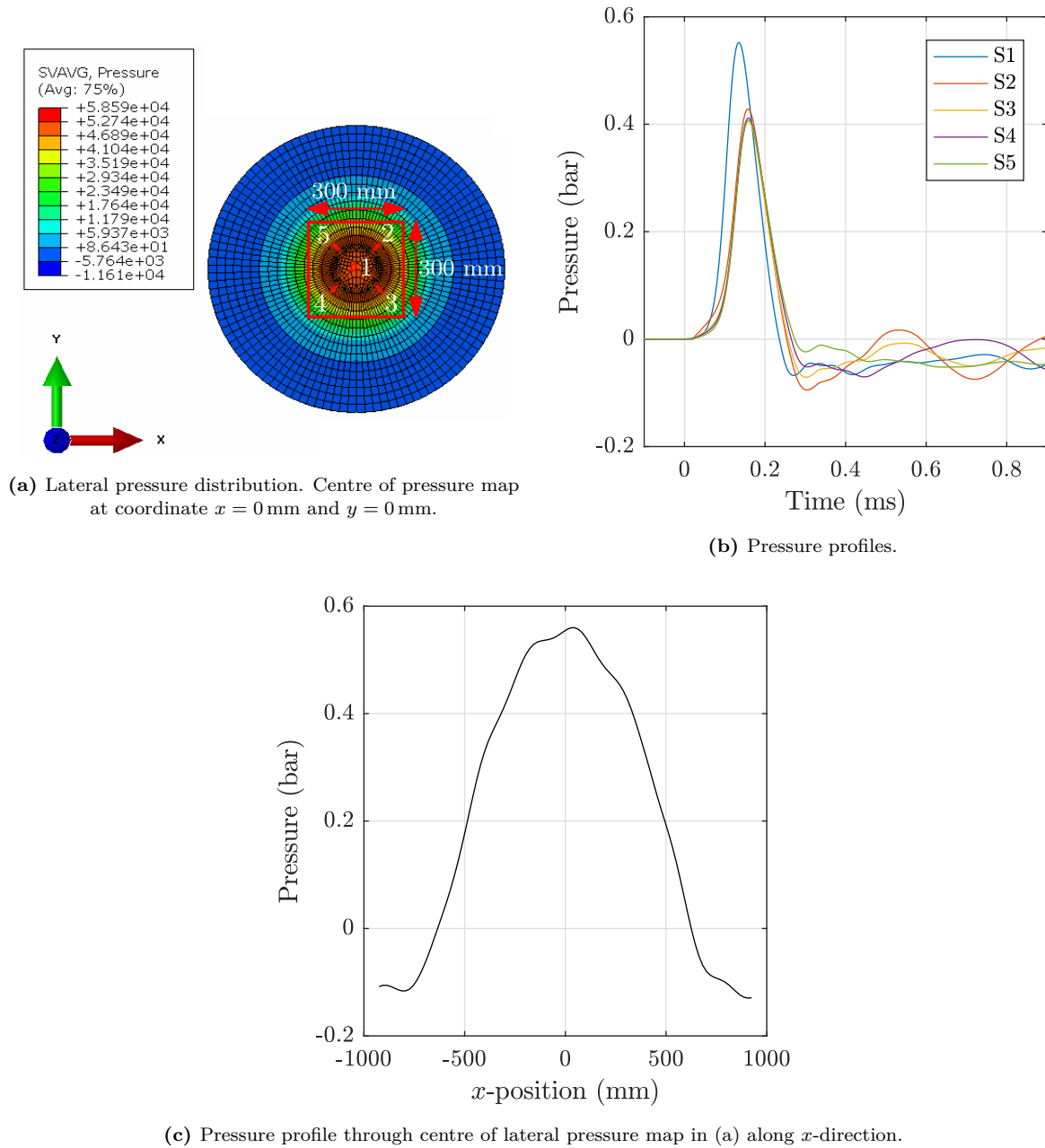


Figure 9.5: Simulation of lateral pressure distribution at a 20 cm stand-off distance from the expansion adaptor outlet. This approximates the loading that the large-scale samples would be exposed to. Note that gauge pressure is shown here. In (a), the red box overlay shows the dimensions of the target mitigation materials. In (b), the pressure profiles measured at the 5 sensor locations are shown. It can be seen that the pressure distribution is non-uniform, with a maximal pressure in the centre. This is emphasised in the (c) horizontal pressure line-out where the pressure drops rapidly with radial distance from the centre to below atmospheric pressure. Scale bar units in Pa. SVAVG: Volume-averaged stress components.

From Figure 9.5(c), it can be seen that the pressure profile is axially symmetric and reduces with radial distance from the centre. This validated that the pressure distribution did not contain anomalous features from interactions neglected by the analytical model in Section 5.6.2 such as a toroidal distribution with multiple pressure maxima. At a radial

distance of 150 mm, corresponding to the extremities of the largest samples loaded by the shock tube, the pressure dropped to approximately 17% of that in the centre. This highlights a limitation of shock tubes in ideally replicating freely propagating blast in the far-field where the pressure profile should tend towards uniformity as the large blast wave radius approximates planarity. Nonetheless, this loading method was considered valid as it is analogous to the shock tube loading carried out by Bowen *et al.* in their computation of the blast lethality curves [63]. Indeed, subsequent extensions to the curves have not closely controlled the incident blast geometry with a variety of loading methods being utilised including high explosives [65, 66]. It is noted the blast peak pressure, duration and impulse are the parameters of principal importance to blast injury assessment.

With this computational characterisation, the adaptor was manufactured and the simulations experimentally verified. In addition, the effect of the adaptor was compared to having no adaptor. As shown in Figure 9.1, the setup utilised pressure measurement at a 20 cm stand-off distance using the head-on bull-nose mount. The results are shown in Figure 9.6.

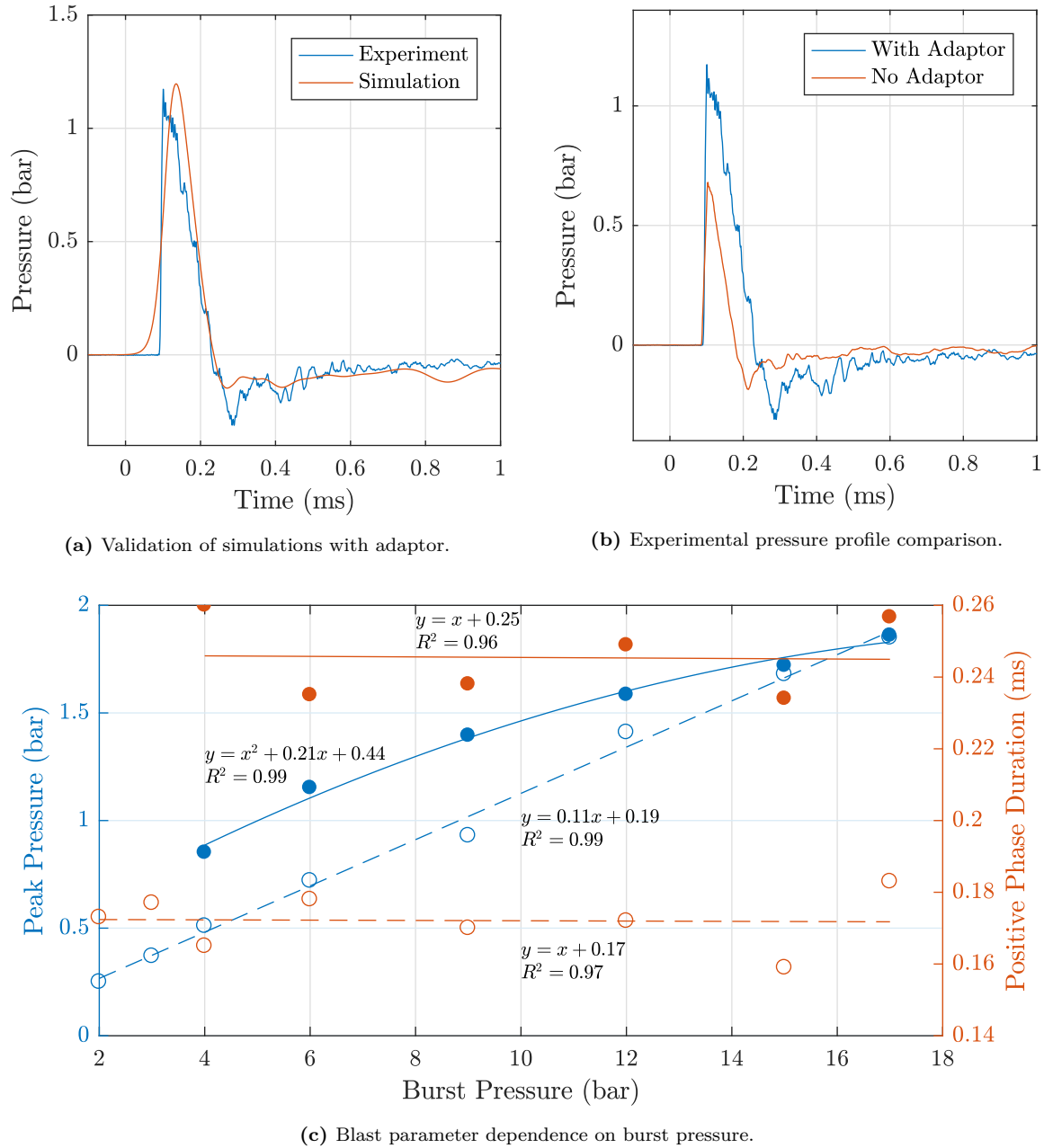


Figure 9.6: Comparison of shock tube blast output with and without the expansion adaptor (full driver volume). In (c), the unfilled and dashed data represents the standard shock tube setup and the filled and solid data represents the output with the adaptor. Additional data points are present for the non-adaptor data due to the restricted working range of the adaptor. As for the shock tube peak pressure measured at the mid-point of the driven section (see Figure 5.2(a)), the peak pressure measured at the muzzle without the adaptor was also found to scale linearly with the diaphragm burst pressure. With the addition of the adaptor, higher peak pressure and duration was achieved due to the reduction of gas losses to the surroundings. The adaptor peak pressure converged to that of the system without the adaptor with increasing burst pressure due to the flow Mach number approaching that where there was no longer interaction with the adaptor. This relationship with the burst pressure was well-described by a second order polynomial. The blast positive phase duration linear fits highlight that this parameter is manipulated only by changes to the driver volume and is approximately independent of burst pressure.

In Figure 9.6(a), it can be seen that the experimentally measured profile closely matched the computational expectation. The slower rise time of the numerical result

was due to the finite element size. In Figures 9.6(b) and 9.6(c), the enhancement of the blast peak pressure and duration (and hence impulse), is evident. In Figure 9.6(b), it can be seen that there are also additional secondary features with a less smooth drop of pressure in the data utilising the adaptor. Nonetheless, the loading still approximated the Friedlander profile. With increasing burst pressure in Figure 9.6(c), it can be seen that the adaptor and non-adaptor peak pressure data converges due to the fact that M_1 approaches the limit where the adaptor does not affect the shock front. Though higher burst pressures could not be investigated due to the maximum firing pressure limitation of the system, for higher burst pressure, it would be expected that the peak pressure with the adaptor enters a linear regime equivalent to that of the conventional system. The positive phase loading duration remained significantly higher with the adaptor compared to without it (41% greater on average) across all burst pressures. Indeed, the analytical considerations considered only the details of the shock front and not the following blast wind gases which dictate the loading duration. It was apparent that the presence of the adaptor served to couple the driver volume gases more effectively at the 20 cm stand-off distance. As such, the adaptor enabled the target structure to be loaded by a greater range of peak pressure and loading duration scenarios.

To make full use of the expansion adaptor, the loading scenarios chosen for investigation were peak incident blast pressures of 0.85 bar, 1.28 bar, 1.39 bar, 1.69 bar, 1.72 bar and 1.86 bar, with corresponding loading durations of 0.08 ms, 0.11 ms, 0.15 ms, 0.18 ms, 0.21 ms and 0.24 ms. These loading durations were generated using the polypropylene inserts to adjust the driver volume between 55% and 100%. This is applicable to nominal positive phase duration values for IEDs at 0.01–0.5 ms [214].

9.3 Influence of the Pressure Gauges

Several experimenters have considered embedding gauge diagnostics in ballistic gelatine [200, 207, 215]. At this stage, there has been little prior effort to validate embedded gauge performance in viscoelastic materials under a known load [200]. An important

consideration is whether the embedded sensor provides an accurate pressure reading, or whether the gauges can distort the pressure field due to the impedance mismatch between the sensor and surrounding medium [200]. This effect was numerically investigated by comparing the pressure measurement at the Sensor 3 location with and without the embedded sensor (and its bracket) in place to the experimental measurements. The simulated gauge and bracket were modelled as stainless steel with density 7700 kg m^{-3} , elastic modulus 190 GPa and Poisson's ratio 0.3. The gelatine was modelled using the calibrated visco-hyperelastic model discussed in Chapter 7. In addition, the accuracy of using the head-on PVDF sensor for measuring the reflected pressure at the front gelatine interface was also investigated. As discussed in Section 5.7, though the PVDF sensor was manufactured under biaxial stretching to minimise non-axial loading contributions, thin-film sensors are known to exhibit strong contributions from flexure and shear.

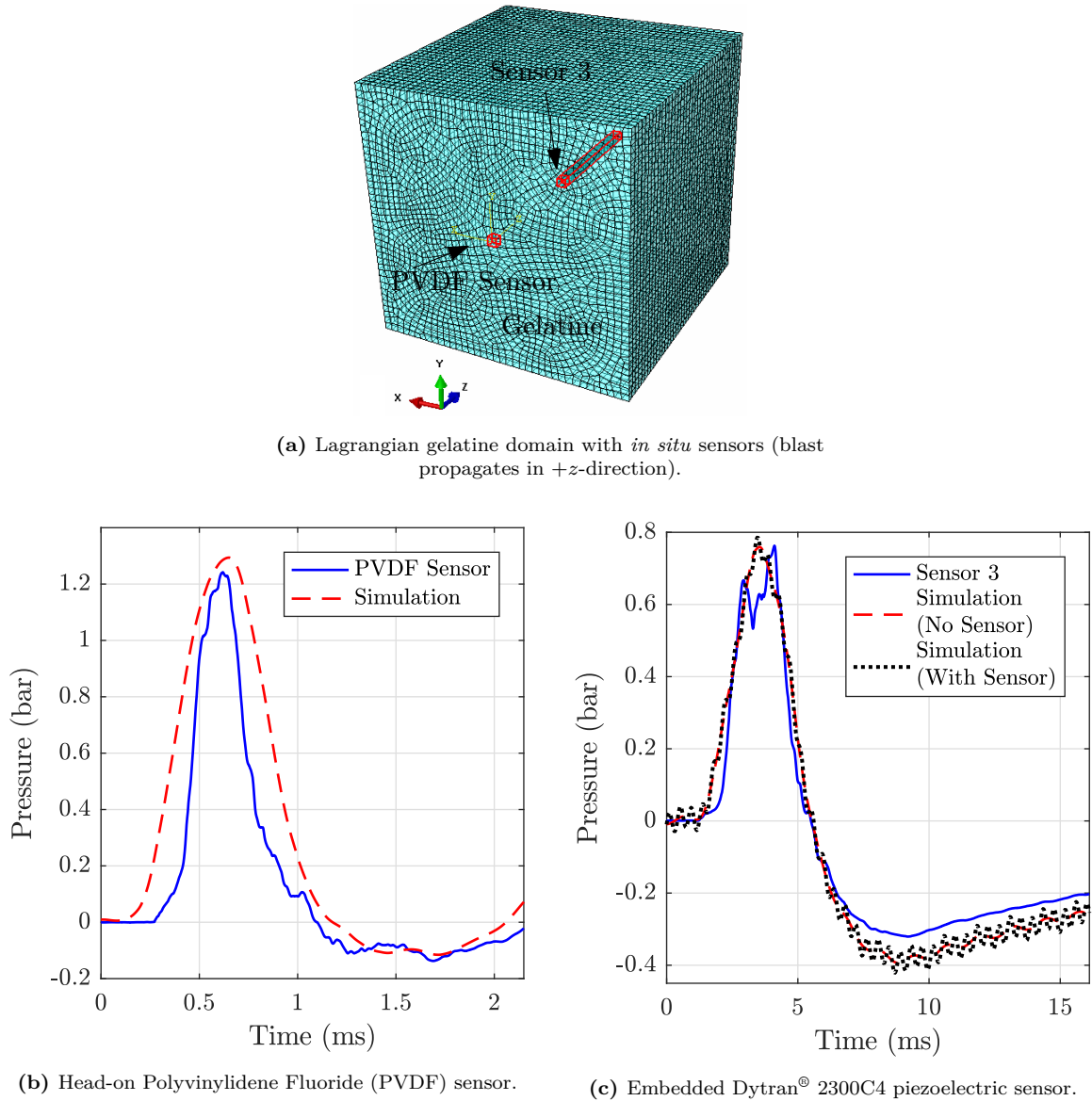


Figure 9.7: Comparison of computational and experimental gelatine pressure diagnostics.

It was observed that both sensors provided a representative reading of the gelatine waveform, with the PVDF and embedded sensors approximating the numerically determined peak pressure and loading duration. As a result of the fact that the Dytran® 2300C4 sensor minimally disrupts the pressure field, it was not necessary to consider embedding ‘floating’ PVDF sensors internally in the gelatine as an alternative. Wen *et al.* notes that such a setup would likely require correction for shear contributions to the pressure reading and present difficulties in positions the sensors in precise locations [207]. The introduction of the sensor and bracket caused a high frequency component in Figure 9.7(c), but this did not significantly alter the waveform. These findings agree

with those of Varas *et al.* [200] in application to a gelatine model of the skull, where it was suggested that the high frequency component may be an artefact of the mesh discretisation.

9.4 Ballistic Plate & Hydrogel Blast Injury Mitigation

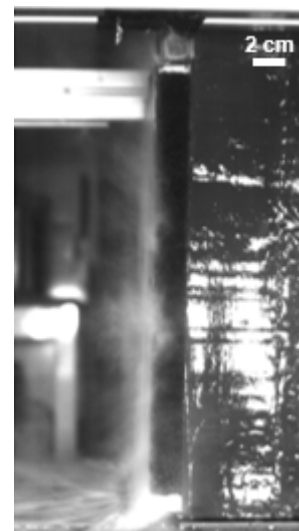
Sample high-speed photography for the ballistic plate and two-phase hydrogel is shown in Figure 9.8. A time sequence for the case of the ballistic plate at the maximal 30 mm stand-off distance is also shown in Figure 9.9. The stress wave propagation into the gelatine surrogate is visible in each case. Notably, for non-zero stand-off distance, the blunt impact of the plate against the gelatine results in significant disruption of the surrogate. Conversely, the hydrogel mitigation material can be seen to sustain widespread dynamic fracture with little deformation of the gelatine block due to rapid dissipation of the blast energy.



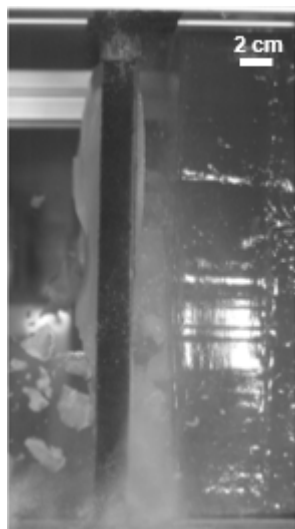
(a) Ballistic plate (0 mm stand-off).



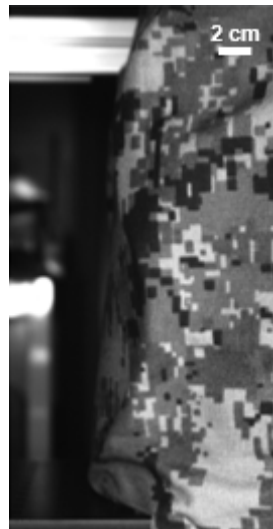
(b) Ballistic plate (30 mm stand-off).



(c) Two-phase hydrogel (0 mm stand-off).



(d) Two-phase hydrogel (30 mm stand-off).



(e) Ballistic plate (30 mm stand-off, Cordura® covered).



(f) Ballistic plate (backed with a 40 mm length of 20 ppi reticulated polyurethane foam).

Figure 9.8: High-speed photography of shock tube loading of mitigation materials. The blast is right-propagating with the gelatine surrogate visible as the right-hand region. All frames are at the same time 10 ms after triggering of the camera from Sensor 1.

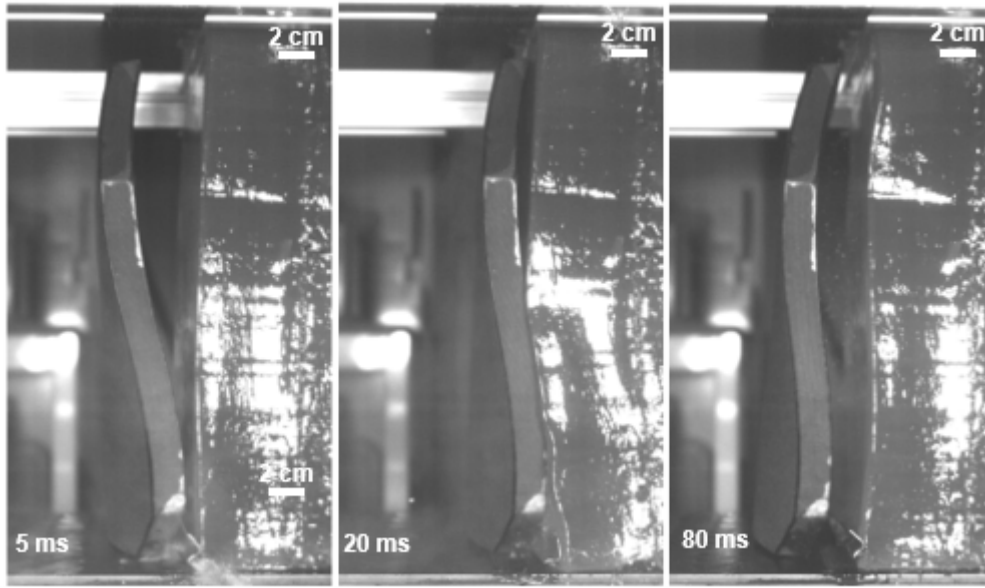


Figure 9.9: Time sequence of ballistic plate impacting the gelatine surrogate from a 30 mm stand-off, corresponding to Figure 9.8(b). The plate was observed to accelerate towards the surrogate (left), impact the gelatine (center) and finally rebound (right) towards the left, losing contact with the gelatine.

The clinical significance of the pressure measurements recording behind the mitigation material at the gelatine front surface were established using the Bowen curves for lethality probability and pulmonary injury risk [63]. The curves for a standing (long axis perpendicular to blast direction) 70 kg human target in the free-stream were used with the updates by Bass *et al.* and Rafaels *et al.* to correct the injury predictions for $t_+ < 30$ ms (as discussed in Section 2.3.4. These curves plot Eq. (2.9) with empirically determined constants.

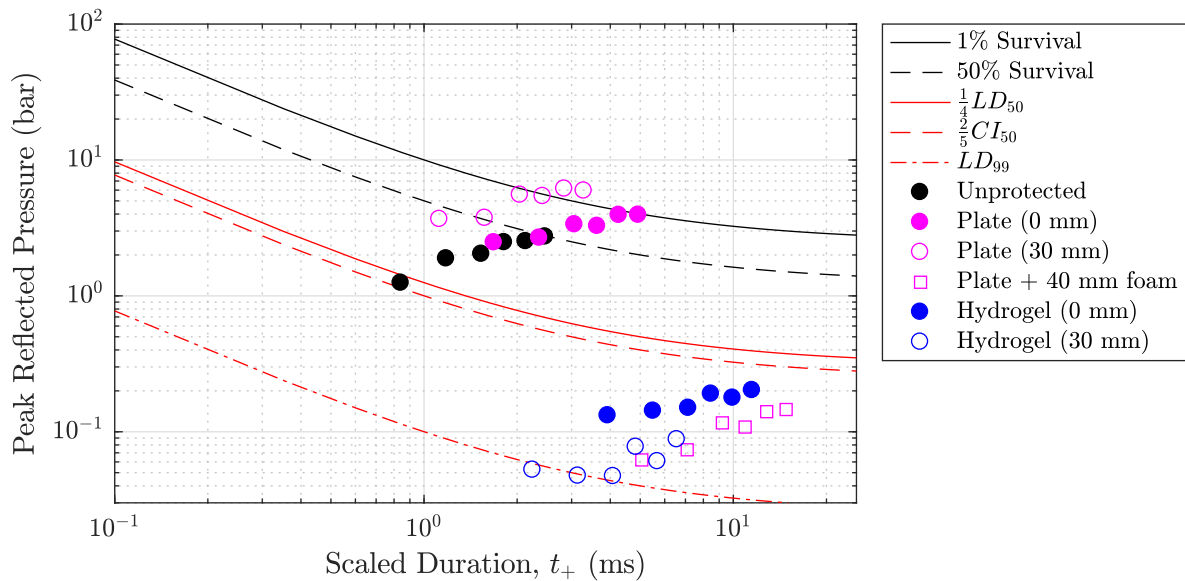


Figure 9.10: Comparison of blast performance of the mitigation materials to published lethality and pulmonary injury risk curves. The standard 1% (LD_1) and 50% (LD_{50}) survival curves are shown in black, with the Table 2.3 injury criteria shown in red. The scaled duration t_+ represents scaling of the iso-injury curves to the case of a 70 kg mass human target. The data was obtained by scanning through increasing peak incident pressure (by increasing the driver pressure) and increasing loading duration (by increasing the driver volume). The data points are computed as the mean of two repeats. LD_1 : Iso-injury curve for 1% survival probability, LD_{50} : Iso-injury curve for 50% survival probability, CI_{50} : Iso-injury curve for 50% probability of becoming Combat Ineffective (CI).

Notably, the use of the ballistic plate against the surrogate with zero stand-off resulted in a greater peak reflected pressure being experienced by the surrogate relative to the unprotected reference for each given loading scenario (on average 52% greater). This is counter to the findings of Wood *et al.* for a ballistic plate attached to a rigid steel thoracic surrogate (Figure 2.15). From impedance considerations, this can be at least partly attributed to the fact that the use of a surrogate with greater impedance than a human will lead to underestimation of the reflected pressure measured behind the plate. In addition, as noted by Wood *et al.*, the dynamic deformation of the gelatine as it is accelerated by the plate may also contribute to the enhanced peak pressure. It can be seen in Figure 9.10 that the effect is severe enough that the ballistic plate data occupies the region between 50% survival and 1% survival. This situation was further exacerbated with stand-off of the plate and resulting impact of the plate against the surrogate. This is likely due to the fact that the plate was accelerated, as depicted in Figure 9.9, resulting in a greater impact force and higher peak reflected pressure due to the greater reaction force needed to arrest the plate's motion. Nominally, the plate attained impact velocities

in the range of $2\text{--}3\text{ m s}^{-1}$. In this case, the highest peak pressure and loading duration scenarios approximated 0% survival probability.

Conversely for, the zero stand-off distance two-phase hydrogel, the blast peak reflected pressure was reduced by an average of 91.5% compared to that experienced by the unprotected surrogate. This effect was amplified with stand-off distance due to enhanced energy dissipation through material fracture, as was found in the shock tube scale experiments of Chapter 8. This figure compares with 97% peak pressure reduction for water-filled tube hydraulic redirection systems [109], and 82% reduction for 3 mm water sheets [110]. Considering the red injury criteria displayed in Figure 9.10, the hydrogel data is below the Bowen $1/4LD_{50}$ pulmonary injury criterion as well as the CI-based criterion, suggesting that such a material would be effective for preventing pulmonary injury. However, the most conservative injury criterion suggested by Kokinakis *et al.* [53] highlights the fact that there remains a finite risk of BLI.

The ballistic plate findings are in agreement with the anecdotal observations by Cooper *et al.* for plate equipped combatants [25] and for animal models with Kevlar[®] thoracic coverings. The Cooper *et al.* observation [114] that the combination of a hard ballistic plate with a foam backing is supported here. It can be seen in Figure 9.10, that the introduction of the 40 mm thickness reticulated foam backing later resulting in a substantial reduction of injury risk to a level comparable to the two-phase hydrogels. This supports Cooper's measurements at the murine scale that the combination of these materials can effectively eliminate BLI [114]. This combination exploits the large impedance mismatch ($Z_A \gg Z_B$) at the plate-foam interface to decouple the stress wave (see Eq. (2.16)). Nonetheless, though the transmission to the gelatine is low amplitude, there remains a long duration stress transmission. As such, it is necessary to consider the positive phase pressure impulse. It is also necessary to consider the impulse as the blast loading considered here is strongly in the short duration regime ($t_+ < t_{crit} \approx 15\text{--}20$ ms) where it has been suggested that the energy delivered to the body is important in determining clinical outcome [65]. This is shown in Figure 9.11.

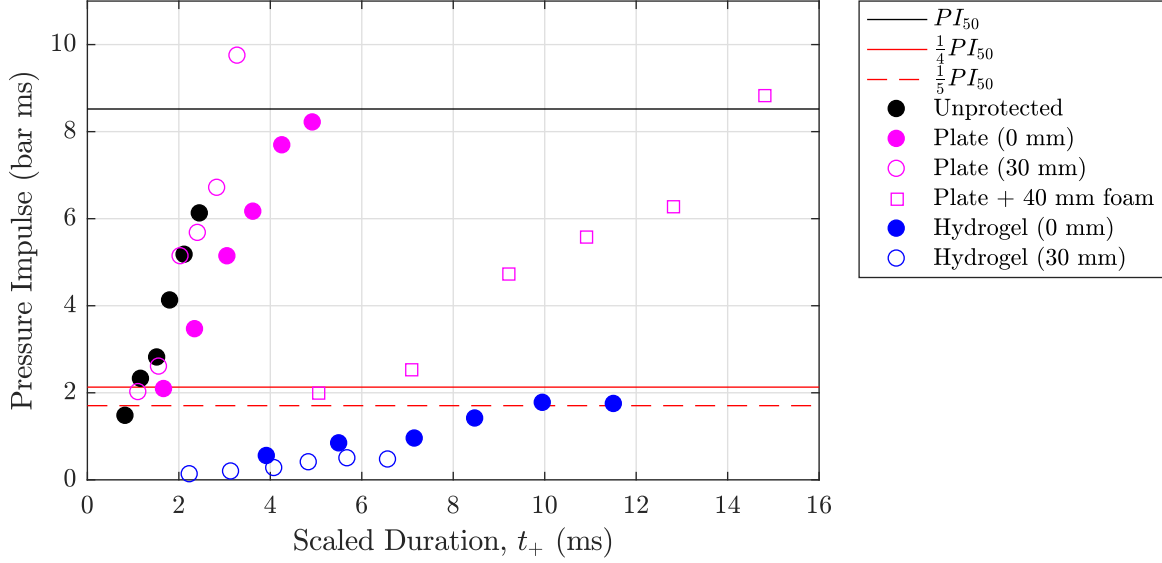


Figure 9.11: Comparison of blast performance of the mitigation materials to published pressure impulse injury criteria. The 50% (PI_{50}) survival line is shown in black, with the Table 2.3 injury criteria shown in red. The scaled duration t_+ represents scaling of the injury lines to the case of a 70 kg mass human target. The curves are obtained by scanning through increasing peak incident impulse (by increasing the driver pressure) and increasing loading duration (by increasing the driver volume). The data points are computed as the mean of two repeats. PI_{50} : Impulse iso-injury curve for 50% survival probability.

The scaled impulse for 50% lethality shown in Figure 9.11 represented the empirical measurement of Eq. (2.10) by Richmond *et al.* [71], found to be a constant value of 30 psi ms kg^{-1/3}. For consistency with the 70 kg case discussed for the Bowen curves, this value of the scaled impulse was $PI_{50} = 8.52$ bar ms. Here, it can be seen that the foam-backed plate delivers a comparable impulse to the surrogate as without the foam, but with a significantly longer duration due to the compression of the foam. This is likely due to the fact that the elastomeric foam does not dissipate significant energy during its deformation. In this case, the pressure impulse injury criteria (see Table 2.3) suggest that there is a risk of pulmonary injury. Clearly, a clinical validation of this is unavailable and the Cooper *et al.* observation of zero BLI incidence for murine models may not extend to the much larger case of a 70 kg human. Considering the plate and hydrogel data, the injury predictions are very similar to the expectations from consideration of peak pressure using the Bowen curves. At zero stand-off distance, the plate and foam-backed plate results in a 25% and 22% increase of impulse relative to the unprotected baseline, while the hydrogel reduces the impulse by 76%. Again, injury is expected with the ballistic plate whilst the two-phase hydrogels provided effective mitigation.

It is instructive to obtain an understanding of the pressure conditions internally in the surrogate as this may have relevance to injuries inflicted on the thoracic and abdominal viscera. As such, the peak pressure and impulse mitigation ratios were measured using the internally embedded sensor (Sensor 3). As in the shock enhancement experiments (Chapter 8), these were defined as the ratio of the parameter with the sample mitigation material to without the material.

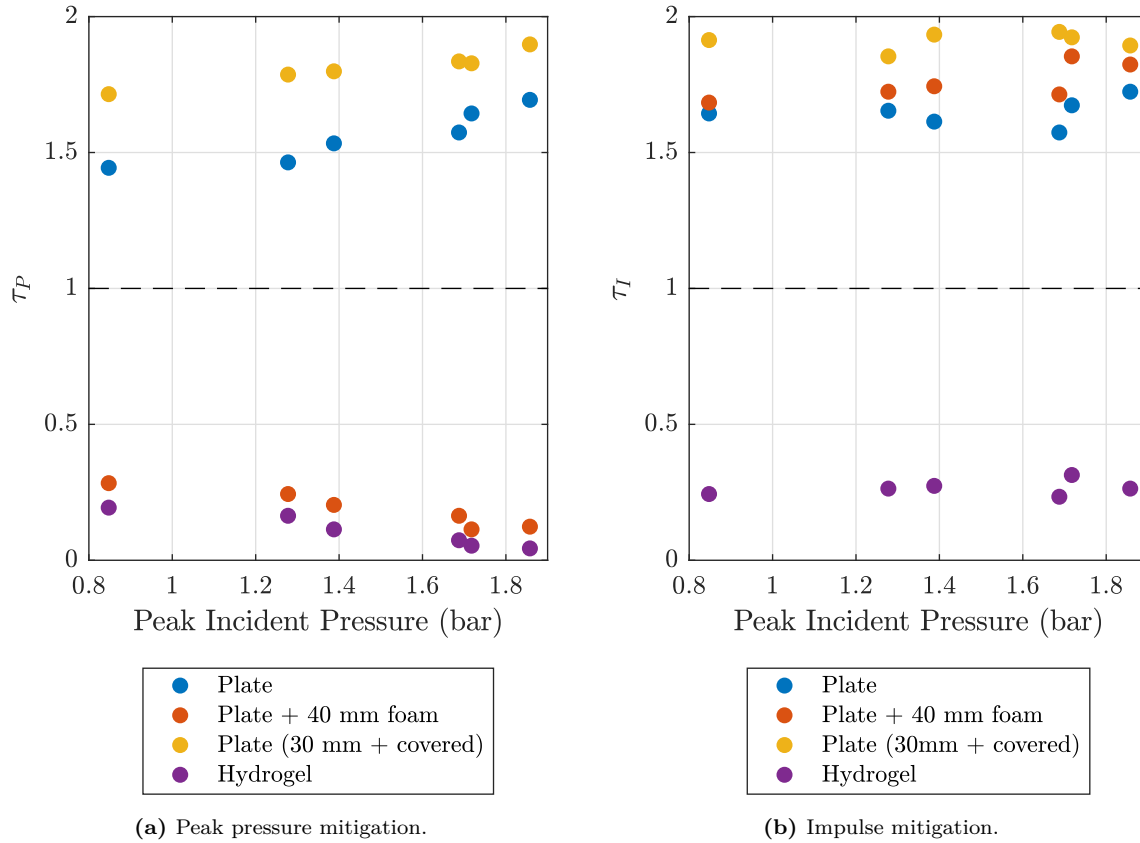


Figure 9.12: Dependence of the mitigation of peak pressure and impulse at the midsection of the gelatine surrogate on incident blast pressure. These measurements were for a loading duration of 0.24 ms. τ_P : Peak pressure mitigation ratio. τ_I : Impulse mitigation ratio.

It was found that the mitigation values were comparable to the values measured at the front surface by the PVDF sensor. As such, the additional path through the 15 cm length of ballistic gelatine did not introduce a pressure-dependent change to the mitigation effect. This may be due to the narrow range of considered peak incident pressures. Indeed, both the peak pressure and impulse mitigation appeared to be independent of peak incident pressure. As before, the ballistic plate acted to more effectively couple the blast energy into the thoracic surrogate, with the hydrogel demonstrating substan-

tial mitigation. For the hydrogel, the mitigation was substantially greater than for the prior shock tube scale experiments with a 60 mm diameter sample size shown in Figure 8.10. Indeed, for equated sample thickness and incident peak pressure, the full-scale hydrogel exhibited approximately 90% and 76% reduction of peak pressure and impulse, respectively, compared with 35% and 39% reduction at the smaller scale. This is likely a consequence of the large difference in aspect ratio between the two sample geometries, with the smaller and larger samples having aspect ratios of 3 and 15 respectively. As such, the larger sample was loaded with a more substantial bending moment resulting in greater fracture (visible in Figure 9.8(d)). In addition, the compliant non-rigid rear boundary condition resulted in greater flexure of the material. It is important to note that the surrogate does not replicate the heterogeneous nature of the thorax with the various air-tissue interfaces and harder tissues neglected. Nonetheless, these results suggest that the addition of plate armour increases the lethality of blast loading.

An additional finding in the experiments was aimed at considering ergonomic factors was the effect of a covering applied to the armour and gelatine. In Figure 9.12, it can be seen the the covering of the plate acts to further increase the coupling of the blast wave to the surrogate when combined with a non-zero stand-off distance from the protected structure. As the plate was concealed from camera view, this was further investigated computationally using a simplified setup. Here, the plate was modelled as a planar material (neglected its curvature) and the covering modelled with an impermeable air interface preventing ventilation from the air Eulerian region between the plate and gelatine. As the plate was not deformed, it was modelled as a rigid structure with density equal to 1050 kg m^{-3} such that the mass and acceleration equalled that of the plate observed through the high-speed photography. The results are shown in Figure 9.13.

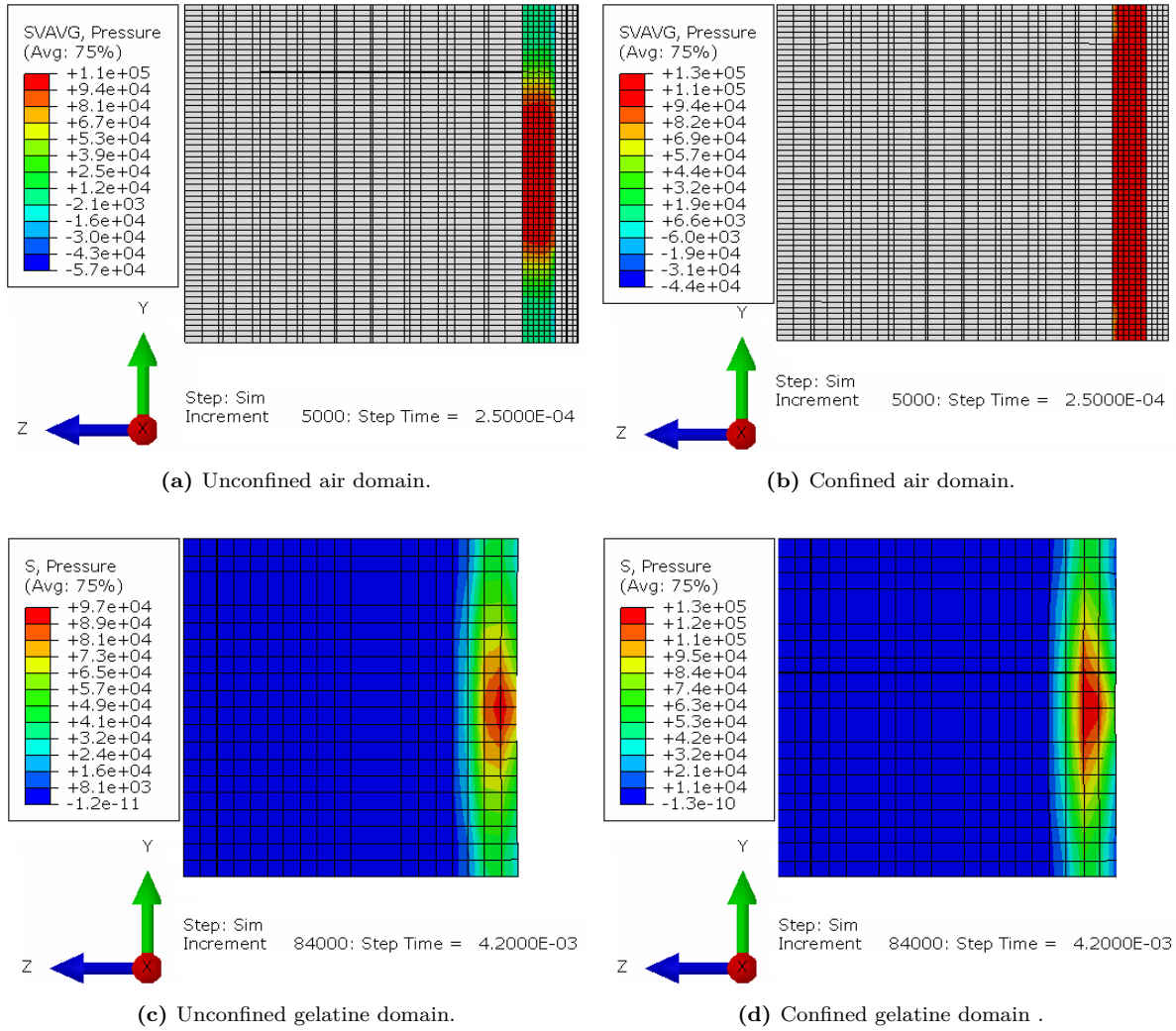


Figure 9.13: Abaqus Coupled Eulerian Lagrangian (CEL) simulation of ballistic plate blast loading with and without entrapment of the air occupying the stand-off region between the surrogate and rear surface of the plate. In (a) and (b), absolute pressure of the Eulerian air region is shown in units of Pa. In (c) and (d), the pressure field for the gelatine Lagrangian region is shown in units of Pa. Time steps are in units of seconds. A peak incident pressure of 1.28 bar and loading duration of 0.24 ms was used. SVAVG: Volume-averaged stress components.

A general contact over-closure definition was used to define the interaction between the air and gelatine, air and ballistic plate, and between the plate and gelatine. In the unconfined air of Figure 9.13(a), the pressure release at the edges is visible. In Figure 9.13(b), at the same time step, the greater pressure of the air is evident (18% greater) due to the piston-like compression of the air as the plate advances. The entrapped air also damped the acceleration of the plate resulting in an impact velocity 14% slower. However, the net effect of this was that the peak pressure transmitted to the gelatine was 34% greater in the confined case at the position of the embedded Sensor 3. This compares

with a 22% experimentally measured increase. The discrepancy can be attributed to the fact that, the numerical simulation will overestimate this increase due to the assumption of perfectly confined air. The Cordura® nylon can be considered to transiently confine the air, resulting in greater pressure transmission to the surrogate as the intervening air is compressed with reduced pressure release to the surroundings.

9.4.1 Ballistic Plate Foam Backing Thickness

It has been found that the ballistic plate acts to increase the coupling of the blast wave into the thorax, increasing peak pressure and impulse measured at the surface and internally in the gelatine surrogate. As suggested by Cooper *et al.* from murine experiments [114], the addition of a foam backing effectively reduces the peak pressure transmission by decoupling the stress wave at the plate-foam interface due to the impedance mismatch. The Wadley *et al.* analytically derived criterion, Eq. (2.20), for the minimum thickness of such a foam layer to avoid densification of the foam [116] was experimentally validated. As the Wadley *et al.* model utilises an arbitrary foam material, the reticulated 20 ppi syntactic foam was used with the parameters in Eq. (2.20) determined as $M_p = 7.1 \text{ kPa s}$ momentum per unit area of the ballistic plate (nominally determined from the acceleration of the plate on the high-speed camera), $m_f = 22 \text{ kg m}^{-2}$ plate areal density, $\sigma_{el}^* \approx 0.6 \text{ kPa}$ foam plateau stress and $e_0 = 0.05$ strain at the start of the plateau regime. The assumption in the Wadley *et al.* model is that the foam follows the ideal phenomenological foam model (Figure 2.10) and, as such, the foam parameters here are nominal values as measured in the mechanical characterisation of Figure 6.3(c). This was considered valid since the reticulated foams exhibited a sharp transition to the plateau regime. The drop-weight derived values were used as the mechanical response of the reticulated foams were not found to strongly depend on strain rate, suggesting that the values of σ_{el}^* and ϵ_D were robust. Using these values, an approximate minimum foam thickness value of $T_{min} = 3.8 \text{ cm}$ was calculated.

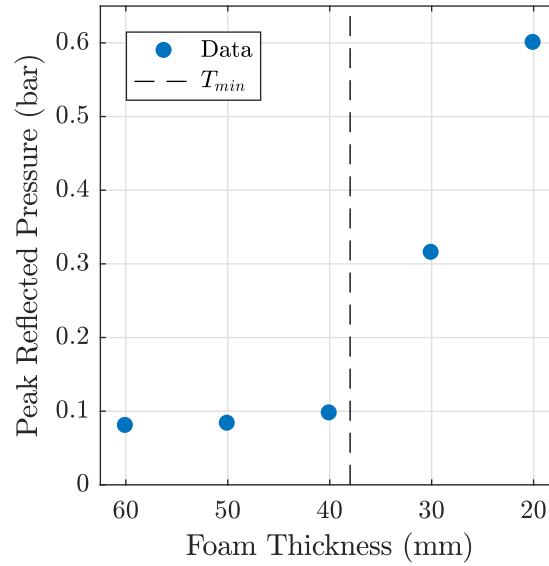


Figure 9.14: Variation of the gelatine surface peak reflected pressure with ballistic plate foam backing layer thickness. The foam was 20 pores per inch (ppi) reticulated polyurethane foam with an incident peak pressure of 0.85 bar and loading duration of 0.24 ms. T_{min} : Analytically calculated minimum foam thickness for densification.

To experimentally investigate this, the ballistic plate was loaded with varying foam backing layer thickness between 60 mm and 20 mm in 10 mm increments. As can be seen in Figure 9.14, it can be seen that the peak reflected pressure sharply rises between 40 mm and 30 mm foam thickness, agreeing with the analytical prediction of Wadley *et al.* This behaviour reflects the fact that, for given loading conditions, there is a critical foam length at which the foam densifies and the peak pressure exposed to the rear surface increases exponentially.

Though it has been shown in this work that the foam layer does not mitigate impulse, the effectiveness of this modification for reducing peak pressure may be of practical use. The results suggest that the Wadley *et al.* minimum foam length criterion is a valid predictor for designing such a modification. Though the foam considered here may be impractically bulky, alternative foams with a higher plateau stress or strain (increasing the denominator of Eq. (2.20)), may result in viably thin backing layers.

9.5 Conclusions

In this chapter, the mitigation performance of the Dyneema[®] ballistic plate and 2.5% (w/v) two-phase hydrogel was investigated using a gelatine surrogate. This extends the data presented in the current literature by considering an intermediate strength thorax. It was found that the application of the ballistic plate to the thorax increases the risk of blast injury with each considered injury criterion predicting BLI. In fact, the data suggest a survival rate of 50% to 1% over the considered range of loading scenarios. The risk was further increased with the introduction of a finite air gap between the plate and surrogate, increasing the pressure and impulse transmitted to the surrogate. The enhanced pressure measured within the gelatine by the embedded sensor also suggests that BABT would occur from impact injury. In addition, transient confinement of the air by external fabric covering of the armour led to an approximate 22% increase of peak pressure within the surrogate due to the dynamic pressure increase of the entrapped air compresses by the advancing ballistic plate. As such, appropriate ventilation of the fabric in this area (*e.g.* in the OSPREY system) may be an important future consideration.

In comparison, the two-phase hydrogel exhibited an average peak reflected pressure reduction of 91.5% with an expectation of no pulmonary injury with the exception of the most conservative injury criterion. This mitigation was further enhanced by stand-off distance of the hydrogel from the surrogate due to increased energy dissipation through fracture of the material.

It has been previously observed that the ballistic plate can achieve substantial peak pressure mitigation with the modification of adding a foam backing layer [114]. This effect was reproduced in this work using a 40 mm backing layer of reticulated polyurethane foam. However, it was also found that the foam, while reducing peak pressure, extended the loading duration such that the overall energy impulse was approximately unchanged compared to not using the backing layer. While the hydrogel sample provided a greater mitigation effect, it does not provide ballistic protection. As such, the inclusion of such

a foam layer in the OSPREY system would yield protection against both ballistic and blast threats. In designing the appropriate foam, the analytical Wadley *et al.* foam length criterion was experimentally validated and offers a prediction of the optimal foam length required given prior mechanical characterisation of the cellular material.

Chapter 10

Conclusions

10.1 Research Summary

With ongoing occurrences of blast in military and civilian contexts, there is increased need to find practical, effective and lightweight means of mitigating blast. While much worthwhile effort has focussed on the blast resistance of civil infrastructure, there is a lesser understanding of how to mitigate the injuries inflicted on human targets. This is primarily a consequence of the combined complexity of Improvised Explosive Device (IED) loading with the response of the biological target. In particular, injuries caused by the explosive blast wave, termed primary blast injuries, are a key cause of mortality and morbidity for combatants. While ballistic armour offers protection against the penetrating threats from blast such as shrapnel, there is a current lack of consensus as to whether such armour mitigates the damaging effects of the blast wave. Clinical observations of injured populations and animal model studies suggest that ballistic plates do not mitigate blast, while laboratory shock tube studies have found the opposite results utilising a non-compliant steel human surrogate. In addition to ballistic plates, much worthwhile research has been devoted to studying the ability of cellular and water-based materials for mitigating blast. While significant mitigation effects have been reported for a number

of materials, relation of the results to the material properties as well as establishing the clinical significance of the mitigation by estimating human survivability has remained elusive.

To address this research problem, it was first necessary to motivate the need for personal blast mitigation by assessing the prevalence and severity of primary blast injury. This was accomplished by carrying out a meta-analysis of clinical studies of injured populations from 1970-2016. Following this, a variety of cellular and hydrogel materials appropriate for personal applications, nominally defined in this work as having a maximum material thickness of 20 mm, were manufactured such that they were isotropic and reproducible, then mechanically characterised and their blast mitigation performance evaluated. To do this, the stress-strain response of the materials were determined over strain rates in the range of $0.01\text{--}1600\text{ s}^{-1}$, addressed using a combination of a universal testing machine, drop-weight and Split-Hopkinson Pressure Bar (SHPB) modified for low impedance materials. The quasi-static rates provided an understanding of the load-bearing characteristics of the materials, while the dynamic rates were relevant to blast loading. The mitigation materials were then blast loaded using a shock tube and the peak pressure and impulse attenuation measured. Due to the interest in personal armour in this work, additional ergonomic considerations when performing these experiments were the influence of an air gap between the mitigation material and rear surface, and the influence of confinement of the material as would be the case in a personal body armour system. With this data, the most effective mitigation material was compared with a Dyneema® ballistic plate under blast loading at a scale representative of the human thorax using a North Atlantic Treaty Organisation (NATO) ballistic gelatine human surrogate. This extends the current understanding in the literature by utilising a deforming surrogate. In order to accomplish this, the shock tube was modified to provide loading of larger scale samples, with the design of the adaptor informed by analytical and computational considerations. The shock tube and dynamic response of the gelatine were computationally modelled with a calibrated visco-hyperelastic constitutive model describing the gelatine. The blast injury mitigation performance of these materials were evaluated by utilising

previously published estimates of human tolerance to blast.

The key findings of this research are summarised as follows:

- Blast injury epidemiology suggests that more enclosed spaces place greater priority on the need to mitigate primary blast injury. This is both due to increased targeting of such spaces by hostile actors and the increased peak pressure and loading duration inflicted on the targets in a non-ventilated environment. The thorax and abdomen are of particular interest for mitigating mortality. Conversely, in more open environments, penetrating trauma is predominant.
- In mechanically characterising the materials, syntactic polyurethane foams and hydrogels exhibit strong strain rate dependent stress-strain behaviour. Due to the dynamic loading of blast mitigation materials, high strain rate material response is an important consideration as strain hardening may prevent effective energy dissipation through collapse of the cellular material. Heterogeneous hydrogels composed of low stiffness hydrogel elements within a stiffer hydrogel matrix exhibit stiffness comparable to the matrix hydrogel at quasi-static rates, with the stiffness of the low impedance inclusions at high strain rate. This allows the material to be self-supporting such that a mitigation plate can be constructed while readily fracturing under blast loading to effectively dissipate energy.
- Ballistic gelatine can be well-described under dynamic loading by a visco-hyperelastic constitutive model. Application of this to a gelatine finite element model loaded by a shock tube in a Coupled Eulerian Lagrangian (CEL) configuration results in close agreement to experimentally measured values of the gelatine external and internal pressure.
- In shock tube studies, reticulated polyurethane foams led to enhancement of the incident shock wave peak pressure, and this effect increases with foam porosity due to the role of the gas dynamics. This enhancement also increases up to a critical initial foam length. Non-collapsing syntactic foams enhanced the peak transmitted pressure due to impedance coupling of the blast wave at the additional air-material

interface. By contrast, brittle fracturing hydrogels provided significant blast peak pressure and impulse mitigation due to energy loss during deformation of the material. Increasing hydrogel concentration and stand-off distance from the rear surface further increased this mitigation effect. Removing confinement of the hydrogels allowed lateral material transport under loading resulting in peak pressure mitigation comparable to those previously reported for water-based blast mitigation systems.

- Ballistic plates applied to a human thoracic surrogate result in greater primary blast injury risk according to all defined injury criteria and an approximate 50% increase of peak pressure behind the plate compared to the unprotected baseline. The presence of a finite air gap between the plate and thorax acts to further increase this risk due to impact injury. Moreover, covering of the plate at non-zero stand-off with military standard nylon fabric resulted in a further 18% increase of peak pressure. From computational simulations of the plate, air and gelatine system, this effect is due to the high pressure increase of the entrapped air as the ballistic plate advances. By comparison, hydrogel-based materials with equal material thickness to the ballistic plate provide approximately 92% peak pressure reduction with an expectation of no pulmonary blast injury. Analogous injury predictions are made when considering impulse-based injury criteria.
- While reticulated foam and ballistic plates alone were found to enhance the transmitted blast peak pressure and lethality, the combination of the two with the plate backed by a layer of foam results in an approximate 90% reduction of peak pressure. However, this modification does not mitigate the delivered impulse. Such a foam layer can be designed using an analytical prediction of the optimal foam length that was experimentally validated in this work.

10.2 Research Impact

Recent conflicts have emphasised an increasing problem on the modern battlefield. Owing to their ease of manufacture and destructive capability, Improvised Explosive Devices

(IEDs) have become the weapons of choice for hostile actors in both military and civilian settings. In the latter case, it has been shown in this work that in the upsurge of terrorist activity since 9/11, terrorist bombings have been responsible for the greatest loss of life relative to any other attack method. IEDs are responsible for inflicting the most complex patterns of injuries, and this is strongly dependent on the deployment method and attack environment. As part of this injury pattern, while the risk of penetrating trauma from shrapnel has been lowered due to advances in ballistic armour design, injuries inflicted directly by the blast wave, termed primary blast injuries, have become one of the most significant causes of morbidity and mortality for combatants in recent conflicts [57–59]. There is often no external indication of such injuries, rendering diagnosis and triage highly challenging for clinicians. In fact, primary blast injuries can manifest days after the event with fatal consequences for casualties that may initially report no injuries. This considerable challenge continues to be met across a range of disciplines including prevention, mitigation and rehabilitation. In the field of mitigation, there is a present lack of understanding of which materials might be appropriate for personal use and what level of injury prevention may be achieved.

The central objective of this research has been establishing the clinical significance of the blast mitigation performance of a range of candidate materials. Maintaining a focus on personal applications, the materials were chosen to be lightweight and thin (20 mm) for applicability to modern armour systems. The materials included open-cell reticulated foams, closed-cell syntactic foams, and homogeneous and two-phase hydrogels (composed of approximately 99% water). Table 10.1 summarises the materials recommended for different representative IED scenarios. These recommendations have been determined on the basis of comprehensive compressive mechanical characterisation of these low impedance materials at strain rates from quasi-static to high strain rate. Note that this data is of relevance to wider energy absorbing applications beyond blast including automotive crashworthiness research.

Peak Incident Overpressure (bar)	Impulse (bar ms)	Example Threats [39]	Unprotected Outcome [55]	Recommended Material(s)
<1.4	<0.45	- Pipe bomb at >7 m - Vest at >10 m	Sufficient to cause TMP	- Two-phase hydrogel - Ballistic plate & reticulated foam
1.4-3.4	2.0 - 2.9	- Vest at 5 m - Compact car bomb at 20 m	Minority of casualties with BLI	- Two-phase hydrogel - Homogeneous hydrogel - Ballistic plate & reticulated foam
3.4-5.5	7.8 - 8.8	- Compact car bomb at 11 m - Full size car bomb at 15 m	Majority of casualties with BLI	- Two-phase hydrogel - Homogeneous hydrogel
>5.5	>24	- Vest at <3 m - Delivery truck bomb at <25 m	Severe BLI and majority dead	- Syntactic foam

Table 10.1: Recommended uses of the materials considered in this research for a variety of explosive scenarios. The threat nomenclature and TNT equivalent masses are defined by the US Department of Homeland Security in Table 2.1. The overpressure and impulse values are estimates and are derived from TNT characteristic curves for hemispherical explosive charges located on the ground [40]. Examples of such curves are shown in Figure 2.5. TMP: Tympanic Membrane Perforation, BLI: Blast Lung Injury.

For blast injury mitigation, the key parameters of interest are the blast peak pressure and impulse. Open-cell reticulated foams were found to enhance the blast wave peak pressure delivered to the protected structure and are not recommended for blast protection under any scenario. As the material is highly compressible, at such low blast loads to avoid densifying the material, the inherent human tolerance to blast is sufficient to negate any injury without the need for such a layer. Conversely, the hydrogels have been found in this work to effectively mitigate both peak pressure and impulse over a range of scenarios as shown in Figures 9.10 and 9.11. The homogeneous hydrogel dissipates energy through widespread fracture of the material at failure stresses of the order of 100 kPa. This performance was improved by engineering a two-phase heterogeneous variant comprised of two contrasting hydrogel concentrations. This structure exploited the

high strain rate dependence of the material by promoting material failure under dynamic loading such as blast (by lowering the failure stress to around 40–90 kPa) while retaining strength under static loading such that an armour panel could be manufactured. While water-based systems have previously demonstrated substantial blast mitigation promise, such solutions have not been intended or practical for personal use. This initial investigation of hydrogels has indicated that they can provide similar mitigation performance while being manufacturable in a compact and self-contained form. Finally, at much more severe blast loads, the syntactic foams are recommended as materials with moduli of the order of MPa. In practice, such blast amplitudes are most often seen in the blast near-field close to the detonation centre. Such a material can readily absorb energy at these amplitudes through structural collapse, and may therefore be of interest to the Explosive Ordnance Disposal (EOD) community where such blast loads are seen. In considering which regimes of peak pressure and impulse are of greatest interest for mitigation for combatants, it is instructive to note that appropriately 70% of IEDs encountered in Afghanistan were victim-operated devices such as pressure plates, trip wires and spring-loaded releases [216]. As these devices employ smaller charges but also are detonated close to their targets, the pressure and impulse regimes personnel are exposed to cannot be precisely predicted. However, it can be estimated that the majority of survivable blast exposures will exhibit peak pressures and impulses less than 5.5 bar and 24 bar ms, respectively, with the most severe scenario presented in Table 10.1 constituting an extreme dose with high lethality. Thus, the materials with the greatest potential for saving lives will robustly mitigate the range of peak pressures and impulses in the ‘grey area’ of survivability up to these upper limits of human tolerance. The two-phase hydrogels are recommended in this regard as materials exhibiting the most significant mitigation performance across the widest range of blast loading conditions as shown in Figures 9.10 and 9.11.

A significant finding in this work has been that the addition of a ballistic plate acts to enhance the peak pressure and impulse delivered to the body, worsening clinical outcome. This finding is significant as prior work has considered a non-biofidelic human

surrogate that led to overestimation of the level of protection. The findings presented here challenge the notion that armour automatically implies protection, and reinforces anecdotal evidence from the battlefield of casualties presenting little penetrating morbidity due to being equipped with ballistic armour, yet succumbing to primary blast injuries [25]. Moreover, the detrimental effect of the body armour is amplified when an air gap is introduced between the armour and body, as can sometimes occur in real-world use, and increases further still when the system is enclosed by a heavy fabric as is used in military armour systems. In fact, in this scenario, lethality was found to approach 99% across a wide range of blast loads. In the context of problematic standard issue armour fit entering the public discourse in recent years, a significant ramification of this finding is that ill-fitting armour may act to worsen the risk of primary blast injury, especially to the thorax.

One potential solution to this issue has been presented in this work. Previous investigators have noted from analytical considerations that the addition of a foam backing layer to the ballistic plate may enable mitigation of the blast wave [116]. This combination was found to be effective for reducing peak pressure transmission to safer levels. As such, this may offer an easily implementable solution to retrofit present ballistic armour for improved blast protection. An analytical criterion for the necessary thickness of such a foam layer has been experimentally validated in this work and may aid the design of such a layer. On the other hand, the system was not found to mitigate impulse effectively. Whether it is more important to mitigate blast peak pressure or impulse is dependent on the type of injury under consideration. Tentatively, Blast Lung Injury (BLI) and Tympanic Membrane Perforation (TMP) are often considered to be sensitive to critical peak pressure thresholds while more severe abdominal injuries may occur at large impulses. Ideally, a mitigation material will minimise risk by reducing both impulse and peak pressure. In this regard, the only material in this work satisfying this requirement was the hydrogels and particularly the two-phase hydrogels. Thus, the hydrogels have been found to be a promising material in this application and warrant further investigation as a potential new material type for blast protection.

10.3 Future Work

- All blast loading was in the short duration regime ($< 15\text{--}20\text{ ms}$) defined as the characteristic time for oscillation of the thorax. A larger diameter shock tube system would enable access to a greater range of incident loading pressures and durations. This would allow for a more robust assessment of the mitigation performance of the materials. An area of particular interest is whether the materials would offer protection against gastrointestinal primary blast injuries which may occur at higher peak pressures and duration.
- The mitigation performance of the hard ballistic plate in combination with soft armour can be investigated. This would more closely replicate the armour system as it is deployed in the field. A key finding of this work has been that the combination of two layers with contrasting impedance acts to effectively decouple the blast wave. As such, the contribution of the soft armour is of interest. Moreover, consideration of the full armour system may modify the requirements of the dedicated mitigation layers that have been discussed in this work.
- The mitigation performance of the ballistic plate with a hydrogel backing layer can be investigated. In this work, it was not feasible to investigate this system due to time and resource constraints. While the two-phase hydrogel was found to effectively mitigate the blast wave peak pressure and impulse, it does not offer ballistic protection and *vice versa* for the ballistic plate. As the combination of the plate with a low impedance compressible backing layer has been shown to be effective for blast mitigation, it is possible that the combination of the two materials would yield an overall system that affords the wearer both blast and ballistic protection.
- The use of a planar homogeneous ballistic gelatine human surrogate neglects the heterogeneous nature of the body and its geometry. A progression of this work would be to utilise a multi-component surrogate or cadaver. This would more closely reproduce the response of the human chest wall. It is important to note that

the chest wall is an anatomical region that is particularly heterogeneous with soft intercostal muscles combining with the harder ribs and sternum. As the stress wave interaction with the armour system is influenced strongly by the local mechanical properties of the body, a more biofidelic surrogate to represent these tissues would enable more accurate estimation of the transmitted pressure and impulse into the body.

- The cyclic, hysteretic and fatigue properties of the materials under repeated load, as is applicable to enclosed environments or daily use, can be investigated. A practical requirement of many armour systems is that they must be reusable over extended campaigns (up to weeks) with minimal maintenance and no loss of performance if the system is not exposed to a sudden impulse. As such, it is necessary to establish how the materials fatigue (*e.g.* crack formation) under cyclic fatigue testing which may be applicable to the loads experienced under daily use. For an acute event such as blast, it is necessary to investigate whether the material continues to provide mitigation when loaded by multiple successive waves as would occur in a enclosed environment.
- While open-cell foams lead to shock enhancement alone, their combination with thin and stiff face-sheets to form sandwich composites have been widely studied under fully clamped boundary conditions and found to provide effective mitigation. These material combinations have predominantly been aimed at civil applications with heavy facesheet materials such as stainless steel. The mitigation effect of a lightweight sandwich composite appropriate for body armour is worthy of investigation as this may yield a system that offers both ballistic and blast protection. In this regard, the use of a hydrogel as a sandwich core would be of interest.
- Investigation of the effect of heterogeneous gross mitigation material structure. As the energy absorbing capabilities of cellular materials can be manipulated by introducing features such as a graded density across the thickness, analogous modifications to the materials investigated in this work are of interest. Such structures might include two-phase hydrogels with increasing matrix stiffness across the thick-

ness, or alternating regions of high and low matrix stiffness. Such modifications may enhance the tailorability of the mitigation material to different loading scenarios.

Bibliography

- [1] MS Goldberg. Updated death and injury rates of U.S. military personnel during the conflicts in Iraq and Afghanistan. Technical Report ADA612871, Congressional Budget Office, 2014.
- [2] PJ Hazell. *Armour: Materials, Theory, and Design*. CRC Press, 2015.
- [3] T Ngo, P Mendis, A Gupta, and J Ramsay. Blast loading and blast effects on structures—an overview. *Electronic Journal of Structural Engineering*, 7(S1): 76–91, 2007. Available from: <http://www.ejse.org/Archives/Fulltext/2007/Special/200707.pdf> [Accessed 11-05-2020].
- [4] N Gardner, E Wang, P Kumar, and A Shukla. Blast mitigation in a sandwich composite using graded core and polyurea interlayer. *Experimental Mechanics*, 52(2):119–133, 2012. Available from: doi:10.1007/s11340-011-9517-9.
- [5] NA Fleck and VS Deshpande. The resistance of clamped sandwich beams to shock loading. *Journal of Applied Mechanics*, 71(3):386–401, 2004. Available from: doi:10.1115/1.1629109.
- [6] VL Tagarielli, NA Fleck, and VS Deshpande. The collapse response of sandwich beams with aluminium face sheets and a metal foam core. *Advanced Engineering Materials*, 6(6):440–443, 2004. Available from: doi:10.1002/adem.200405154.
- [7] C Atas and C Sevim. On the impact response of sandwich composites with cores

- of balsa wood and pvc foam. *Composite Structures*, 93(1):40–48, 2010. Available from: doi:10.1016/j.compstruct.2010.06.018.
- [8] GN Nurick, GS Langdon, Y Chi, and N Jacob. Behaviour of sandwich panels subjected to intense air blast—part 1: Experiments. *Composite Structures*, 91(4): 433–441, 2009. Available from: doi:10.1016/j.compstruct.2009.04.009.
- [9] DD Radford, GJ McShane, VS Deshpande, and NA Fleck. The response of clamped sandwich plates with metallic foam cores to simulated blast loading. *International Journal of solids and structures*, 43(7):2243–2259, 2006. Available from: doi:10.1016/j.ijsolstr.2005.07.006.
- [10] H Arora, P Del Linz, and JP Dear. Damage and deformation in composite sandwich panels exposed to multiple and single explosive blasts. *International Journal of Impact Engineering*, 104:95–106, 2017. Available from: doi:10.1016/j.ijimpeng.2017.01.017.
- [11] KP Dharmasena, HNG Wadley, Z Xue, and JW Hutchinson. Mechanical response of metallic honeycomb sandwich panel structures to high-intensity dynamic loading. *International Journal of Impact Engineering*, 35(9):1063–1074, 2008. Available from: doi:10.1016/j.ijimpeng.2007.06.008.
- [12] H Su and J McConnell. Influences of material properties on energy absorption of composite sandwich panels under blast loads. *Journal of Composites for Construction*, 16(4):464–476, 2011. Available from: doi:10.1061/(ASCE)CC.1943-5614.0000269.
- [13] Body armour for security officers. Technical report, Centre for the Protection of National Infrastructure, 2018.
- [14] J Breeze, EA Lewis, R Fryer, AE Hepper, PF Mahoney, and JC Clasper. Defining the essential anatomical coverage provided by military body armour against high energy projectiles. *Journal of the Royal Army Medical Corps*, 162(4):284–290, 2016. Available from: doi:10.1136/jramc-2015-000431.

- [15] J Breeze, EA Lewis, and R Fryer. Determining the dimensions of essential medical coverage required by military body armour plates utilising computed tomography. *International Journal of the Care of the Injured*, 47(9):1932–1938, 2016. Available from: doi:10.1016/j.injury.2016.06.010.
- [16] J Breeze, R Fryer, J Hare, R Delaney, NC Hunt, EA Lewis, and JC Clasper. Clinical and post mortem analysis of combat neck injury used to inform a novel coverage of armour tool. *International Journal of the Care of the Injured*, 46(4):629–633, 2015. Available from: doi:10.1016/j.injury.2015.01.045.
- [17] J Breeze, LC Allanson-Bailey, NC Hunt, R Delaney, AE Hepper, and EA Lewis. Using computerised surface wound mapping to compare the potential medical effectiveness of enhanced protection under body armour combat shirt collar designs. *Journal of the Royal Army Medical Corps*, 161(1):22–26, 2015. Available from: doi:10.1136/jramc-2013-000220.
- [18] WE Gortney. Department of Defense dictionary of military and associated terms. Technical Report AD1004650, Joint Chiefs of Staff, 2016.
- [19] WG Proud, T-TN Nguyen, C Bo, BJ Butler, RL Boddy, A Williams, S Masouros, and KA Brown. The high-strain rate loading of structural biological materials. *Metallurgical and Materials Transactions A*, 46(10):4559–4566, 2015. Available from: doi:10.1007/s11661-015-2975-4.
- [20] A Ramasamy, A-M Hill, AE Hepper, AMJ Bull, and JC Clasper. Blast mines: physics, injury mechanisms and vehicle protection. *Journal of the Royal Army Medical Corps*, 155(4):258–264, 2009. Available from: doi:10.1136/jramc-155-04-06.
- [21] WG Proud. The physical basis of explosion and blast injury processes. *Journal of the Royal Army Medical Corps*, 159(suppl 1):i4–i9, 2013. Available from: doi:10.1136/jramc-2013-000030.

- [22] SD Masouros, KA Brown, J Clasper, and WG Proud. Briefing: Blast effects on biological systems. *Proceedings of the Institution of Civil Engineers-Engineering and Computational Mechanics*, 166(3):113–118, 2013. Available from: doi:10.1680/eacm.13.00009.
- [23] JAG Singleton, IE Gibb, NCA Hunt, AMJ Bull, and JC Clasper. Identifying future ‘unexpected’ survivors: a retrospective cohort study of fatal injury patterns in victims of improvised explosive devices. *BMJ open*, 3(8):e003130, 2013. Available from: doi:10.1136/bmjopen-2013-003130.
- [24] BO Brismar and Lennart Bergenwald. The terrorist bomb explosion in Bologna, Italy, 1980: an analysis of the effects and injuries sustained. *Journal of Trauma*, 22(3):216–220, 1982. Available from: doi:10.1097/00005373-198203000-00007.
- [25] GJ Cooper, RL Maynard, NL Cross, and JF Hill. Casualties from terrorist bombings. *Journal of Trauma*, 23(11):955–967, 1983. Available from: doi:10.1097/00005373-198311000-00001.
- [26] CT Born. Blast trauma: the fourth weapon of mass destruction. *Scandinavian Journal of Surgery*, 94(4):279–285, 2005. Available from: doi:10.1177/145749690509400406.
- [27] HL Lew, C Tun, and DX Cifu. Prevalence of chronic pain, posttraumatic stress disorder, and persistent postconcussive symptoms in OIF/OEF veterans: polytrauma clinical triad. *Journal of Rehabilitation Research and Development*, 46(6):697, 2009. Available from: doi:10.1682/JRRD.2009.01.0006.
- [28] GW Wood, MB Panzer, JK Shridharani, KA Matthews, BP Capehart, BS Myers, and CR Bass. Attenuation of blast pressure behind ballistic protective vests. *Injury Prevention*, 19(1):19–25, 2013. Available from: doi:10.1136/injuryprev-2011-040277.
- [29] MD Salas. The curious events leading to the theory of shock waves. *Shock Waves*, 16(6):477–487, 2007. Available from: doi:10.1007/s00193-007-0084-z.

- [30] L Davison. *Fundamentals of shock wave propagation in solids*. Springer, 2008.
- [31] POK Krehl. The classical Rankine-Hugoniot jump conditions, an important cornerstone of modern shock wave physics: ideal assumptions vs. reality. *The European Physical Journal H*, 40(2):159–204, 2015. Available from: doi:10.1140/epjh/e2015-50010-4.
- [32] WC Griffith and W Bleakney. Shock waves in gases. *American Journal of Physics*, 22(9):597–612, 1954. Available from: doi:10.1119/1.1933855.
- [33] NM Elsayed and JL Atkins. *Explosion and blast-related injuries: effects of explosion and blast from military operations and acts of terrorism*. Academic Press, 2010.
- [34] SE Rigby, A Tyas, T Bennett, SD Clarke, and SD Fay. The negative phase of the blast load. *International Journal of Protective Structures*, 5(1):1–19, 2014. Available from: doi:10.1260/2041-4196.5.1.1.
- [35] FG Friedlander. The diffraction of sound pulses I. Diffraction by a semi-infinite plane. In *Proceedings of the Royal Society of London A: Mathematical, Physical and Engineering Sciences*, volume 186, pages 322–344. The Royal Society, 1946. Available from: doi:10.1098/rspa.1946.0046.
- [36] E Yandzio and M Gough. *Protection of buildings against explosions*. Steel Construction Institute, 1999.
- [37] V Karlos and G Solomos. Calculation of blast loads for application to structural components. Technical Report JRC 32253-2011, European Commission Joint Research Centre, 2013.
- [38] J Artero-Guerrero, J Pernas-Sánchez, and F Teixeira-Dias. Blast wave dynamics: the influence of the shape of the explosive. *Journal of Hazardous Materials*, 331: 189–199, 2017. Available from: doi:10.1016/j.jhazmat.2017.02.035.
- [39] United States Department of Homeland Security. Bomb threats stand-off chart,

2018. Available from: <https://www.hSDL.org/?abstract&did=4506> [Accessed 11-05-2020].
- [40] FD Alonso, EG Ferradás, JFS Pérez, AM Aznar, JR Gimeno, and JM Alonso. Characteristic overpressure–impulse–distance curves for the detonation of explosives, pyrotechnics or unstable substances. *Journal of Loss Prevention in the Process Industries*, 19(6):724–728, 2006. Available from: doi:10.1016/j.jlp.2006.06.001.
- [41] I Cernak. *Blast Injuries and Blast-Induced Neurotrauma*. Taylor & Francis, 2015. [NLM PubReader version].
- [42] AMJ Bull, J Clasper, and PF Mahoney. *Blast Injury Science and Engineering: A Guide for Clinicians and Researchers*. Springer, 2016.
- [43] C Bo. *Investigation of the effects of high pressure pulses on biological samples*. PhD thesis, Imperial College London, 2013.
- [44] T-TN Nguyen. *The characterisation of a shock tube system for blast injury studies*. PhD thesis, Imperial College London, 2016.
- [45] HR Champion, JB Holcomb, and LA Young. Injuries from explosions: physics, biophysics, pathology, and required research focus. *Journal of Trauma and Acute Care Surgery*, 66(5):1468–1477, 2009. Available from: doi:10.1097/TA.0b013e3181a27e7f.
- [46] A Sedman and A Hepper. Protection of the lung from blast overpressure by stress wave decouplers, buffer plates or sandwich panels. *Journal of the Royal Army Medical Corps*, 165(1):22–26, 2019. Available from: doi:10.1136/jramc-2017-000882.
- [47] RF Brown, GJ Cooper, and RL Maynard. The ultrastructure of rat lung following acute primary blast injury. *International Journal of Experimental Pathology*, 74(2):151, 1993. Available from: <https://www.ncbi.nlm.nih.gov/pmc/articles/PMC2002117/> [Accessed 11-05-2020].
- [48] G Almogy, Y Mintz, G Zamir, T Bdolah-Abram, R Elazary, L Dotan, M Faruga, and AI Rivkind. Suicide bombing attacks: can external signs predict in-

- ternal injuries? *Annals of Surgery*, 243(4):541, 2006. Available from: doi:10.1097/01.sla.0000206418.53137.34.
- [49] M Bala, AI Rivkind, G Zamir, T Hadar, I Gertsenshtein, Y Mintz, AJ Pikarsky, D Amar, N Shussman, MA Gazala, et al. Abdominal trauma after terrorist bombing attacks exhibits a unique pattern of injury. *Annals of Surgery*, 248(2):303–309, 2008. Available from: doi:10.1097/SLA.0b013e318180a3f7.
- [50] G Almogy, T Luria, E Richter, R Pizov, T Bdolah-Abram, Y Mintz, G Zamir, and AI Rivkind. Can external signs of trauma guide management?: Lessons learned from suicide bombing attacks in Israel. *Archives of Surgery*, 140(4):390–393, 2005. Available from: doi:10.1001/archsurg.140.4.390.
- [51] *Injury Control: A Guide to Research and Program Evaluation*, chapter Measurement of Injury Severity and Co-morbidity, pages 32–46. Cambridge University Press, 2009.
- [52] L Moore, A Lavoie, N Le Sage, E Bergeron, M Emond, and B Abdous. Consensus or data-derived anatomic injury severity scoring? *Journal of Trauma and Acute Care Surgery*, 64(2):420–426, 2008. Available from: doi:10.1097/01.ta.0000241201.34082.d4.
- [53] W Kokinakis and RR Rudolph. An assessment of the current state-of-the-art of incapacitation by air blast. Technical Report 508:135-151, Army Ballistic Research Laboratory, 1982.
- [54] E Katz, B Ofek, J Adler, HB Abramowitz, and MM Krausz. Primary blast injury after a bomb explosion in a civilian bus. *Annals of Surgery*, 209(4):484, 1989. Available from: doi:10.1097/00000658-198904000-00016.
- [55] SG Mellor and GJ Cooper. Analysis of 828 servicemen killed or injured by explosion in Northern Ireland 1970–84: the hostile action casualty system. *British Journal of Surgery*, 76(10):1006–1010, 1989. Available from: doi:10.1002/bjs.1800761006.

- [56] JS Gondusky and MP Reiter. Protecting military convoys in Iraq: an examination of battle injuries sustained by a mechanized battalion during Operation Iraqi Freedom II. *Military Medicine*, 170(6):546–549, 2005. Available from: doi:10.7205/MILMED.170.6.546.
- [57] JE Smith. The epidemiology of blast lung injury during recent military conflicts: a retrospective database review of cases presenting to deployed military hospitals, 2003–2009. *Philosophical Transactions of the Royal Society of London B: Biological Sciences*, 366(1562):291–294, 2011. Available from: doi:10.1098/rstb.2010.0251.
- [58] C Stevenson. Evolving mechanisms and patterns of blast injury and the challenges for military first responders. *Australian Military Medicine Association*, 17(4), 2009. Available from: <https://jmvh.org/wp-content/uploads/2009/10/JMVHVol117No4-1.pdf#page=19> [Accessed 11-05-2020].
- [59] TE Scott, E Kirkman, M Haque, IE Gibb, P Mahoney, and JG Hardman. Primary blast lung injury-a review. *British Journal of Anaesthesia*, 118(3):311–316, 2017. Available from: doi:10.1093/bja/aew385.
- [60] P Radford, HDL Patel, N Hamilton, M Collins, and S Dryden. Tympanic membrane rupture in the survivors of the July 7, 2005, London bombings. *Otolaryngology–Head and Neck Surgery*, 145(5):806–812, 2011. Available from: doi:10.1177/0194599811411143.
- [61] E Gruss. A correction for primary blast injury criteria. *Journal of Trauma and Acute Care Surgery*, 60(6):1284–1289, 2006. Available from: doi:10.1097/01.ta.0000220015.21948.ec.
- [62] *Bioastronautics and the Exploration of Space: Proceedings*, chapter Biodynamic response of the human body, pages 951–958. Aerospace Medical Division, Brooks Air Force Base, 1968.
- [63] IG Bowen, ER Fletcher, and DR Richmond. Estimate of man’s tolerance to the

- direct effects of air blast. Technical Report AD0693105, Lovelace Foundation for Medical Education and Research, 1968.
- [64] DR Richmond and PW Cooper. Evaluation of Bowen's curves. Technical Report USZA 26-02-P-3274, Lovelace Foundation for Medical Education and Research.
- [65] CR Bass, KA Rafaels, and RS Salzar. Pulmonary injury risk assessment for short-duration blasts. *Journal of Trauma and Acute Care Surgery*, 65(3):604–615, 2008. Available from: doi:10.1097/TA.0b013e3181454ab4.
- [66] KA Rafaels, CR Bass, MB Panzer, and RS Salzar. Pulmonary injury risk assessment for long-duration blasts: a meta-analysis. *Journal of Trauma and Acute Care Surgery*, 69(2):368–374, 2010. Available from: doi:10.1097/TA.0b013e3181e88122.
- [67] HJ Goodman. Compiled free-air blast data on bare spherical pentolite. Technical Report 1092, Army Ballistic Research Laboratory, 1960.
- [68] Structures to resist the effect of accidental explosions. Technical Report TM5-1300/NAVFAC P-397/AIR FORCE AFR 88-22, Departments of the Army, the Navy and the Air Force, 1990.
- [69] DR Richmond, EG Damon, IG Bowen, ER Fletcher, and CS White. Air-blast studies with eight species of mammals. Technical Report AD653022, Lovelace Foundation for Medical Education and Research, 1966.
- [70] CJ Clemedson and A Jonsson. Estimates of medical risks for personnel firing with recoilless rifles from bunkers. Technical Report FOA Report A-20012-DG, Research Institute of the Defense Department, Sweden, 1976.
- [71] DR Richmond, JT Yelverton, and ER Fletcher. New airblast criteria for man. Technical Report AD-A181 274, Los Alamos National Laboratory, 1986.
- [72] TPM Johnson, SS Sarva, and S Socrate. Comparison of low impedance split-Hopkinson pressure bar techniques in the characterization of polyurea. *Experimental Mechanics*, 50(7):931–940, 2010. Available from: doi:10.1007/s11340-009-9305-y.

- [73] K Kontis, R An, H Zare-Behtash, and D Kounadis. Head-on collision of shock wave induced vortices with solid and perforated walls. *Physics of Fluids*, 20(1):016104, 2008. Available from: doi:10.1016/j.ijheatfluidflow.2008.04.010.
- [74] ISM Miller, D McGahey, and K Law. The otologic consequences of the Omagh bomb disaster. *Otolaryngology—Head and Neck Surgery*, 126(2):127–128, 2002. Available from: doi:10.1067/mhn.2002.122186.
- [75] LJ Gibson and MF Ashby. *Cellular solids: structure and properties*. Cambridge University Press, 1999.
- [76] JG Nerenberg. *Blast wave loading of polymeric foams*. PhD thesis, McGill University, 1998.
- [77] N Gupta and W Ricci. Comparison of compressive properties of layered syntactic foams having gradient in microballoon volume fraction and wall thickness. *Materials Science and Engineering: A*, 427(1-2):331–342, 2006. Available from: doi:10.1016/j.msea.2006.04.078.
- [78] G Li and VD Muthyala. A cement based syntactic foam. *Materials Science and Engineering: A*, 478(1-2):77–86, 2008. Available from: doi:10.1016/j.msea.2007.05.084.
- [79] N Gupta, SE Zeltmann, VC Shunmugasamy, and D Pinisetty. Applications of polymer matrix syntactic foams. *Journal of the Minerals, Metals & Materials Society*, 66(2):245–254, 2014. Available from: doi:10.1007/s11837-013-0796-8.
- [80] MD Goel, VA Matsagar, and AK Gupta. Blast resistance of stiffened sandwich panels with aluminum cenosphere syntactic foam. *International Journal of Impact Engineering*, 77:134–146, 2015. Available from: doi:10.1016/j.ijimpeng.2014.11.017.
- [81] MD Goel, V Matsagar, P Venkitanarayanan, and DP Mondal. Low, medium and high strain rate characteristics of cenosphere aluminum syntactic foam for blast and impact applications. In *Response of Structures Under Extreme Loading: Proceedings of the Fifth International Workshop on Performance, Protection & Strengthening*

- of *Structures Under Extreme Loading*, pages 52–58, 2015. Available from: <http://www.dpi-proceedings.com/index.php/protect2015/article/view/21411> [Accessed 11-05-2020].
- [82] R Shankar and S Sankaran. Gradient syntactic foams: Tensile strength, modulus and fractographic features. *Materials Science and Engineering: A*, 412(1-2):153–158, 2005. Available from: doi:10.1016/j.msea.2005.08.017.
- [83] A Pellegrino, VL Tagarielli, R Gerlach, and N Petrinic. The mechanical response of a syntactic polyurethane foam at low and high rates of strain. *International Journal of Impact Engineering*, 75:214–221, 2015. Available from: doi:10.1016/j.ijimpeng.2014.08.005.
- [84] P Fahr. *Response of filled corrugated sandwich structures to shock loading at high temperatures*. PhD thesis, University of Rhode Island, 2014.
- [85] QM Li and H Meng. Attenuation or enhancement-a one-dimensional analysis on shock transmission in the solid phase of a cellular material. *International Journal of Impact Engineering*, 27(10):1049–1065, 2002. Available from: doi:10.1016/S0734-743X(02)00016-7.
- [86] Rodolfo Monti. Normal shock wave reflection on deformable solid walls. *Meccanica*, 5(4):285–296, 1970. Available from: doi:10.1007/BF02145653.
- [87] MW Seitz and BW Skews. Effect of compressible foam properties on pressure amplification during shock wave impact. *Shock Waves*, 15(3):177–197, 2006. Available from: doi:10.1007/s00193-006-0033-2.
- [88] BE Gelfand, SA Gubin, SM Kogarko, and OE Popov. Investigation of the special characteristics of the propagation and reflection of pressure waves in a porous medium. *Journal of Applied Mechanics and Technical Physics*, 16(6):897–900, 1975. Available from: doi:10.1007/BF00852818.

- [89] GV Bazhenova, LG Gvozdeva, YP Lagutov, VN Liahov, YM Faresov, and VP Fokeev. *Nonstationary Interactions of Shock Waves and Detonation Waves in Gases*. Nauka, 1986.
- [90] BE Gelfand, SP Medvedev, AA Borisov, AN Polenov, SM Frolov, and SA Tsyganov. Shock loading of stratified dusty systems. *Archivum Combustionis*, 9(4):153–165, 1989. Available from: <http://www.frolovs.ru/pdf/1989-1-eng.pdf> [Accessed 11-05-2020].
- [91] T Suzuki and T Adachi. *Theoretical and Applied Mechanics*, chapter A basic study of the blast waves over a dust deposit, pages 439–446. University of Tokyo Press, 1981.
- [92] GJ Cooper, DJ Townend, SR Cater, and BP Pearce. The role of stress waves in thoracic visceral injury from blast loading: modification of stress transmission by foams and high-density materials. *Journal of Biomechanics*, 24(5):273–285, 1991. Available from: doi:10.1016/0021-9290(91)90346-O.
- [93] BW Skews. Shock wave impact on porous materials. In *Shock Waves @ Marseille III*, pages 11–20. Springer, 1995.
- [94] JJ Lee, DL Frost, and JHS Lee. Transmission of a blast wave through a deformable layer. In *Shock Waves @ Marseille III*, pages 181–186. Springer, 1995.
- [95] VF Nesterenko. *High-rate deformation of heterogeneous materials*. Nauka, 1992.
- [96] G Ben-Dor, A Britan, T Elperin, O Igra, and JP Jiang. Experimental investigation of the interaction between weak shock waves and granular layers. *Experiments in Fluids*, 22(5):432–443, 1997. Available from: <https://link.springer.com/article/10.1007/s003480050069> [Accessed 11-05-2020].
- [97] MW Seitz and BW Skews. Shock impact on porous plugs with a fixed gap between the plug and a wall. In *Proceedings of the 20th International Symposium on Shock Waves*, pages 1381–1386, 1996.

- [98] J Zhang, J Fan, Z Wang, L Zhao, and Z Li. Shock enhancement of cellular materials subjected to intensive pulse loading. *Shock Waves*, pages 1–15, 2017. Available from: doi:10.1007/s00193-017-0730-z.
- [99] OE Petel, S Ouellet, AJ Higgins, and DL Frost. The elastic–plastic behaviour of foam under shock loading. *Shock Waves*, 23(1):55–67, 2013. Available from: doi:10.1007/s00193-012-0414-7.
- [100] LG Gvozdeva, Yu M Faresov, and Yu L Sharov. Structure of deformation waves in porous compressible materials under the influence of shock waves. In *Shock Waves @ Marseille III*, pages 175–180. Springer, 1995.
- [101] MF Ashby, T Evans, NA Fleck, JW Hutchinson, HNG Wadley, and LJ Gibson. *Metal foams: a design guide*. Elsevier, 2000.
- [102] PJ Tan, JJ Harrigan, and SR Reid. Inertia effects in uniaxial dynamic compression of a closed cell aluminium alloy foam. *Materials Science and Technology*, 18(5): 480–488, 2002. Available from: doi:10.1179/026708302225002092.
- [103] BW Skews. The reflected pressure field in the interaction of weak shock waves with a compressible foam. *Shock Waves*, 1(3):205–211, 1991. Available from: <https://link.springer.com/article/10.1007/BF01413795> [Accessed 11-05-2020].
- [104] BW Skews, MD Atkins, and MW Seitz. The impact of a shock wave on porous compressible foams. *Journal of Fluid Mechanics*, 253:245–265, 1993. Available from: doi:10.1017/S0022112093001788.
- [105] M Olim, MEH Van Dongen, T Kitamura, and K Takayama. Numerical simulation of the propagation of shock waves in compressible open-cell porous foams. *International Journal of multiphase flow*, 20(3):557–568, 1994. Available from: doi:10.1016/0301-9322(94)90029-9.
- [106] K Kitagawa, M Yasuhara, and K Takayama. Attenuation of shock waves propagating in polyurethane foams. *Shock Waves*, 15(6):437–445, 2006.

- [107] K Kailasanath, PA Tatem, and J Mawhinney. Blast mitigation using water-a status report. Technical Report NRL/MR/6410-02-8606, Naval Research Laboratory, 2002.
- [108] H Jeon and V Eliasson. Shock wave interactions with liquid sheets. *Experiments in Fluids*, 58(4):24, 2017. Available from: doi:10.1007/s00348-017-2300-7.
- [109] Y Chen, W Huang, and S Constantini. Blast shock wave mitigation using the hydraulic energy redirection and release technology. *PLOS One*, 7(6):e39353, 2012. Available from: doi:10.1371/journal.pone.0039353.
- [110] SF Son, AJ Zakrajsek, EJ Miklaszewski, DE Kittell, JL Wagner, and DR Guildenbecher. *Experimental investigation of blast mitigation for target protection*, pages 1–19. Springer, 2014.
- [111] T Rahimzadeh, EM Arruda, and MD Thouless. Design of armor for protection against blast and impact. *Journal of the Mechanics and Physics of Solids*, 85: 98–111, 2015. Available from: doi:10.1016/j.jmps.2015.09.009.
- [112] AHS Holbourn. Mechanics of head injuries. *The Lancet*, 242(6267):438–441, 1943. Available from: doi:10.1016/S0140-6736(00)87453-X.
- [113] *The scientific papers of Sir Geoffrey Ingram Taylor: Volume 3*, chapter The pressure and impulse of submarine explosion waves on plates, pages 287–303. Cambridge University Press, 1963.
- [114] GJ Cooper, BP Pearce, Ce Kenward, D Townend, et al. Augmentation by foam materials of lung injury produced by blast waves. the role of stress waves in thoracic visceral injury at high rates of energy transfer. In *Proceedings of the International Research Council on the Biomechanics of Injury conference*, volume 17, pages 123–138, 1989. Available from: http://www.ircobi.org/wordpress/downloads/irc1989/pdf_files/1989_11.pdf [Accessed 11-05-2020].
- [115] P Dearden. New blast weapons. *Journal of the Royal Army Medical Corps*, 147(1): 80–86, 2001. Available from: doi:10.1136/jramc-147-01-08.

- [116] HNG Wadley, KP Dharmasena, M He, RM McMeeking, AG Evans, T Bui-Thanh, and R Radovitzky. An active concept for limiting injuries caused by air blasts. *International Journal of Impact Engineering*, 37(3):317–323, 2010. Available from: doi:10.1016/j.ijimpeng.2009.06.006.
- [117] J Hopkinson. *Original Papers by the Late John Hopkinson Volume 1: Technical papers*. Cambridge University Press, 1901.
- [118] B Hopkinson. The effects of momentary stresses in metals. In *Proceedings of the Royal Society of London*, volume 74, pages 498–506. The Royal Society, 1904. Available from: doi:10.1098/rspl.1904.0145.
- [119] H Kolsky. An investigation of the mechanical properties of materials at very high rates of loading. *Proceedings of the Physical Society. Section B*, 62(11):676, 1949. Available from: doi:10.1088/0370-1301/62/11/302.
- [120] BA Gama, SL Lopatnikov, and JW Gillespie Jr. Hopkinson bar experimental technique: a critical review. *Applied Mechanical Reviews*, 57(4):223–249, 2004. Available from: doi:10.1115/1.1704626.
- [121] WW Chen and Bo Song. *Split Hopkinson (Kolsky) bar: design, testing and applications*. Springer Science & Business Media, 2010.
- [122] SE Rigby, AD Barr, and M Clayton. A review of Pochhammer-Chree dispersion in the Hopkinson bar. *Proceedings of the Institution of Civil Engineers-Engineering and Computational Mechanics*, pages 1–11, 2017. Available from: doi:10.1680/jencm.16.00027.
- [123] RM Davies. A critical study of the Hopkinson pressure bar. *Philosophical Transactions of the Royal Society of London. Series A, Mathematical and Physical Sciences*, 240(821):375–457, 1948. Available from: doi:10.1098/rsta.1948.0001.
- [124] *ASM Handbook Volume 8: Mechanical Testing and Evaluation*, chapter Classic split Hopkinson pressure bar testing, pages 462–476. ASM International, 2000.

- [125] VA Phadnis, P Kumar, A Shukla, A Roy, and VV Silberschmidt. Blast response of curved carbon/epoxy composite panels: Experimental study and finite-element analysis. In *Journal of Physics: Conference Series*, volume 451, page 012018. IOP Publishing, 2013. Available from: doi:10.1088/1742-6596/451/1/012018.
- [126] R Brun. Shock tubes and shock tunnels: design and experiments. Technical Report ADA568015, Aix-Marseille University, 2009.
- [127] DW Holder. A note on shock tubes. Technical Report 14879, National Physical Laboratory, 1953.
- [128] AG Gaydon and IR Hurle. The shock tube in high-temperature chemical physics. *Journal of Chemical Education*, 41(2):114, 1964. Available from: doi:10.1021/ed041p114.3.
- [129] JN Bradley. *Shock waves in chemistry and physics*. Methuen, 1962.
- [130] CC Combs. *Terrorism in the twenty-first century*. Routledge, 2017.
- [131] J Boutillier, C Deck, P Magnan, P Naz, and R Willinger. A critical literature review on primary blast thorax injury and their outcomes. *Journal of Trauma and Acute Care Surgery*, 81(2):371–379, 2016. Available from: doi:10.1097/TA.0000000000001076.
- [132] AE Ritenour, LH Blackbourne, JF Kelly, DF McLaughlin, LA Pearse, JB Holcomb, and CE Wade. Incidence of primary blast injury in us military overseas contingency operations: a retrospective study. *Annals of Surgery*, 251(6):1140–1144, 2010. Available from: doi:10.1097/SLA.0b013e3181e01270.
- [133] National Consortium for the Study of Terrorism and Responses to Terrorism (START). Global terrorism database, 2017. Available from: <https://www.start.umd.edu/gtd> [Accessed 11-05-2020].
- [134] JL Arnold, P Halpern, M Tsai, and H Smithline. Mass casualty terrorist bombings:

- a comparison of outcomes by bombing type. *Annals of Emergency Medicine*, 43(2): 263–273, 2004. Available from: doi:10.1016/S0196-0644(03)00723-6.
- [135] DS Edwards, L McMenemy, SA Stapley, HDL Patel, and JC Clasper. 40 years of terrorist bombings—a meta-analysis of the casualty and injury profile. *International Journal of the Care of the Injured*, 47(3):646–652, 2016. Available from: doi:10.1016/j.injury.2015.12.021.
- [136] A Liberati, DG Altman, J Tetzlaff, C Mulrow, PC Gøtzsche, JPA Ioannidis, M Clarke, PIJ Devereaux, J Kleijnen, and D Moher. The PRISMA statement for reporting systematic reviews and meta-analyses of studies that evaluate health care interventions: explanation and elaboration. *Journal of Clinical Epidemiology*, 62(10):e1–e34, 2009. Available from: doi:10.1016/j.jclinepi.2009.06.006.
- [137] Institute for Economics & Peace. Global Terrorism Index 2017: Measuring and understanding the impact of terrorism, 2017. Available from: <http://economicsandpeace.org/reports> [Accessed 11-05-2020].
- [138] M Smith and SM Zeigler. Terrorism before and after 9/11—a more dangerous world? *Research and Politics*, 4(4):2053168017739757, 2017. Available from: doi:10.1177/2053168017739757.
- [139] C O’Neill, AM Robinson, and S Ingleton. Mitigating the effects of firebomb and blast attacks on metro systems. *Procedia-Social and Behavioral Sciences*, 48:3518–3527, 2012. Available from: doi:10.1016/j.sbspro.2012.06.1315.
- [140] HDL Patel, S Dryden, A Gupta, and SC Ang. Pattern and mechanism of traumatic limb amputations after explosive blast: experience from the 07/07/05 London terrorist bombings. *Journal of Trauma and Acute Care Surgery*, 73(1):276–281, 2012. Available from: doi:10.1097/TA.0b013e318256dd80.
- [141] DE Hogan, JF Waeckerle, DJ Dire, and SR Lillibridge. Emergency department impact of the Oklahoma City terrorist bombing. *Annals of Emergency Medicine*, 34(2):160–167, 1999. Available from: doi:10.1016/S0196-0644(99)70224-6.

- [142] WA Odhiambo, SW Guthua, FG Macigo, and MK Akama. Maxillofacial injuries caused by terrorist bomb attack in Nairobi, Kenya. *International Journal of Oral and Maxillofacial Surgery*, 31(4):374–377, 2002. Available from: doi:10.1054/ijom.2001.0199.
- [143] R Persaud, D Hajioff, M Wareing, and E Chevetton. Otological trauma resulting from the Soho nail bomb in London, April 1999. *Clinical Otolaryngology*, 28(3):203–206, 2003. Available from: doi:10.1046/j.1365-2273.2003.00688.x.
- [144] Ü Rodoplu, JL Arnold, R Tokyay, G Ersoy, S Cetiner, and T Yücel. Impact of the terrorist bombings of the Neve Shalom and Beth Israel Synagogues on a hospital in Istanbul, Turkey. *Academic Emergency Medicine*, 12(2):135–141, 2005. Available from: doi:10.1111/j.1553-2712.2005.tb00851.x.
- [145] Ü Rodoplu, JL Arnold, T Yücel, R Tokyay, G Ersoy, and S Cetiner. Impact of the terrorist bombings of the Hong Kong Shanghai Bank Corporation headquarters and the British Consulate on two hospitals in Istanbul, Turkey, in November 2003. *Journal of Trauma and Acute Care Surgery*, 59(1):195–201, 2005. Available from: doi:10.1097/01.TA.0000171527.49354.8F.
- [146] AK Singh, E Goralnick, G Velmahos, PD Biddinger, J Gates, and A Sodickson. Radiologic features of injuries from the Boston Marathon bombing at three hospitals. *American Journal of Roentgenology*, 203(2):235–239, 2014. Available from: doi:10.2214/AJR.14.12549.
- [147] F Turégano-Fuentes, P Caba-Doussoux, JM Jover-Navalón, E Martín-Pérez, D Fernández-Luengas, L Díez-Valladares, D Perez-Diaz, P Yuste-Garcia, HG Labajo, R Rios-Blanco, et al. Injury patterns from major urban terrorist bombings in trains: the Madrid experience. *World Journal of Surgery*, 32(6):1168–1175, 2008. Available from: doi:10.1007/s00268-008-9557-1.
- [148] RM Walsh, JP Pracy, AM Huggon, and MJ Gleeson. Bomb blast injuries to the

- ear: the London Bridge incident series. *Emergency Medicine Journal*, 12(3):194–198, 1995. Available from: doi:10.1136/emj.12.3.194.
- [149] C Yazgan and NM Aksu. Imaging features of blast injuries: experience from 2015 Ankara bombing in Turkey. *The British Journal of Radiology*, 89(1062):20160063, 2016. Available from: doi:10.1259/bjr.20160063.
- [150] H Zafar, R Rehmani, T Chawla, M Umer, et al. Suicidal bus bombing of French nationals in Pakistan: physical injuries and management of survivors. *European Journal of Emergency Medicine*, 12(4):163–167, 2005. Available from: doi:10.1097/00063110-200508000-00004.
- [151] S Lohr. *Sampling: design and analysis*. Nelson Education, 2009.
- [152] JM Wilgeroth, T-TN Nguyen, and WG Proud. Interaction between blast wave and reticulated foam: assessing the potential for auditory protection systems. In *Journal of Physics: Conference Series*, volume 500, page 102005. IOP Publishing, 2014. Available from: doi:10.1088/1742-6596/500/10/102005.
- [153] Acoustafoam Ltd., 2020. Available from: <http://www.acoustafoam.com> [Accessed 11-05-2020].
- [154] H Haugen, J Will, A Köhler, U Hopfner, J Aigner, and E Wintermantel. Ceramic TiO₂-foams: characterisation of a potential scaffold. *Journal of the European Ceramic Society*, 24(4):661–668, 2004. Available from: doi:10.1016/S0955-2219(03)00255-3.
- [155] A Jillavenkatesa, SJ Dapkunas, and LH Lum. *Particle size characterization*, volume 960. National Institute of Standards and Technology, 2001.
- [156] L Bardella, A Sfreddo, C Ventura, M Porfiri, and N Gupta. A critical evaluation of micromechanical models for syntactic foams. *Mechanics of Materials*, 50:53–69, 2012. Available from: doi:10.1016/j.mechmat.2012.02.008.

- [157] MW Tibbitt and KS Anseth. Hydrogels as extracellular matrix mimics for 3D cell culture. *Biotechnology and Bioengineering*, 103(4):655–663, 2009. Available from: doi:10.1002/bit.22361.
- [158] KY Lee and DJ Mooney. Hydrogels for tissue engineering. *Chemical Reviews*, 101(7):1869–1880, 2001. Available from: doi:10.1021/cr000108x.
- [159] J Narayanan, J Xiong, and X Liu. Determination of agarose gel pore size: Absorbance measurements vis a vis other techniques. In *Journal of Physics: Conference Series*, volume 28, page 83. IOP Publishing, 2006. Available from: doi:10.1088/1742-6596/28/1/017.
- [160] N Russ, BI Zielbauer, K Koynov, and TA Vilgis. Influence of nongelling hydrocolloids on the gelation of agarose. *Biomacromolecules*, 14(11):4116–4124, 2013. Available from: doi:10.1021/bm4012776.
- [161] Y Mu, A Lyddiatt, and AW Pacek. Manufacture by water/oil emulsification of porous agarose beads: effect of processing conditions on mean particle size, size distribution and mechanical properties. *Chemical Engineering and Processing: Process Intensification*, 44(10):1157–1166, 2005. Available from: doi:10.1016/j.cep.2005.03.005.
- [162] J Breeze, LS Allanson-Bailey, AE Hepper, and MJ Midwinter. Demonstrating the effectiveness of body armour: a pilot prospective computerised surface wound mapping trial performed at the Role 3 hospital in Afghanistan. *Journal of the Royal Army Medical Corps*, 161(1):36–41, 2015. Available from: doi:10.1136/jramc-2014-000249.
- [163] J Breeze, CH Watson, I Horsfall, and JC Clasper. Comparing the comfort and potential military performance restriction of neck collars from the body armor of six different countries. *Military Medicine*, 176(11):1274–1277, 2011. Available from: doi:10.7205/MILMED-D-11-00014.

- [164] J Breeze, I Horsfall, A Hepper, and J Clasper. Face, neck, and eye protection: adapting body armour to counter the changing patterns of injuries on the battlefield. *British Journal of Oral and Maxillofacial Surgery*, 49(8):602–606, 2011. Available from: doi:10.1016/j.bjoms.2010.10.001.
- [165] M Helliker, D Carr, C Lankester, L Fenton, L Girvan, and I Horsfall. Effect of domestic laundering on the fragment protective performance of fabrics used in personal protection. *Textile Research Journal*, 84(12):1298–1306, 2014. Available from: doi:10.1177/0040517513512400.
- [166] L Cannon. Behind armour blunt trauma-an emerging problem. *Journal of the Royal Army Medical Corps*, 147(1):87–96, 2001. Available from: doi:10.1136/jramc-147-01-09.
- [167] MB Mukasey, JL Sedgwick, and DW Hagy. Ballistic resistance of body armor, NIJ Standard-0101.06. Technical Report NCJ 223054, National Institute of Justice, 2008.
- [168] DJ Carr, T Stevenson, and PF Mahoney. The use of gelatine in wound ballistics research. *International Journal of Legal Medicine*, 132(6):1659–1664, 2018. Available from: doi:10.1007/s00414-018-1831-7.
- [169] T-TN Nguyen, GR Tear, SD Masouros, and WG Proud. Fragment penetrating injury to long bones. In *AIP Conference Proceedings*, volume 1979, page 090011. AIP Publishing, 2018. Available from: doi:10.1063/1.5044868.
- [170] NR Maiden. *The assessment of bullet wound trauma dynamics and the potential role of anatomical models*. PhD thesis, University of Adelaide, 2014.
- [171] M Fackler and J Malinowski. The wound profile: a visual method for quantifying gunshot wound components. *Journal of Trauma and Acute Care Surgery*, 25(6):522–529, 1985. Available from: <https://www.ncbi.nlm.nih.gov/pubmed/4009751> [Accessed 11-05-2020].

- [172] A Mabbott, DJ Carr, S Champion, and C Malbon. Comparison of porcine thorax to gelatine blocks for wound ballistics studies. *International Journal of Legal Medicine*, 130(5):1353–1362, 2016. Available from: doi:10.1007/s00414-015-1309-9.
- [173] J Breeze, N Hunt, I Gibb, G James, A Hepper, and J Clasper. Experimental penetration of fragment simulating projectiles into porcine tissues compared with simulants. *Journal of Forensic and Legal Medicine*, 20(4):296–299, 2013. Available from: doi:10.1016/j.jflm.2012.12.007.
- [174] DJ Chapman. Design of shock tube facility. Technical report, Imperial College London, 2010.
- [175] DR Sory. *Dynamic Loading of Periosteum-derived Mesenchymal Stromal Cells*. PhD thesis, Imperial College London, 2018.
- [176] RN Trubshaw. Resonance and signal conditioning in instrumented impact testing. *Polymer Testing*, 6(5):387–401, 1986. Available from: doi:10.1016/0142-9418(86)90044-9.
- [177] GT Gray III and WR Blumenthal. *ASM Handbook Volume 8: Mechanical Testing and Evaluation*, chapter Split-Hopkinson pressure bar testing of soft materials, pages 488–496. ASM International, 2000.
- [178] D Casem, T Weerasooriya, and P Moy. Inertial effects of quartz force transducers embedded in a split Hopkinson pressure bar. *Experimental Mechanics*, 45(4):368, 2005. Available from: doi:10.1007/BF02428167.
- [179] M Prot and TJ Cloete. A tandem momentum trap for dynamic specimen recovery during split Hopkinson pressure bar testing of cancellous bone. *Journal of Dynamic Behavior of Materials*, 2(1):50–58, 2016. Available from: doi:10.1007/s40870-016-0046-6.
- [180] J Blaber, B Adair, and A Antoniou. Ncorr: open-source 2D digital image correlation matlab software. *Experimental Mechanics*, 55(6):1105–1122, 2015. Available from: doi:10.1007/s11340-015-0009-1.

- [181] Y Yu, Y Wang, W Dong, Y Jin, and J Ou. Strain properties analysis and wireless collection system of PVDF for structural local health monitoring of civil engineering structures. In *Second International Conference on Smart Materials and Nanotechnology in Engineering*, volume 7493, page 74931G. International Society for Optics and Photonics, 2009. Available from: doi:10.1117/12.838709.
- [182] Y Meng and W Yi. Application of a PVDF-based stress gauge in determining dynamic stress-strain curves of concrete under impact testing. *Smart Materials and Structures*, 20(6):065004, 2011. Available from: doi:10.1088/0964-1726/20/6/065004.
- [183] F Bauer. PVDF shock compression sensors in shock wave physics. In *AIP Conference Proceedings*, volume 706, pages 1121–1124. AIP, 2004. Available from: doi:10.1063/1.1780435.
- [184] S Ouellet, D Cronin, and M Worswick. Compressive response of polymeric foams under quasi-static, medium and high strain rate conditions. *Polymer Testing*, 25(6):731–743, 2006. Available from: doi:10.1016/j.polymertesting.2006.05.005.
- [185] VC Shunmugasamy, N Gupta, NQ Nguyen, and PG Coelho. Strain rate dependence of damage evolution in syntactic foams. *Materials Science and Engineering: A*, 527(23):6166–6177, 2010. Available from: doi:10.1016/j.msea.2010.06.021.
- [186] JP Caillou, M Dannawi, L Dubar, and Ch Wielgosz. Dynamic behaviour of a gelatine 20% material numerical simulation. In *Proceedings of the 2nd Personal Armour System Symposium*, pages 325–331. International Personal Armour Committee, 1994.
- [187] TF Juliano, AM Forster, PL Drzal, T Weerasooriya, P Moy, and MR VanLandingham. Multiscale mechanical characterization of biomimetic physically associating gels. *Journal of Materials Research*, 21(8):2084–2092, 2006. Available from: doi:10.1557/jmr.2006.0254.

- [188] L Koene, JL Barou, and P Viot. Ballistic gelatine behaviour under quasi-static and dynamic loadings. In *Proceedings of the 26th International Symposium on Ballistics*, volume 26, 2011.
- [189] J Kwon and G Subhash. Compressive strain rate sensitivity of ballistic gelatin. *Journal of Biomechanics*, 43(3):420–425, 2010. Available from: doi:10.1016/j.jbiomech.2009.10.008.
- [190] Y Huang, AI King, and JM Cavanaugh. Finite element modeling of gross motion of human cadavers in side impact. *SAE Transactions*, pages 1604–1622, 1994. Available from: doi:10.4271/942207.
- [191] K Furusu, I Watanabe, C Kato, K Miki, and J Hasegawa. Fundamental study of side impact analysis using the finite element model of the human thorax. *JSAE Review*, 22(2):195–199, 2001. Available from: doi:10.1016/S0389-4304(01)00084-4.
- [192] E Lizée, S Robin, E Song, N Bertholon, J Le Coz, B Besnault, and F Lavaste. Development of a 3D finite element model of the human body. *SAE Transactions*, pages 2760–2782, 1998. Available from: doi:10.4271/983152.
- [193] Y Deng, W Kong, and H Ho. Development of a finite element human thorax model for impact injury studies. *SAE Transactions*, pages 1193–1199, 1999. Available from: doi:10.4271/1999-01-0715.
- [194] C Pezowicz and M Głowacki. The mechanical properties of human ribs in young adult. *Acta of Bioengineering and Biomechanics*, 14(2):53–60, 2012. Available from: doi:10.5277/abb120207.
- [195] R Akhtar, MJ Sherratt, JK Cruickshank, and B Derby. Characterizing the elastic properties of tissues. *Materials Today*, 14(3):96–105, 2011. Available from: doi:10.1016/S1369-7021(11)70059-1.
- [196] N Ravikumar, C Noble, E Cramphorn, and ZA Taylor. A constitutive model for ballistic gelatin at surgical strain rates. *Journal of the Mechanical Behavior of Biomedical Materials*, 47:87–94, 2015. Available from: doi:10.1016/j.jmbbm.2015.03.011.

- [197] SS Naarayan and G Subhash. Wave propagation in ballistic gelatine. *Journal of the Mechanical Behavior of Biomedical Materials*, 68:32–41, 2017. Available from: doi:10.1016/j.jmbbm.2017.01.030.
- [198] RKA Pasumarthi and HV Tippur. Mechanical and optical characterization of a tissue surrogate polymer gel. *Polymer Testing*, 55:219–229, 2016. Available from: doi:10.1016/j.polymertesting.2016.08.004.
- [199] AE Forte, F D’Amico, MN Charalambides, D Dini, and JG Williams. Modelling and experimental characterisation of the rate dependent fracture properties of gelatine gels. *Food Hydrocolloids*, 46:180–190, 2015. Available from: doi:10.1016/j.foodhyd.2014.12.028.
- [200] JM Varas, MMGM Philippens, SR Meijer, AC Van Den Berg, PC Sibma, JLMJ Van Bree, and DVWM De Vries. Physics of IED blast shock tube simulations for mTBI research. *Frontiers in Neurology*, 2:58, 2011. Available from: doi:10.3389/fneur.2011.00058.
- [201] JC Roberts, EE Ward, TP Harrigan, TM Taylor, MA Annett, and AC Merkle. Development of a human head-neck computational model for assessing blast injury. In *ASME 2009 International Mechanical Engineering Congress and Exposition*, pages 95–96. ASME Digital Collection, 2009. Available from: doi:10.1115/IMECE2009-11813.
- [202] *Abaqus 6.13 Documentation*. Dassault Systèmes, 2013.
- [203] C Mougeotte, P Carlucci, S Recchia, and H Ji. Novel approach to conducting blast load analyses using Abaqus/Explicit-CEL. Technical report, Army Armament Research Development and Engineering Center, 2010.
- [204] J Jablonski, P Carlucci, R Thyagarajan, B Nandi, and J Arata. Simulating underbelly blast events using Abaqus/Explicit-CEL. Technical report, Dassault Systèmes, 2013.

- [205] MV Grimm, KH Muci-Küchler, BJ Hinz, and SM Walsh. Comparison of numerical and experimental results of small scale compressed gas blast experiments involving a surrogate head form. In *ASME 2012 International Mechanical Engineering Congress and Exposition*, pages 759–770. American Society of Mechanical Engineers Digital Collection, 2012. Available from: doi:10.1115/IMECE2012-87663.
- [206] J Zhou. *Experimental and numerical investigation of soft impact loading on aircraft materials*. PhD thesis, 2017.
- [207] Y Wen, C Xu, S Wang, and RC Batra. Analysis of behind the armor ballistic trauma. *Journal of the Mechanical Behavior of Biomedical Materials*, 45:11–21, 2015. Available from: doi:10.1016/j.jmbbm.2015.01.010.
- [208] L Zhang, Q Fang, Y Mao, and L Chen. Blast mitigation effects of water walls: numerical simulation and analytical approach. *International Journal of Protective Structures*, 6(3):551–565, 2015. Available from: doi:10.1260/2041-4196.6.3.551.
- [209] T-TN Nguyen and WG Proud. An investigation of a reticulated foam-perforated steel sheet combination as a blast mitigation structure. In *AIP Conference Proceedings*, volume 1793, page 120008. AIP Publishing, 2017. Available from: doi:10.1063/1.4971690.
- [210] G Mazar. *The influence of surface properties on the head on reflection of shock waves*. PhD thesis, Ben-Gurion University, 1989.
- [211] KA Marchand, CJ Oswald, and MA Polcyn. Testing and analysis done in support of the development of a container for on-site weapon demilitarization. Technical report, Southwest Research Institute, 1996.
- [212] CE Joachim and CV Lunderman. Blast suppression with water-results of small-scale test program. In *Proceedings of the 15th International Symposium on the Military Aspects of Blast and Shock*, pages 1–9, 1997. Available from: <https://mabs.tcnet.ch/data/documents/15-06.pdf> [Accessed 11-05-2020].

- [213] H Bornstein, P Phillips, and C Anderson. Evaluation of the blast mitigating effects of fluid containers. *International Journal of Impact Engineering*, 75:222–228, 2015. Available from: doi:10.1016/j.ijimpeng.2014.08.014.
- [214] MW Courtney and AC Courtney. Working toward exposure thresholds for blast-induced traumatic brain injury: thoracic and acceleration mechanisms. *NeuroImage*, 54:S55–S61, 2011. Available from: doi:10.1016/j.neuroimage.2010.05.025.
- [215] L Liu, Y Fan, and W Li. Viscoelastic shock wave in ballistic gelatin behind soft body armor. *Journal of the Mechanical Behavior of Biomedical Materials*, 34:199–207, 2014. Available from: doi:10.1016/j.jmbbm.2014.02.011.
- [216] PC Nolin. Countering the afghan insurgency: Low-tech threats, high-tech solutions. Technical Report 189 STC 11 E, 2011.

Appendices

A Allocation of GTD attack settings

In order to distinguish between different attack environments in the epidemiological study of terrorist blast incidents (Chapter 2), the settings defined in the GTD were manually allocated to the following categories: ‘Train’, ‘bus’, ‘Semi-Confined Space’ (SCS), ‘Open Space’ (OS) and ‘Other’. This allocation is shown in Table A.1.

Attack Setting	GTD Settings
Train	‘Train/Train Tracks/Trolley’, ‘Subway’
Bus	‘Tour Bus/Van/Vehicle’, ‘Bus (excluding tourists)’, ‘Taxi/Rickshaw’
Semi-Confined Space (SCS)	‘Gas/Oil’, ‘Restaurant/Bar/Café’, ‘Bank/Commerce’, ‘Multinational Corporation’, ‘Industrial/Textiles/Factory’, ‘Medical/Pharmaceutical’, ‘Retail/Grocery/Bakery’, ‘Hotel/Resort’, ‘Farm/Ranch’, ‘Mining’, ‘Entertainment/Cultural/Stadium/Casino’, ‘Construction’, ‘Private Security Company/Firm’, ‘Government Buildings/Facility/Office’, ‘Clinics’, ‘Airport’, ‘Embassy/Consulate’, ‘School/University/Educational Building’, ‘Civilian Maritime’, ‘Commercial Maritime’, ‘Oil Tanker’, ‘Refugee Camp’, ‘Vehicles/Transportation’, ‘Marketplace/Plaza/Square’, ‘House/Apartment/Residence’, ‘Public Areas (<i>e.g.</i> , Public garden, parking lot, garage, beach, public buildings, camps)’, ‘Memorial/Cemetery/Monument’, ‘Museum/Cultural Center/Cultural House’, ‘Tourism Travel Agency’, ‘Bus Station/Stop’, ‘Party Office/Facility’
Open Space (OS)	‘Port’, ‘Village/City/Town/Suburb’, ‘Bridge/Car Tunnel’, ‘Highway/Road/Toll/Traffic Signal’
Other	‘Judges/Attorneys/Courts’, ‘Politician or Political Party Movement/Meeting/Rally’, ‘Royalty’, ‘Head of State’, ‘Government Personnel (excluding police, military)’, ‘Election-related’, ‘Intelligence’, ‘Personnel’, ‘Airline Officer/Personnel’, ‘Diplomatic Personnel’, ‘International Organization (peacekeeper, aid agency, compound)’, ‘Teacher/Professor/Instructor’, ‘Other Personnel’, ‘Food Supply’, ‘Water Supply’, ‘Newspaper Journalist/Staff/Facility’, ‘Radio Journalist/Staff/Facility’, ‘Television Journalist/Staff/Facility’, ‘Other (including online news agencies)’, ‘Domestic NGO’, ‘International NGO’, ‘Ambulance’, ‘Fire Fighter/Truck’, ‘Demilitarized Zone (including Greez Zone)’, ‘Unnamed Civilian/Unspecified’, ‘Named Civilian’, ‘Religion Identified’, ‘Student’, ‘Race/Ethnicity Identified’, ‘farmer’, ‘Laborer (General)/Occupation Identified’, ‘Procession/Gathering (funeral, wedding, birthday, religious)’, ‘Labor Union Related’, ‘Protester’, ‘Political Party Member/Rally’, ‘Religious Figure’, ‘Place of Worship’, ‘Affiliated Institution’, ‘Radio’, ‘Television’, ‘Telephone/Telegraph’, ‘Internet Infrastructure’, ‘Multiple Telecommunication Targets’, ‘Terrorist Organization’, ‘Non-State Militia’, ‘Tourist’, ‘Other Facility’, ‘Gas’, ‘Electricity’, ‘Oil’, ‘Party Official/Candidate/Other Personnel’, ‘Rally’

Table A.1: Allocation of GTD attack environment for epidemiological study (Chapter 2).

B Statistical significance of blast injury rates on attack setting

In the epidemiological study of Chapter 2, a central research question was whether blast injury has a significant dependence on attack setting. The null hypothesis states that there is no significant difference in injury rates across attack settings. In Table 3.6, non-overlapping CIs imply that the null hypothesis can be rejected, but the opposite is not necessarily true; significant difference can exist between two means with overlapping CIs. To correctly test the null hypothesis, the difference between the two $\hat{p}$ values must be computed with a corresponding 95% CI:

$$|\hat{p}_1 - \hat{p}_2| \pm t_{1-\alpha/2}(dof)\sigma_p\sqrt{\frac{1}{n_1} + \frac{1}{n_2}},$$

where the subscripts 1 and 2 denote the two settings under comparison. For the two-tailed t -distribution, the significance level was $\alpha = 0.05$ and the degrees of freedom given by $dof = n_1 + n_2 - 2$. The pooled estimate of the common standard deviation, σ_p , for the two settings was given by:

$$\sigma_p = \sqrt{\frac{(n_1 - 1)\hat{V}(\hat{p}_1) + (n_2 - 1)\hat{V}(\hat{p}_2)}{n_1 + n_2 - 2}}.$$

The null hypothesis was rejected if the resulting range did not include zero. The results of this calculation are shown in Table B.1 for the different permutations of attack setting.

Injury	$ \hat{p}_1 - \hat{p}_2 (\pm \times 10^{-4} \text{ 95\% CI})$					
	Train-OS	Train-Bus	Train-SCS	SCS-OS	SCS-Bus	Bus-OS
BLI	0.05 (0.05)	0.09 (0.16)	0.02 (0.06)	0.04 (0.06)	0.11 (0.16)	0.15 (0.17)
TMP	0.35 (0.58)	0.17 (0.59)	0.05 (0.39)	0.29 (0.24)	0.11 (0.22)	0.18 (0.41)
Intra-abdominal Rupture	0.04 (0.05)	0.01 (0.27)	0.01 (0.18)	0.06 (0.31)	0.00 (0.61)	0.06 (0.49)
Penetrating Torso	0.20 (1.22)	0.19 (1.35)	0.15 (0.30)	0.06 (1.86)	0.04 (2.09)	0.02 (4.34)
Penetrating Head, Neck and Face	0.20 (1.22)	0.50 (1.51)	0.08 (0.18)	0.12 (1.24)	0.42 (1.55)	0.30 (4.52)
Penetrating Extremity	0.25 (2.02)	0.21 (1.60)	0.26 (0.75)	0.01 (4.28)	0.05 (3.56)	0.04 (7.06)
Skull Fracture	0.04 (0.24)	0.09 (0.68)	0.05 (0.07)	0.01 (0.30)	0.04 (0.76)	0.05 (1.64)
Other Fracture	0.17 (0.43)	0.27 (0.14)	0.10 (0.16)	0.07 (0.44)	0.17 (0.17)	0.10 (0.49)
Traumatic Amputation	0.07 (0.55)	0.07 (0.27)	0.05 (0.45)	0.03 (0.28)	0.02 (0.13)	0.01 (0.01)
Burns	0.10 (0.31)	0.01 (0.19)	0.12 (0.31)	0.22 (0.63)	0.12 (0.41)	0.09 (0.45)

Table B.1: Statistical significance tests for injury rates across different attack settings.

It can be seen from Table B.1 that the lower limit of the difference remains positive in all cases. Therefore, the injury rates have a significant dependence on the attack environment.

C Casting Equipment

C.1 Material Characterisation Sample Moulds

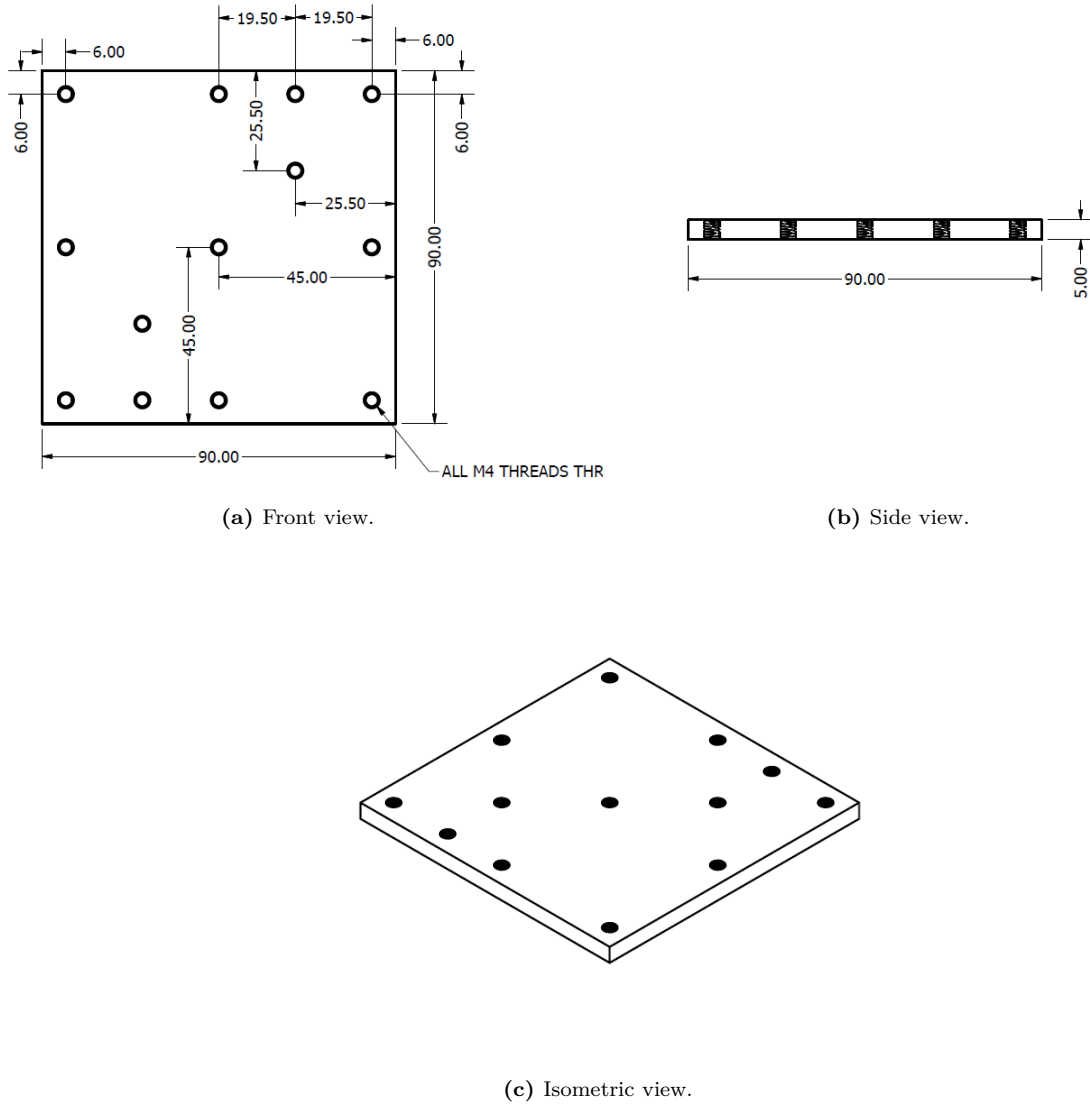


Figure C.1: CAD drawing of mould back plate used for manufacturing samples for material characterisation. This was bolted to the front plate in Figure C.2 with M4 bolts. Parts were made of acetal for ease of demoulding polyurethane and hydrogel materials. All units in mm.

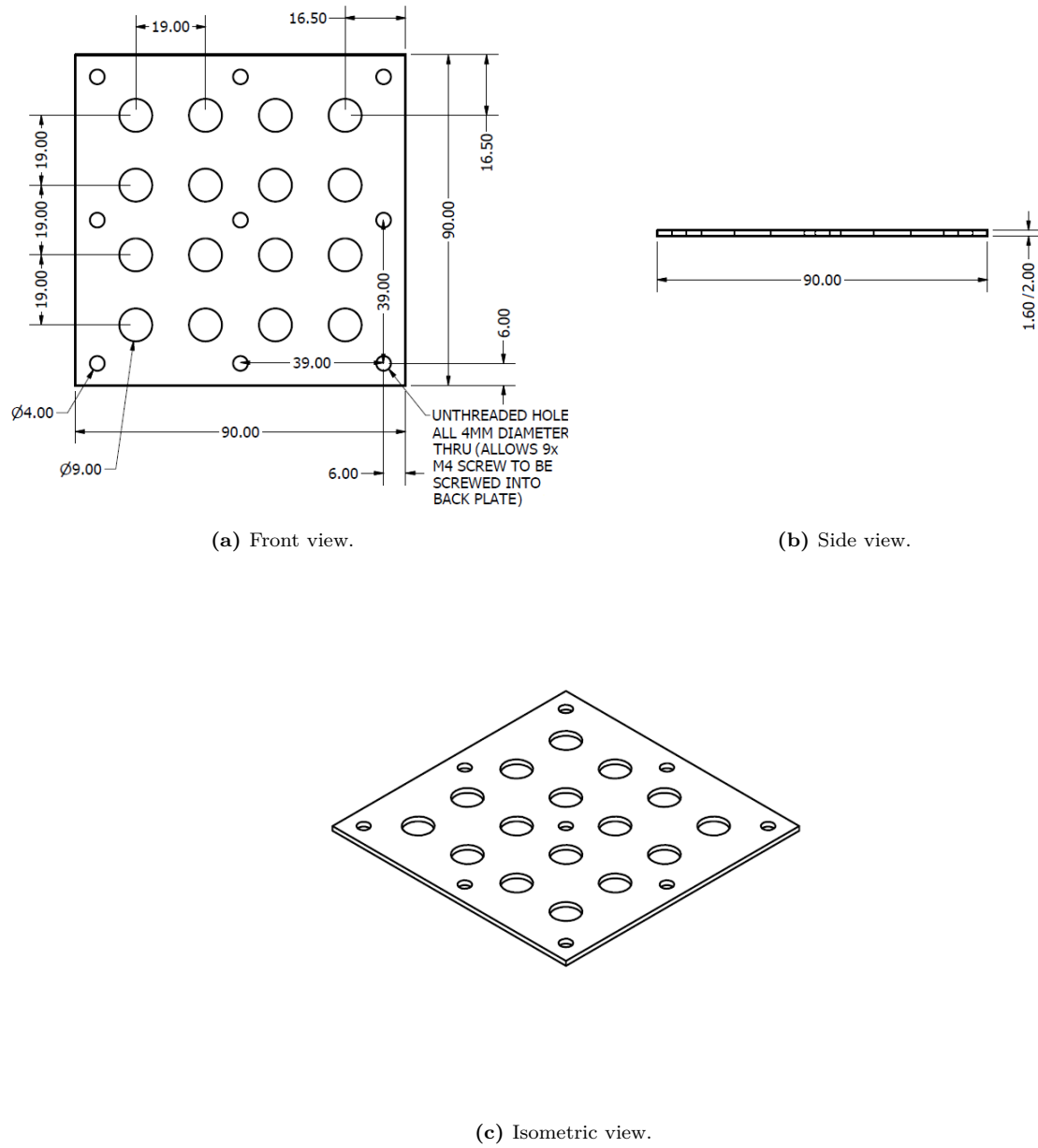
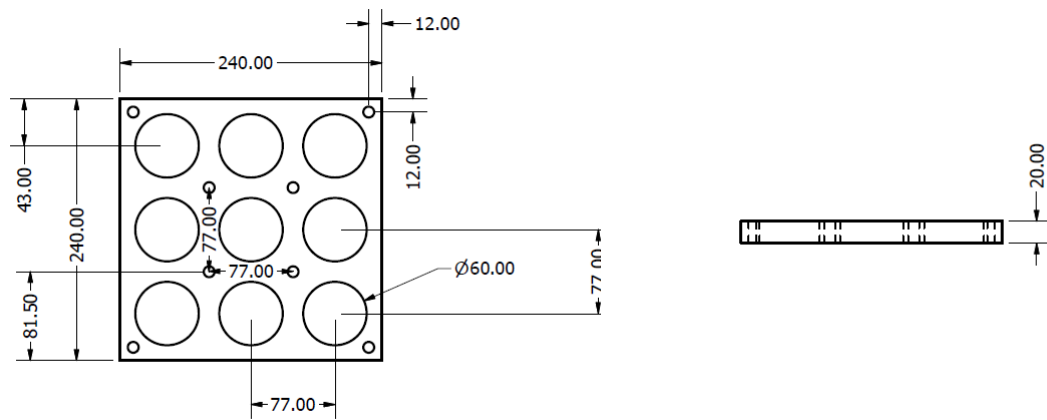


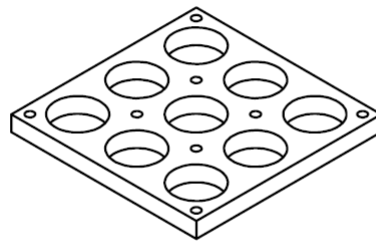
Figure C.2: CAD drawing of mould front plate used for manufacturing samples for material characterisation. Two moulds, 1.6 mm and 2.0 mm thick, were made to manufacture samples appropriate for both drop-weight and SHPB testing. This part was bolted to the back plate in Figure C.1 with M4 bolts. Parts were made of acetal for ease of demoulding polyurethane and hydrogel materials. Following vacuum degassing, polycarbonate covers were placed on the top of the assembly to seal the material during curing. All units in mm.

C.2 Shock Tube Sample Moulds



(a) Front view.

(b) Side view.



(c) Isometric view.

Figure C.3: CAD drawing of mould front plate used for manufacturing samples for shock tube shock enhancement experiments. This part was bolted to a back plate plate analogous to Figure C.1 and a polycarbonate front plate with M10 bolts, sealing the assembly for sample curing. All units in mm.

D Permissions

For non-public domain or open access content (*e.g.* Creative Commons license), permissions for reproduction of figures used in this work were obtained from the rightsholders.

Copyright license details are shown in Tables D.1 and D.2.

Language	Original language
Type of Use	Republish in a thesis/dissertation
Portion Item	Image/Photo/Illustration
Format	Print, Electronic
Republished By	Academic Institution (Imperial College London)
Distribution	Worldwide
Duration of Use	Life of Current and all future editions
Rights Requested	Main Product
Requesting Person	Danyal Magnus

Table D.1: Copyright license details

Licensed Content			Request Details		Thesis Reference
License ID	Type	Holder	Ordered	Items	
1010558-1	Journal	ICE	25-12-19	2	Figure 2.2 [22]
1011802-1	Journal	Elsevier	04-01-20	1	Figure 2.4 [38]
1011803-1	Journal	Elsevier	04-01-20	2	Figure 2.11 [77]
4741911464505	eBook	Springer	04-01-20	2	Figures 2.12 and 2.13 [93]
1011804-1	Journal	Elsevier	04-01-20	1	Figure 4.1(b) [154]
1011806-1	Journal	Elsevier	04-01-20	1	Figure 6.10 [195]
1011808-1	Journal	Springer	04-01-20	1	Figure 8.4 [87]

Table D.2: Copyright licenses for reproduction of figures.