

BLAST MITIGATION AND DESIGN OF LAMINATED GLASS FACADES

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Abstract

Laminated glass is an essential component for any building designed against blast loading. A polymer interlayer is sandwiched between glass layers in order to keep the cracked glass fragments intact. In successful design, the interlayer deforms and absorbs the blast load without tearing.

A novel testing method was developed to capture the bending response of laminated glass under high rate uniform loading. Specimens with a dimension of 700 mm x 60 mm were tested. It was found that the failure mechanism of the laminated glass is controlled by the thickness of the interlayer. In addition, a new framing arrangement was tested which showed that the load on the laminated glass can be reduced by increasing the energy absorbing capacities within the frame. By allowing the frame to purposefully deform under loading, the chances of failure of the laminated glass are reduced.

Tensile tests were conducted on cracked laminated glass in the temperature range of 0°C – 60°C. Single-cracked and randomly-cracked specimens were tested. It was found that the optimum temperature for the greatest energy absorption of the cracked laminated varies between 10°C – 40°C depending on the interlayer thickness. The single-cracked results were further expanded to calculate a bond fracture toughness for the separation of the interlayer from the glass. A finite element model was developed to simulate the bond separation between the glass and the interlayer at different testing-rates and temperatures. It was found that the bond is testing-rate dependent in the range of 0.01 /s – 200 /s, but temperature independent in the range of 20°C – 60°C.

The *single-degree-of-freedom* model is conventionally used for predicting the response of a laminated glass pane subject to blast loading. The *single-degree-of-freedom* model includes mass and load transformation factors, which vary with the deflected shape of the structure. In this thesis a finite element model was used to derive the deflected shapes and transformation factors for a range of loading conditions, boundary conditions and pane aspect ratios. The time-varying deflected shape was taken into account in this analysis, as this is currently not included in other *single-degree-of-freedom* models. The transformation factors were found to be insensitive to aspect ratio. In addition, at high loading rates, the deflected shape–time history proved to be important.

Whomever claims that they have reached the peak of knowledge,
has in fact unveiled the extremity of their ignorance.

— ALI IBN ABI TALIB (601-661 AD)

Declaration of Originality

I certify that the work presented in this thesis is the result of my own investigations during the PhD project. Text and results obtained from other sources have been correctly referenced and acknowledged.

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Nomenclature

α	Power law exponent
Δl	Displacement
$\Delta P_{\min}$	Greatest negative pressure
$d\delta$	Displacement
δ_c	Separation at complete de-bonding failure
δ_i	Separation at initiation of de-bonding
ε	Strain
ε_a	Tensile strain
ε_f	Failure strain
ε_n	Nominal strain
ε_p	Plastic strain
$\dot{\varepsilon}$	Strain rate
$\dot{\varepsilon}_0$	Reference strain rate
θ	Peeling angle
θ_0	Slope of the arm at peeling front
ν	Poisson's ratio
ρ	Density
σ	Stress
τ_i	Prony series individual relaxation time constants
ϕ	Shape function
A_s	Area
a	De-bonding length
A, B, C, n	Johnson-Cook constants
$a \times b$	Dimensions of two dimensional model
b	Specimen width
b_p	Wavefront parameter
C_i	Reduced polynomials hyperelastic springs constants
D	Flexural rigidity
E	Young's modulus
F_e	Equivalent load of structure
F_t	Equivalent mass of structure
G_a	Bond fracture toughness
g_i	Prony series individual stiffness constants

G_{Ia}	Bond fracture toughness in normal direction
G_{Ia}	Energy dissipated per unit area in normal direction
G_{IIa}	Bond fracture toughness in first shear direction
G_{IIa}	Energy dissipated per unit area in first shear direction
G_{IIIa}	Bond fracture toughness in second shear direction
G_{IIIa}	Energy dissipated per unit area in second shear direction
h	Interlayer thickness
i	Impulse
I	Second moment of area
i_s	Specific impulse
k	stiffness factor
K_L	Load transformation factor
K_M	Mass transformation factor
l	Length of one dimensional model
L	Length of structure
l_{del}	Delaminated length
l_g	Gauge length
L_L	Lower delaminated length
L_U	Upper delaminated length
m	Mass per unit length
M_e	Equivalent mass of structure
M_t	Actual mass of structure
p	Pressure
P	De-bonding load
$p(x)$	Distributed load along x direction
p_{atm}	Atmospheric pressure
P_{FE}	Predicted de-bonding load from FE results
p_r	Reflected overpressure
p_s	Static overpressure
P_t	Load
q	Uniform load
R	Stand-off distance
R_t	Test strain rate
t	Time
T	Natural period of vibration of the structure
t^{max}	Maximum traction
t_a	Arrival time
t_d	Positive phase of idealised blast pressure
T_g	Glass transition temperature
t_I	Normal traction
t_I^{max}	Maximum normal traction
t_{II}	Traction in first shear direction
$t_{\text{II}}^{\text{max}}$	Maximum traction in first shear direction
t_{III}	Traction in second shear direction
$t_{\text{III}}^{\text{max}}$	Maximum traction in second shear direction

$t_{\text{Interlayer}}$	Initial interlayer thickness
t_{n}	Negative phase duration
t_{r}	Load rise time
t_{s}	Positive phase duration
T_{t}	Test temperature
U_{dt}	Energy absorbed by de-bonded ligament
U_{ext}	External work
U_{s}	Stored energy
w	Deflection
W	Mass of charge
x	Displacement
$\ddot{x}$	Acceleration
Z	Scaled distance

Acronyms and Abbreviations

BC	Boundary conditions
CFD	Computational fluid dynamics
CZM	Cohesive zone model
DIC	Digital image correlation
DMA	Dynamic mechanical analysis
EVA	Ethylene vinyl acetate
FE	Finite element
PDE	Partial differential equation
PVB	Polyvinyl butyral
SDOF	Single degree of freedom
SGP	SentryGlas plus
TNT	Trinitrofluorene
WLF	Williams-Landel-Ferry

Introduction

Protection of human life is the highest priority in any engineering design, and buildings are no different. Buildings are put to their greatest test when encountering explosions. Following an explosion, the most important factor is the safety of the building occupants and surrounding pedestrians. If protection from the explosion is the only factor for concern, then concrete shelters are the best solution. However, this will be very unappealing to the general public.

Explosions are demanding events in terms of loading of structures and damage to structures is unavoidable. When designing buildings, engineers must consider many factors. The safety of the building occupants and pedestrians must be set as the number one priority, without any compromise. However, this is not always the case because of the added cost which is associated with complications of explosion resistant designs.

Buildings are vulnerable to explosive threats particularly because of the extensive use of glass in modern facades (Fig. 1.1). Use of glass in buildings is almost unavoidable because of the need for natural light and also to create aesthetically pleasing structures. The two common types of glazing systems use monolithic glass panes or laminated glass panes. Monolithic glass panes are further split into two main categories, annealed glass and toughened glass.



Figure 1.1: Failure of a building facade subject to blast loading (courtesy of David Smith at Arup).

In annealed glass, cracking is a random and uncontrolled process. A crack initiates at sites where flaws exist in the material. Once the strength required to crack the glass at one of these flaw sites is exceeded, a crack is created. This crack spreads almost instantly throughout the material, propagating from that initial cracking site and forming glass fragments of various sizes. In toughened glass, however, the cracking mechanism is different. Toughened glass has a much greater failure strength than annealed glass because of the tempering process that it has received. A compressive stress is present on both sides of the glass. Once this compressive stress is overcome, the glass cracks into fine fragments of similar size.

Monolithic glass panes (annealed or toughened) are not an ideal solution for blast resistant design and may result in further fatalities following an explosion. The pressure pulse from the explosion causes cracking of the glass, which inevitably results in sharp glass fragments travelling at high velocities. In addition the pressure wave can enter the building, causing further damage to the interior. Anti-shatter films and net curtains can be additionally installed with monolithic systems to reduce the impact of the flying fragments – but their protective capacity is not able to match that of well-designed laminated panes.

In order to keep structures aesthetically appealing and protected

1. Introduction

simultaneously, laminated glass can be employed (Fig. 1.2). At an additional cost, this is the best alternative to monolithic glass panes. Laminated glass is a composite which consists of a polymer interlayer, usually *PolyVinyl butyral* (PVB), sandwiched between glass layers. Examples of its use include governmental buildings, high security sites, office buildings and airports. It is also used for generic safety glass.



Figure 1.2: Cracked laminated glass following a blast explosion (courtesy of David Smith at Arup).

When an explosion occurs, a pressure pulse propagates from the source of the explosion. When the pressure pulse interacts with a laminated glazing system with protective design, three components must not fail. Firstly, the laminated glass pane must be able to absorb the pressure pulse. Secondly, the pane must remain in its frame and not detach from it. Finally, the frame and its fixing must survive. The protection from the laminated glass pane depends on two requirements. When the glass cracks, its fragments must remain adhered to the interlayer. In addition the interlayer must deform and absorb the pressure load, without tearing. All the mentioned requirements are important and engineers must be aware of the failure mechanisms prior to setting out any design guidelines.

1.1. Aims and objectives

The fundamental aim of this research is to assist designers to gain a better understanding of the macro and micro behaviour of laminated glass under blast loading, and also to improve on current design guidelines. This research is primarily focused on the post-cracking phase of the glass layers in a laminated glazing system. However, the pre-cracking phase will also be briefly mentioned.

An important factor which has been overlooked in the literature is the high rate bending of laminated glass under laboratory conditions. Little or no experimental data exists of this category. In this thesis, a novel testing method is presented that allows the study of the bending behaviour of laminated glass subject to uniform loading. The objective of this study is to capture the response under laboratory conditions using high speed image recording techniques, at a fraction of the cost of a full-scale blast test. Factors such as boundary conditions, interlayer thickness and test rate have been studied.

In the post-cracking phase of response of laminated glass, two factors are important; the deformation of the PVB interlayer, and the bond between the PVB interlayer and the glass layers. PVB like all polymers is temperature and rate dependent. In the literature the rate dependence of the deformation in the post-cracking behaviour of laminated glass has extensively been covered. However, little information exists about temperature dependence in the post-cracking phase of response. In this thesis, the temperature dependence of the deformation of cracked laminated glass and its effect on the interlayer bond has been experimentally studied. In addition, a finite element (FE) model is presented to simulate the behaviour of the bond between the PVB and the glass layers, at different temperatures and testing rates.

In structural design, it is important to be able to predict failure. For this purpose, the common tool used by designers is the *single-degree-of-freedom-model* (SDOF). The SDOF is preferred to FE methods purely because it is simpler, less complex and cheaper to use than FE. In current SDOF models, for a given deformation phase (pre-crack or post-crack), the deflected shape-time history is typically ignored. In this thesis, an investigation has been carried out to predict the shape of the glass pane during the deformation period. This study has been carried out for different boundary conditions, aspect ratios and framing arrangements (one-way or

two-way spanning frames).

1.2. Thesis outline

This thesis is split into seven chapters. This chapter introduces the research, and gives an outline of the aims and objectives. Chapter 2 is a literature review and its main aim is to put this thesis into perspective. At times, the literature review is beyond what is required for this thesis, however, it is necessary to raise awareness of the reader about the bigger picture of this research topic.

In chapter 3, a new arrangement is presented for testing laminated glass under uniform loading in laboratory conditions. The testing apparatus and measuring techniques are first explained. This is followed by an analysis of the effects of interlayer thickness, glass cracking patterns and framing arrangements on the bending behaviour of the laminated glass specimen.

Chapters 4 and 5 are closely linked together. They are both based on the post-cracking response of laminated glass using through-cracked tensile specimens. The focus of chapter 4 is on the deformation of cracked laminated glass at different temperatures. Whilst chapter 5 looks into the effects of the temperature on the bond between the PVB interlayer and glass layers. In chapter 5, a FE model is also presented that simulates the bond separation behaviour between subsequent layers at different testing rates and temperatures.

Chapter 6 presents the work on the SDOF model which is used to predict response of structures. The SDOF model is dependent on a set of load and mass transformation factors which are dependent on the deflected shape of the structure. In this chapter, FE modelling techniques are used to derive values of the transformation factors for different boundary conditions, loading types, aspect ratios, framing arrangements (one-way or two-way spanning systems), and for the different deformation phases of response (pre-crack or post-crack). At specific conditions, a verification is also made against theoretical results.

Finally, in chapter 7, a conclusion is presented that brings together all the findings of the thesis as a whole. In addition, future investigation topics are also presented to allow researchers to follow up from this thesis.

Literature Review

2.1. Theory of blast loading

When an explosion occurs, a large amount of energy is suddenly released in the form of gaseous products. The gaseous products released from the blast expand almost instantaneously, leading to the volume of air surrounding the products of explosion being compressed to a pressure termed static overpressure, p_s , from the atmospheric pressure, p_{atm} . This leads to the formation of a high pressure shock wave, which travels at supersonic velocity. This wave carries all the energy released from the explosion and is sometimes referred to as the blast wave. The wave has a pressure gradient from its initial peak, decreasing to p_{atm} , followed by a negative phase below atmospheric pressure. After this suction phase, equilibrium is finally established [2]. This is also illustrated by the blast wave pressure-time ($p(t) - t$) trace in Fig. 2.1.

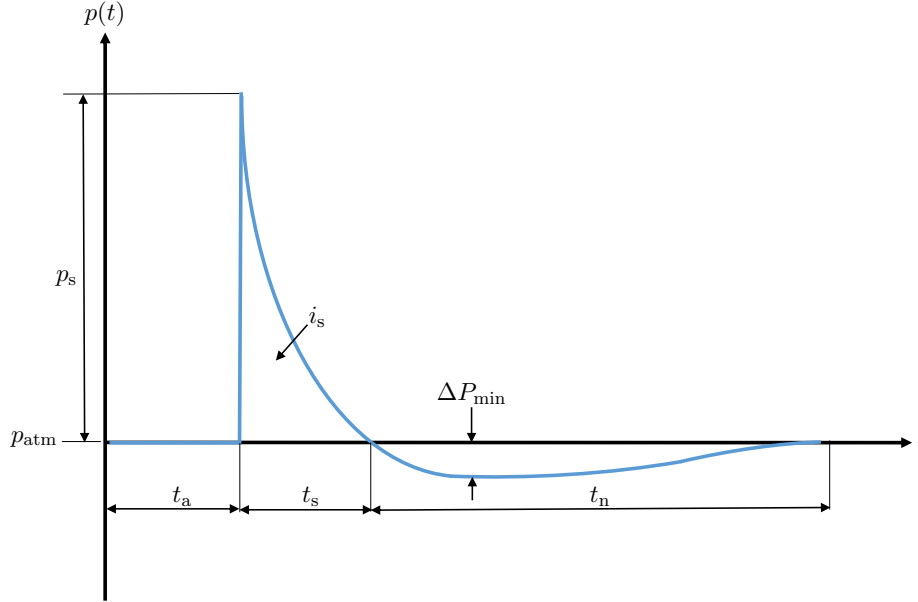


Figure 2.1: Blast wave pressure-time profile (adapted from [1]).

The wave arrival time at a point away from the seat of the explosion is represented by t_a . The duration of the positive phase is shown by t_s . The area under the positive phase of the graph, i_s , is the specific impulse of the wave. It should also be noted that $\Delta P_{\min}$ is the greatest value of pressure below the ambient in the negative part of the graph and t_n is the duration at which the pressure is below p_{atm} .

The pressure time trace can be represented by the Friedlander Eqn. [1]. This is shown by Eqn. 2.1.

$$p(t) = p_s \left(1 - \frac{t}{t_s} \right) e^{-\frac{b_p t}{t_s}} \quad (2.1)$$

Where b is the wavefront parameter which is a function of the peak overpressure, p_s .

2.1.1 Scaling of blast waves

When two blast waves are formed from the same explosive type but at different sizes, they can be related to each other using Hopkinson-Cranz scaling. A deduction of this theory is that the ratio of the distance at which

a given blast wave is produced, R , to the cubed root of the mass of the charge, W , is equal to a constant Z [3, 4]. This is shown by Eqn. 2.2.

$$Z = \frac{R}{W^{\frac{1}{3}}} \quad (2.2)$$

For the validity of Eqn. 2.2, it should be noted that TNT should be the reference explosive. However other type of explosives can also be analysed as long as their mass is converted to the equivalent TNT mass [1].

2.1.2 Blast wave parameters

In 1870, blast wavefront parameters were first introduced by Rankine and Hugoniot describing normal shocks in ideal gases [5, 6]. Further details about gas dynamics and compressible fluids can be found in reference texts such as Leipmann and Roshko [7] or Leipmann and Puckett [8].

Of more interest was Brode's work on developing results on peak static overpressure [9]. As a consequence of Brode's work Eqn. 2.3 and Eqn. 2.4 can be deduced.

$$p_s = \frac{6.7}{Z^3} + 1 \quad (p_s > 10 \text{ bar}) \quad (2.3)$$

$$p_s = \frac{5.85}{Z^3} + \frac{1.455}{Z^2} + \frac{0.975}{Z} - 0.019 \quad (0.1 < p_s < 10 \text{ bar}) \quad (2.4)$$

Alternative solutions also exist that stem from numerical and experimental work. They are in a very similar format to Brode's solutions. For example the Eqn. 2.5, Eqn. 2.6 and Eqn. 2.7 show the solutions presented by Henrych [10].

$$p_s = \frac{0.00625}{Z^3} - \frac{0.357}{Z^3} + \frac{5.540}{Z^2} + \frac{14.072}{Z} \quad (0.05 < Z < 0.3) \quad (2.5)$$

$$p_s = \frac{2.132}{Z^3} - \frac{0.326}{Z^2} + \frac{6.194}{Z} \quad (0.3 < Z < 1) \quad (2.6)$$

$$p_s = \frac{3.288}{Z^3} + \frac{4.05}{Z^2} + \frac{0.662}{Z} \quad (1 < Z < 10) \quad (2.7)$$

2.1.3 Blast wave interactions

As blast waves travel they inevitably would encounter with other mediums. When they confront a medium more dense than the medium transporting

the wave, they will reflect from that medium. A practical case would be when a blast wave reflects off a wall. For example if a blast wave is incident on a wall perpendicular to the direction of travel of the wave (angle of incidence = 0), the reflected wave will have a pressure which is higher than that of the incident wave. This is because the air molecules are brought to rest and further compressed, causing a reflected wave of higher magnitude to be induced [1].

Continuing from the work of Rankine and Hugoniot [5, 6], for zero incidence the reflected overpressure, p_r , can be derived [1]. This is shown in Eqn. 2.8.

$$p_r = 2p_s \left[\frac{7p_{\text{atm}} + 4p_s}{7p_{\text{atm}} + p_s} \right] \quad (2.8)$$

When the peak overpressure, p_s , is much lower than the ambient, the reflected overpressure term reduces to $2p_s$. However if the peak overpressure is much greater than the ambient pressure, then the reflected overpressure term reduces to $8p_s$.

2.1.4 Blast load prediction

Following a review of current methods for estimating blast loads [11], three main methods were identified as:

- 1) Empirical models
- 2) Phenomenological models
- 3) Computation Fluid Dynamics (CFD)

Empirical models entail the evaluation of experimental test measurements. The simplest methods to use for estimation of blast loads are empirical methods. A computer program called ConWep has been developed which determine blast loads on buildings based on experimental data [12]. This program employs the compound polynomials developed by Kingery and Bulmash [13]. They are essentially curve fits to a large quantity of experimental data for incident overpressure, incident impulse, positive phase duration and arrival time for spherical and hemispherical TNT explosions.

Alternative empirical methods also exist, such as the TNO [14]. Using this method the overpressure and the positive phase duration can be

calculated as a function of distance from the explosion. Another method is also the multi-energy concept [14]. This method can be used to estimate the blast of a variable strength gas explosion. This method is fast and also takes a conservative approach. However the selection of a suitable value for the strength of a charge can be difficult [11]

Phenomenological methods represent the physics of an explosion. Phenomenological models are said to have a lot of overlap between empirical and CFD models [2]. The two main models can be identified as SCOPE (Shell Code for Overpressure Prediction in gas Explosions) [15, 16] and CLICHE (Confined Linked CHamber Explosion) [17, 18].

Computational Fluid Dynamics (CFD) tools are the third category of methods which can be used to predict blast loads. ProSAir (Propagation of Shocks in Air) is a CFD tool used to predict blast loads [19]. It can be used to model the blast waves from a single explosive device. It should be noted that this Program was formerly known as Air3D. An alternative software which is also used is Ansys Autodyn – further details of which can be found in the literature [20] .

2.2. Mechanical behaviour of laminated glass

2.2.1 Response of monolithic glass

Annealed glass is commonly used for windows. Annealed glass generally contains many flaws. Griffith stated that for brittle materials, failure will occur at the largest flaw in the material [21]. He carried out a series of experiments on glass fibres at different diameters. He suggested that at larger diameters, the size of the flaw in the material is likely to be greater, thus implying lower strength. The results he gained from experiment confirmed this. On glass fibres with a smaller diameter, a higher breaking stress was measured.

One of the standard testing methods for assessing the strength of glass is the ring-on-ring test. Two rings of different sizes are placed concentrically on either side of the glass. On loading a uniform stress field is created in the annular area between the two rings. For a database in a European standard over 700 tests were carried out at low loading rates [22]. Breaking stress was recorded to be in the range of 30 MPa to 120 MPa. Following an assessment, 45 MPa was deemed as the acceptable strength for glass manufacturers. At high speed, for design purposes, the

breaking stress value of annealed glass has been estimated in the literature as 80 MPa [2].

2.2.2 PVB

Polyvinyl Butyral (PVB) is commonly employed as the interlayer in laminated glass. Many authors such as Du Bois *et al.* [23] have carried out modelling of laminated glass and have modelled the PVB interlayer using a hyperelastic material law.

Xu *et al.* [24] have carried out experiments on PVB at high strain rates in the range of $700 - 4500 \text{ s}^{-1}$. From their experiments they concluded that the PVB response is rate dependent – so as the rate is increased, the stress is increased for the same strain. They also fitted a material model to the data using a Mooney–Rivlin Model and Ogden Model. They tried fitting models with and without viscoelasticity. Not much difference was observed between the two. Thus they concluded that PVB under high speed loading can be treated with little or no viscoelasticity.

2.2.3 Post-crack response of laminated glass

PVB–glass bond

As a standalone structure, a laminated glass pane consists of a PVB interlayer sandwiched between glass layers. There is no physical adhesive between the glass and PVB. The bonding between the the glass and PVB is purely due to molecular interactions. The response of the laminated glass pane under loading depends on the stretching of the interlayer, and the adhesion between the glass and the interlayer. The designers objective is for the glass fragments to remain adhered to the glass, while the pane deforms, fully absorbing the energy from the blast wave – keeping the occupants of the building protected.

To what extent flying fragments detach from the interlayer or to what capacity the pane is able to absorb energy is influenced by many factors such as the adhesion between the glass and the polymer interlayer. For example, if there is too little adhesion, all the glass fragments can detach from the interlayer. If there is too much adhesion, however, there can be little de-bonding and thus can result in early tearing of the interlayer. This early tearing occurs because the interlayer is not able to deform as much, due to the unavailability of a de-bonded interlayer length.

The most classical experiments used for assessing adhesion are the pummel and peel tests. In the pummel test, the glass samples are impacted and visual observations of damage is made. The sample with the greatest percentage of glass that remains adhered to the interlayer is said to have the greatest adhesion. The pummel test does not lead to a bond fracture toughness; it is a subjective test and results are influenced by human judgement [25]. The peel test, however, is a better approach than the pummel test, as it leads to the calculation of a bond fracture toughness. Kinloch *et al.* have outlined the testing and analysis techniques for the peeling of flexible laminates [26]. The analysis of the peel test can become complex at times, as it requires energy lost via other mechanical routes such as bending of the peeling arm. Therefore a simpler method is desired.

In addition to the classical adhesion testing methods, some authors have adapted the through-cracked tensile test. This test was originally developed by Sha *et al.* to characterise the adhesion between the glass layers and the PVB interlayer [27]. In its early days, this test method was termed ‘tension adhesion test’, and was purely used for assessing adhesion. Several authors have previously carried out adhesion tests using this method [27, 28, 29, 30, 31, 32]. Some researchers, however, used this test method for assessing the post-crack response of laminated glass subject to blast loading [33, 34]. In this test, a straight crack perpendicular to the loading direction is created coincidently on both sides of the glass specimen, before testing in tension. This testing method has similarities to the peel test in that a load is applied to one layer which leads to bond failure between the loaded layer and the substrate. The through-cracked tensile test, however, has advantages over its predecessors. Firstly, it is not as subjective as the Pummel test. Secondly, the data analysis is simpler than the peel test because complications surrounding the bending of the peeling arm can be ignored, as there is no physical bending of the interlayer.

The majority of the work by previous authors has not provided a thorough analysis of the bond fracture toughness from the through-cracked tensile test. Seshadri *et al.* are the only authors to date who have provided a thorough analysis method [35]. They proposed that the interfacial fracture toughness and energy release rate in laminated glass can be calculated by knowing the interlayer constitutive model and conducting through-cracked tensile tests. Seshadri *et al.* later extended their work to develop a finite element model which would allow the analysis of more complex geometries [29]. In their work, a hyperelastic material

was assumed to represent the PVB interlayer. Delince *et al.* tried to use the methods proposed by Seshadri on a range of interlayers such as PVB and SGP (SentryGlas Plus) [36]. They compared their results for laminated glass with the PVB interlayer to those of Seshadri, however, they could not verify their results. Delince *et al.* proposed that this was because the specimens spend a very short time in steady state de-bonding. Also for the SGP, they could not successfully implement the same methods proposed by Seshadri.

Multiple cracking

As an extension to the single crack tests, Hooper *et al.* [33] conducted tests with multiple parallel cracks normal to the loading direction at different spacings. They then used a rate dependent Johnson-Cook plasticity model to characterise the stress-strain post-crack response of laminated glass. Hooper [37] also conducted random-cracked tests, in which the laminated samples were randomly-cracked and tested in tension. Del Linz *et al.* [28] also looked at delamination of the PVB interlayer in single cracked tensile tests and compared experimental data with a finite element model.

Temperature dependence

In the literature, the work on temperature dependency of laminated glass is very limited. Bermbach *et al.* [38] have conducted shock tube experiments at 13°C and 30°C. They concluded that at 30°C, greater strain and strain rate are observed than at 13°C. Hooper *et al.* [39] conducted DMA (dynamic mechanical analysis) tests on PVB alone and they reported that below 5°C – 10°C the material behaves glassy and more stiffly. Above this temperature, it goes into transition and finally it behaves rubbery above 40°C.

2.2.4 Flexural behaviour

The conventional experimental methods for testing materials in out-of-plane bending are the three-point and the four-point bending test methods. In such tests, the sample is supported using knife edges and is then loaded at preallocated locations. For testing laminated glass, if assessing the strength of the glass prior to cracking is the sole purpose of the test, then three-point or four-point testing methods could be useful. For

blast loading applications, the conventional testing methods are no longer useful, specifically in the post-cracking phase.

Under three-point or four-point bending, after the glass cracks, the interlayer will continue to deform at the location at which the preallocated loading points have been defined. Such testing methods are problematic for assessing the post-cracked behaviour because the location of deformation is being defined by the knife edges and loading points. Even in the uncracked laminated glass, three-point or four-point test may not be the most suitable testing method. In blast loading applications, the location at which glass cracks is of interest and this information cannot be deduced from conventional bending test methods.

Four-point bending tests on laminated glass was found not to be a common test in the literature. Only a few researchers have addressed this issue. In 1972 Hooper [40] carried out four-point bending tests at slow conventional speeds to measure deflection. He also carried out more long term tests at sustained loads under ambient temperature.

More recently Aiello *et al.* [41] have carried out four point bending tests on monolithic and laminated glass at low speeds in room temperature. They deduced the bending tensile strength and from observing the samples, the monolithic glass samples failed in a completely brittle manner into separate pieces, whereas the laminated glass remained fully intact but still brittle. The load against deflection was also linear up to the point of failure.

Fam and Rizkalla [42] have also carried out four-point bend tests on monolithic and laminated glass panes. They also observed similar behaviour to Aiello *et al.* [41]. Fam and Rizkala [42] also concluded that compared to monolithic glass, laminated samples have a higher flexural strength, stiffness and strain energy. They also reported that no delamination was observed in the experiment.

Jalham and Alsaed [43] have carried out bending tests on laminated glass with different interlayer thicknesses and different interlayer materials, specifically Ethylene Vinyl Acetate (EVA) film and Polyvinyl Butyral (PVB). They concluded, the thicker the interlayer, the higher the absorbed energy will be. On the fracture surface, they also observed that the propagation of cracks were linear in the glass and non-linear in the polymer interlayer.

Serafinavicius *et al.* [44] studied the effects of long term four-point bending tests on laminated glass with PVB, EVA and Dupont's SentryGlas

Plus (SGP). The tests were carried out at temperatures of 20°C, 30°C and 40°C, with each test taking 72 hours. The main motivation for running these tests were to assess the durability of laminated glass at a variety of temperatures. They concluded that samples with a PVB interlayer showed the highest mid-span deflections and laminated glass with SGP interlayers showed the lowest.

2.3. Structural silicone

Reaction forces on the surrounding frame of the laminated glass can be restrained through the use of structural silicone. Shear stresses can be transferred from the glass to the frame through the structural silicone [2]. Meunier *et al.* [45] have carried out experimental work on this material. They undertook tensile, compression, pure shear and bulge tests. Through the use of digital image correlation techniques they also managed to take full-field strain measurements. They fitted hyperelastic models to their experimental data and also compared their results to a finite element model. Hautekeer *et al.* [46] have conducted high speed tests on structural silicone. Tensile tests were conducted at 1.1 m/s and 2.5 m/s and shear tests were conducted at 1.1 m/s – speeds being representative of a blast scenario. The tests were conducted on different size specimens. The tensile strength was measured to be 1.5 – 2 MPa and the shear strength was measured to be 1.2 – 1.7 MPa.

Vallabhan *et al.* [47] proposed a mathematical model which can be used to calculate the in-plane, out-of-plane forces and moments in silicone sealant used along edges of window panels. The restraining effects of the sealant were modelled through the use of three springs. In the model, springs constants were used to represent in-plane, out-of-plane and rotational effects.

Ramesh *et al.* [48] have studied how the modulus of structural silicone is affected by the time of curing of the sealant. They concluded that after 100 extra days of curing the samples (in addition to the standard 2 weeks), a noticeable increase in modulus was observed. The effect of moisture on silicone sealant has also been studied, further details of which can be found in the literature [49].

2.4. Single-degree-of-freedom model

In structural design, it is important to be able to predict failure. Commonly the *single-degree-of-freedom* (SDOF) model has been used to design structures under blast loading. Finite Element (FE) analysis can also be used, but it is relatively costly. The SDOF model is simpler, less complex and cheaper than FE modelling, and can be applied easily to many different boundary conditions and loading types. In special cases, FE is employed, but in most cases, time requirements and computational costs cannot be justified.

In current SDOF models, for a given deformation phase (elastic or plastic), the deflected shape–time history is typically ignored and calculations are based on the final deflected shape. It is assumed that the structure takes the shape of that produced by a static load [2]. The deflected shape is assumed to be the same under all pressures and impulses. However, this may not be the case under fast loading rates and a more accurate design can be achieved if the deflected shape–time history is considered.

The varying deflected shape is used to calculate a set of load and mass transformation factors (K_L and K_M). The transformation factors are then used in the SDOF model. In the 1950's, values for the transformation factors were derived from an assumed final deflected shape – based on a static load. The transformation factors were tabulated for a range of boundary conditions, loading types, aspect ratios, and for elastic and plastic final deformations [50, 51].

Recently, some of the values of the transformation factors calculated in the 1950's have been challenged [52, 53]. One reason is that the deflections under static load were previously calculated by small-deflection theory, however, this was inaccurate for large deflections. Deflected shapes were developed using FE and corrected transformation factors were proposed [53]. This study is, however, still limited in the sense that for a given deformation phase (elastic or plastic), the variation of the transformation factors during the deflected shape–time history were not considered.

It is important to be able to predict the deflected shape correctly. If the deflected shape–time history is ignored, the final design outcome could be affected and several questions about the design are raised. For example, are the stresses on the edges of the pane and the early tearing of the interlayer (close to the edges) being predicted correctly?

Blast Simulator

3.1. Introduction

An important factor which has been overlooked in the literature is the high rate bending of laminated glass. The main focus of research to date in the literature has been on four-point bend tests carried out at low-rates, which as previously mentioned do not give much information about the behaviour of laminated glass subject to blast loading. Certainly there is an alternative testing method which is a full-scale blast test, however, it is a time consuming and costly process.

In this chapter a novel testing method is explained to study the bending behaviour of laminated glass subject to uniform loading, which is not as costly as full-scale blast tests. The objective of this study is to capture the high rate behaviour of laminated glass under bending. The effect of loading rate and interlayer thickness have been considered. In addition, the effect of boundary conditions on the bending behaviour has also been studied. A comparison will also be made to a finite element (FE) model, to determine the importance of bending resistance of the laminated glass pane in the post-cracking phase of the response.

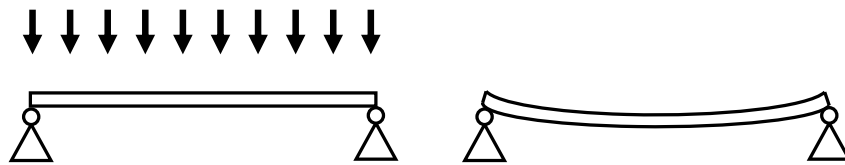
3.2. Materials and methods

3.2.1 Experimental methods

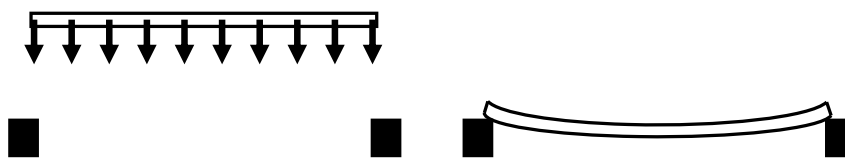
Achieving uniform loading

In order to achieve uniform loading without physical contact, a uniform load can be applied across the surface of the test specimen. When a uniform load is applied to the surface of the specimen which is held firmly at its edges, the specimen deflects in the out-of-plane direction at a given velocity (Fig. 3.1(a)). However, in the laboratory, this is not very practical as it is almost impossible to apply a velocity to a stationary object using conventional testing equipment. So an alternative method is required.

The underlying idea behind the test rig presented here is that the frame of reference of the application point of velocity is changed (Fig. 3.1(b)). The test specimen and its fixtures are accelerated to a certain velocity. At an instant later in the test, where the specimen is travelling at a certain velocity, the edges of the specimen are prevented from moving forward. The remainder of the specimen which is not prevented from moving, still has momentum and begins to deflect in the out-of-plane direction. The end result is that a uniform impulse (equating to mass $\times$ velocity) is applied to the test specimen.



(a) The desired test with an applied pressure causing the specimen to deflect at a certain velocity profile.



(b) The idea behind the current test rig with a velocity applied directly to the specimen.

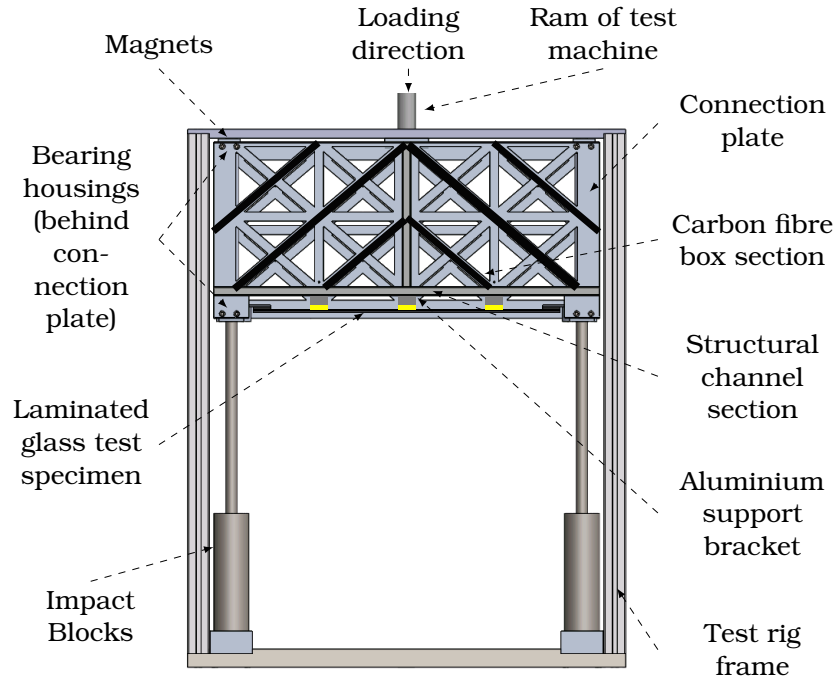
Figure 3.1: Comparison between two different testing arrangements where the frame of reference of the loading points is changed.

The test rig

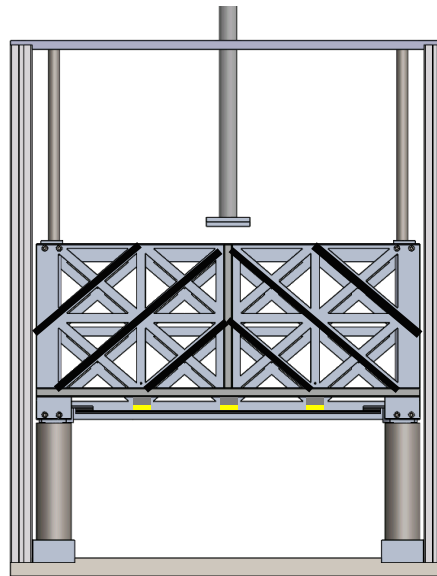
In addition to the uniform loading requirement, an additional element of complexity is also present in the problem. In order for the tests to be

meaningful and to be applied to applications of blast protection, the tests must be carried out at high rate. According to blast test observations in the literature, the out-of-plane deformation velocity of the center of the laminated glass pane has been recorded to be in the range of 15 – 30 m/s. In order to achieve a similar order of magnitude such that the results from the experiments can be relevant and meaningful, a high rate servo-hydraulic Instron testing machine was employed. This machine is capable of accelerating test specimens to velocities of up to 20 m/s.

A test rig was designed that would accommodate the high-rate Instron testing machine (Fig. 3.2). In this testing arrangement, a glass specimen is held by a carriage which is accelerated in the downwards direction by the ram of the high rate Instron testing machine. The underlying principle of this test is that a laminated glass specimen is accelerated using a carriage and the carriage is prevented from moving forward at an instant later in the test.



(a) Resting position prior to beginning of test.



(b) Position at impact.

Figure 3.2: Illustration of the test rig.

The carriage is initially held up against the frame of the test rig using magnets. The carriage along with the glass specimen is then accelerated by the ram to a pre-defined velocity¹. At an instant later in the test, the sides of the carriage are promptly brought to rest by the impact blocks. The glass specimen which is supported at its edges on the carriage, is free to deflect in the out-of-plane direction (Fig. 3.3). Note that the glass specimen does not directly impact against any object. In addition, in order to prevent the specimen from rebounding during the acceleration phase, aluminium support brackets were connected on the carriage. To prevent damage to the glass specimen, a soft foam was used on the contact surface between the aluminium bracket and the glass specimen.

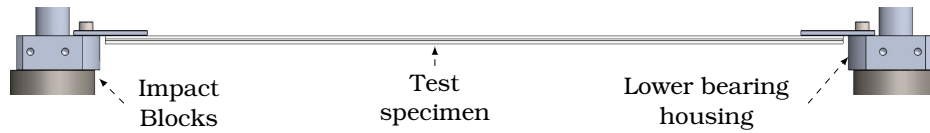


Figure 3.3: Illustration of the test specimen at impact point (with front and back connection plates and structural features removed).

The carriage used for carrying the glass specimen is supported on four bearings housings which facilitate the sliding of the carriage on the two shafts. This carriage was made of two aluminium sheets acting as a connection plate between the bearing housings. Structural features such as carbon fibre box sections and aluminium channel sections were added to the connection plate to increase its out-of-plane stiffness.

The Instron testing machine has a limitation on the specimen weight which it can accelerate, so it was not practical to test a square glass pane. Therefore, the test specimens used were in the form of a strip of laminated glass. This would allow the one-dimensional behaviour of laminated glass subject to high rate uniform loading to be studied.

Test specimens

The glass specimens were prepared by bonding the ends of the glass to aluminium tabs using Araldite 2021. Prior to applying the adhesive the surface of the glass and aluminium were grit blasted and cleaned with

¹The applied speed selected on the machine results in a different impact speed. This is because of the nature of the testing machine, not producing exact speeds at high testing rates.

Acetone. For attaching the specimens to the carriage, the aluminium tabs were bolted on the lower bearing housings of the carriage.

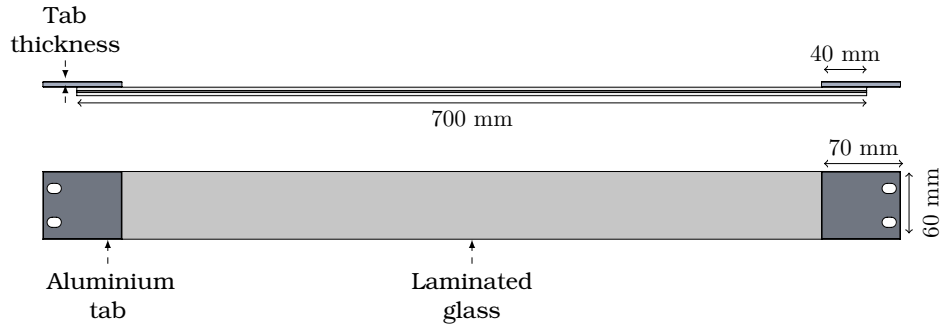


Figure 3.4: Test specimen schematic.

A total of 26 specimens were tested (Table 3.1). The thickness of each glass layer was 3 mm. The PVB thickness, however, was varied to allow the effect of interlayer thickness to be studied. Three different interlayer thicknesses were considered, 0.76 mm, 1.52 mm, 2.28 mm. Three different speeds of 6, 8 and 10 m/s were tested along with repeat tests at some conditions. These experiments were conducted on specimens with a aluminium tab thickness of 5 mm.

In addition to the experiments conducted on the normal specimens, two further specimen conditions were tested. The first condition was specimens which were pre-scored using a scribing tool, across the width on the outward side of each layer at a spacing of 62 mm along the length of the specimen (Fig. 3.5). This test was carried out to observe the effects of glass cracking at a lower stress upon impact on the response of the specimen in the post-cracking phase of the deformation. The second condition was specimens with a different boundary condition. The aluminium tab thickness was reduced to 2 mm in order to allow the aluminium tab to deform under loading. The purpose of these specimens were to study the effects of increasing the energy absorbing capabilities of the laminated glazing system as a whole unit.

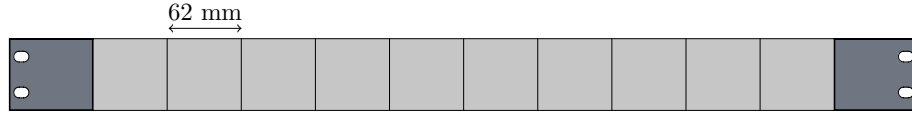


Figure 3.5: Pre-scored test specimen.

Table 3.1: Test matrix.

Specimen No.	Thickness (mm) Glass/PVB/Glass	Aluminium tab thickness (mm)	Applied speed (m/s)	Pre-scored
1	3/0.76/3	5	6	-
2	3/0.76/3	5	6	-
3	3/0.76/3	5	6	-
4	3/0.76/3	5	6	-
5	3/1.52/3	5	6	-
6	3/1.52/3	5	6	-
7	3/1.52/3	5	6	-
8	3/2.28/3	5	6	-
9	3/2.28/3	5	6	-
10	3/2.28/3	5	6	-
11	3/0.76/3	5	8	-
12	3/1.52/3	5	8	-
13	3/1.52/3	5	8	-
14	3/2.28/3	5	8	-
15	3/2.28/3	5	8	-
16	3/1.52/3	5	10	-
17	3/2.28/3	5	10	-
18	3/2.28/3	5	10	-
19	3/0.76/3	5	10	YES
20	3/1.52/3	5	10	YES
21	3/2.28/3	5	10	YES
22	3/0.76/3	2	10	-
23	3/1.52/3	2	10	-
24	3/1.52/3	2	10	-
25	3/2.28/3	2	10	-
26	3/2.28/3	2	10	-

Data processing

In this test, the displacement and the velocity of the laminated glass specimen were measured, prior to impact and during the deformation period. High speed video recording along with data analysis techniques on MATLAB [54], were used to calculate the displacement in number of pixels. Using a known distance in the image, the displacement in pixels was converted to displacement in mm.

The video recording was conducted in grey scale and the experiments were recorded from a side-on view. In order to distinguish the specimen from the background, the side of the specimen was painted in white, and a black card was placed in the background. The edge of the glass specimen could now be detected by using an algorithm written in MATLAB, at every point across the length of the specimen and between every time frame.

There was, however, an issue with some parts of the specimen. At the edges where extreme bending took place, small glass fragments would travel away from the specimen. These small glass fragments would interrupt the image and be falsely detected as the glass edge. This false detection results in a wrong displacement value. An example location where this problem would arise is shown as a blue line in Fig. 3.6. To overcome this problem, cross-correlation techniques were added into the data processing method.

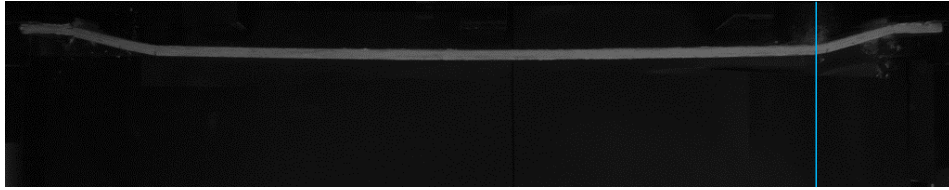
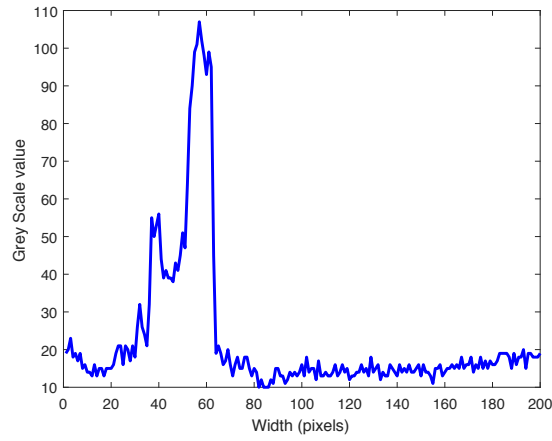
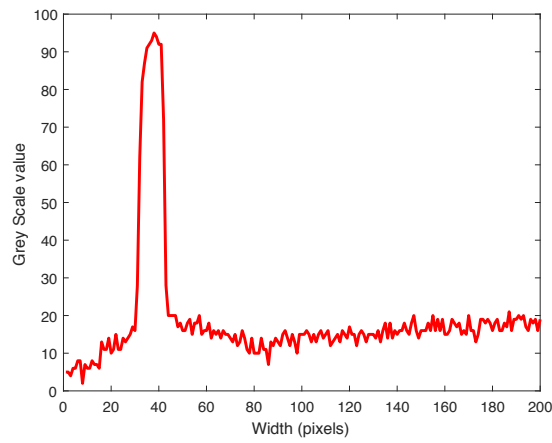


Figure 3.6: Example frame from specimen No. 14.

An illustration is provided in this section to explain the data processing method. If the blue line along Fig. 3.6 is considered, it is possible to see that separated glass fragments interrupt the image. Fig. 3.7(a) shows the grey scale value along the blue line in Fig. 3.6. To obtain the true specimen edge, a cross correlation was calculated between a section of the image that was interrupted by separated glass fragments (Fig. 3.7(a)) and a clean part of the image from the central section (Fig. 3.7(b)). The maximum value in the cross correlation, represents the location of the edge of the specimen (Fig. 3.8).



(a) The actual data from the interrupted part of the image due to separated glass fragments along the blue line in Fig. 3.6.



(b) The desired data from an uninterrupted part of the image.

Figure 3.7: Illustration of the desired and actual data present prior to use of cross correlation in the image processing.

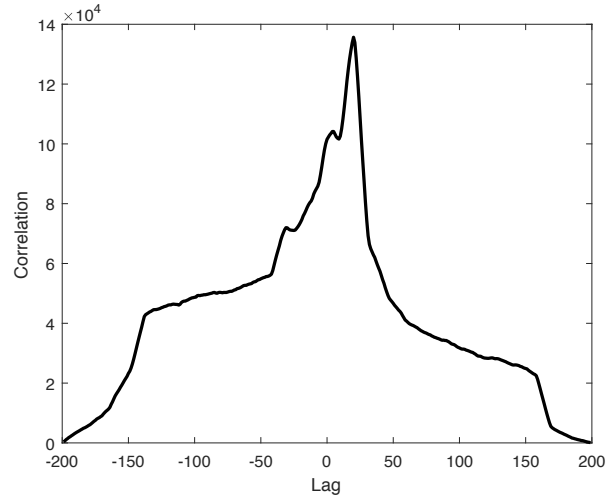


Figure 3.8: Cross correlation between the desired and the actual data.

This procedure was repeated along the length of the specimen. This only gives the location of the top edge of the specimen, so the image was rotated by 180 degrees and the process was repeated again to find the location of the bottom edge. An average was taken between the bottom edge and the top edge and this value was used as the current location of the laminated glass. This was also repeated at every time step so that the displacement–time history for the specimen can be obtained.

3.2.2 Modelling methods

A finite element model was created to allow a study of the bending resistance of laminated glass in the post-cracking phase of the deformation. A laminated glass composite consisting of three layers (glass/PVB/glass), was connected to aluminium end tabs. A velocity was applied to the laminated glass such that the aluminium tabs impacted onto a rigid steel impact block (Fig. 3.9).

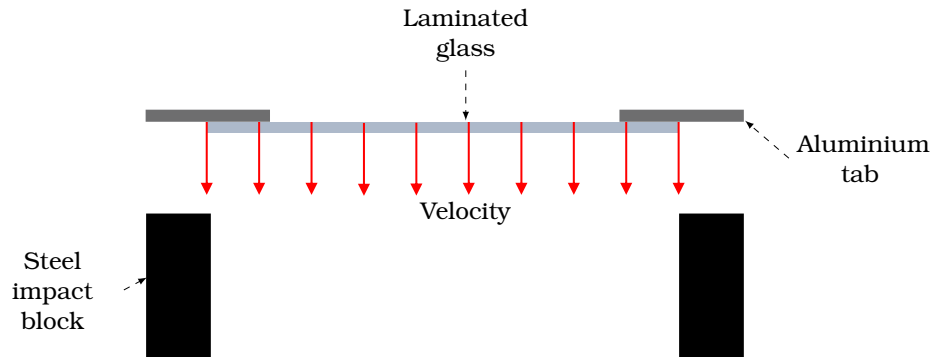


Figure 3.9: Illustration of the FE model.

This FE model was compared only with the normal specimens that did not fail (specimen numbers 1-10 and 12-15). The applied velocities which were examined were therefore 6 m/s and 8 m/s. The dimensions for the laminated glass were identical to the experimental specimens (700 mm long x 60 mm wide). The thickness of each glass layer was 3 mm. Three different interlayer thicknesses were considered (0.76 mm, 1.52 mm and 2.28 mm).

Material model

The modelling of laminated glass is a challenging task. The two distinct behaviours of glass and PVB, along with their interaction, complicate the material modelling process. Currently, two main approaches exist. The first approach is to treat each material and their interactions separately. The second approach is to treat the failure uniformly throughout the specimen. In the pre-cracked phase the two different modelling approaches are almost identical. In the post-cracked phase, however, differences exist.

One of the most complete materials models which has been verified against blast test observations is that developed by Hooper [33]. The modelling undertaken in this study is a modified form of the model developed by Hooper purely because it is a simpler model to apply. Hooper has shown that this model is in good agreement with blast test observations.

The Hooper model assumes negligible bending resistance (flexural rigidity) for the glass layers in the post-cracked phase of the deformation. Strictly, this may not be true, as bending resistance can originate from

many sources such as the interlocking of the glass fragments (Fig. 3.10). Furthermore, bending resistance can also form from the fact that the lines of cracking on either side of the laminate are not coincident with each other.

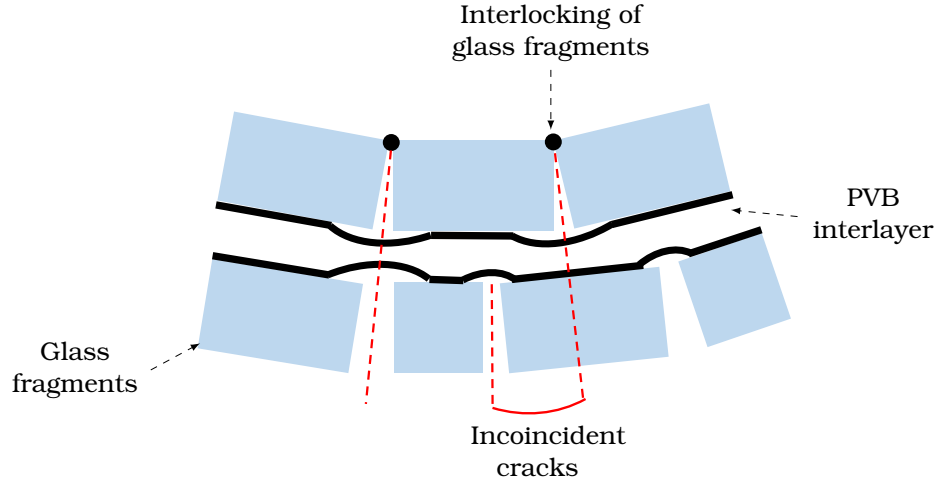


Figure 3.10: Origin of bending resistance in cracked laminated glass.

A shell model was constructed using three layers of glass/PVB/glass. For selecting the material model, it was assumed that a small amount of the deflection (less than 5%) was spent in the pre-cracked phase of the response. Therefore the pre-cracked phase of the response was ignored and it was assumed that the post-cracking phase begins at impact. For the post-cracking phase of the response, the model developed by Hooper [33] was selected.

In the Hooper model [33], the glass and PVB are treated separately. The glass component is treated as having elastic properties with zero stiffness and hence no bending resistance. In the model presented here, however, this was modified to incorporate bending resistance. An iteration was carried out to find the correct bending resistance required at every impact speed such that the maximum deflection in the FE model corresponded to the experimental results (Table 3.4). The flexural rigidity for each glass layer was calculated as EI , where E is the Young's modulus and I is the second moment of area.

In the Hooper model, the PVB interlayer is modelled using elastic properties (Table 3.2) and a rate dependent Johnson-Cook plasticity model

(Eqn. 3.1):

$$\sigma = (A + B\varepsilon_p^n) \left(1 + C \ln \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \right) \quad (3.1)$$

where σ is the stress, where $\dot{\varepsilon}$ is the strain rate, $\dot{\varepsilon}_0$ is a reference strain rate, ε_p is the plastic strain and A , B , C and n are material constants. The material constants used in the model are shown in Table 3.3.

Table 3.2: Material properties used in the FE.

Material	Density ρ (kg m ⁻³)	Young's Modulus E (GPa)	Poisson's ratio ν
Aluminium	2700	70	0.34
Glass	2530	Table 3.4	0.22
PVB	1100	0.53	0.485
Steel	8050	200	0.3

Table 3.3: Constants used in the Johnson-Cook plasticity model.

A (MPa)	B (MPa)	C	n
6.72	10.6	0.248	0.303

Table 3.4: The constants used for introducing bending resistance for the glass into the material model.

Impact speed (m/s)	Young's modulus (GPa)	Flexural rigidity (GPa mm ⁴)
6	0.3	40.5
8	0.05	6.75

3.3. Results

3.3.1 Experimental results

The experiments provided a comprehensive pool of results. A variety of results including two distinct failure modes were seen depending on interlayer thickness, impact speed, aluminium tab thickness and specimen pre-scoring. A summary of the results is shown in Table 3.5.

Table 3.5: Summary of test results.

Specimen No.	Thickness (mm) Glass/PVB/Glass	Aluminium tab thickness (mm)	Impact speed (m/s)	Pre-scored	Failure mode	Maximum Deflection (mm)
1	3/0.76/3	5	5.7	-	-	46.6
2	3/0.76/3	5	6.0	-	-	65.8
3	3/0.76/3	5	5.9	-	-	48.6
4	3/0.76/3	5	5.5	-	-	47.0
5	3/1.52/3	5	5.6	-	-	56.3
6	3/1.52/3	5	6.0	-	-	55.6
7	3/1.52/3	5	6.3	-	-	62.0
8	3/2.28/3	5	5.7	-	-	55.9
9	3/2.28/3	5	5.9	-	-	53.5
10	3/2.28/3	5	5.8	-	-	50.0
11	3/0.76/3	5	7.8	-	PVB tear	-
12	3/1.52/3	5	7.7	-	-	73.3
13	3/1.52/3	5	7.4	-	-	75.5
14	3/2.28/3	5	8.3	-	-	68.7
15	3/2.28/3	5	7.9	-	-	70.9
16	3/1.52/3	5	9.3	-	De-bond	-
17	3/2.28/3	5	9.0	-	De-bond	-
18	3/2.28/3	5	10.2	-	De-bond	-
19	3/0.76/3	5	9.3	YES	-	90.4
20	3/1.52/3	5	9.4	YES	-	78.6
21	3/2.28/3	5	9.5	YES	De-bond	-
22	3/0.76/3	2	9.4	-	-	107.1
23	3/1.52/3	2	9.5	-	-	80.9
24	3/1.52/3	2	9.8	-	-	98.3
25	3/2.28/3	2	9.8	-	-	80.7
26	3/2.28/3	2	9.8	-	-	75.4

The deflection of the specimen was determined by the impact speed and the PVB interlayer thickness (Fig. 3.11). For a given impact speed and at a constant condition (normal, pre-scored or specimens with thinner aluminium tab), a greater deflection is achieved for specimens with a lower interlayer thickness. This was true for all test conditions, except for an applied speed of 6 m/s for specimens with a specimen thickness of 0.76 mm. As expected, a greater deflection was achieved for specimens tested at a greater impact speed.

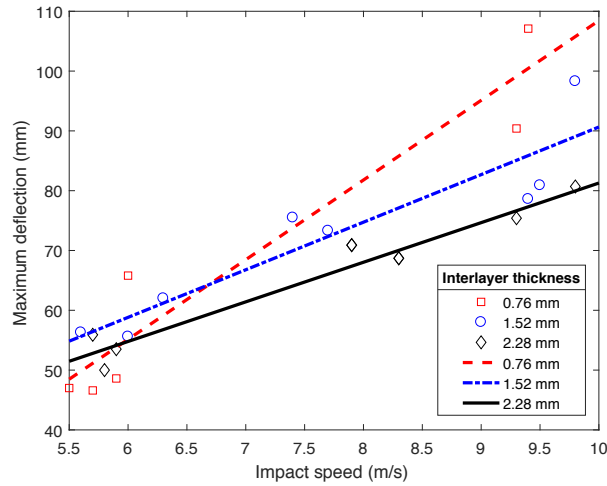


Figure 3.11: Maximum deflection against impact speed for all the different test conditions that did not fail.

At a applied speed of 6 m/s all the specimens showed the expected behaviour. After impact, cracking in the glass along with stretching of the interlayer was observed. Furthermore, as the applied speed was increased, failure of some specimens became more evident. For example as the applied speed was increased from 6 m/s to 8 m/s, the specimen with the 0.76 mm interlayer failed due to the tearing of the interlayer (Fig. 3.12).

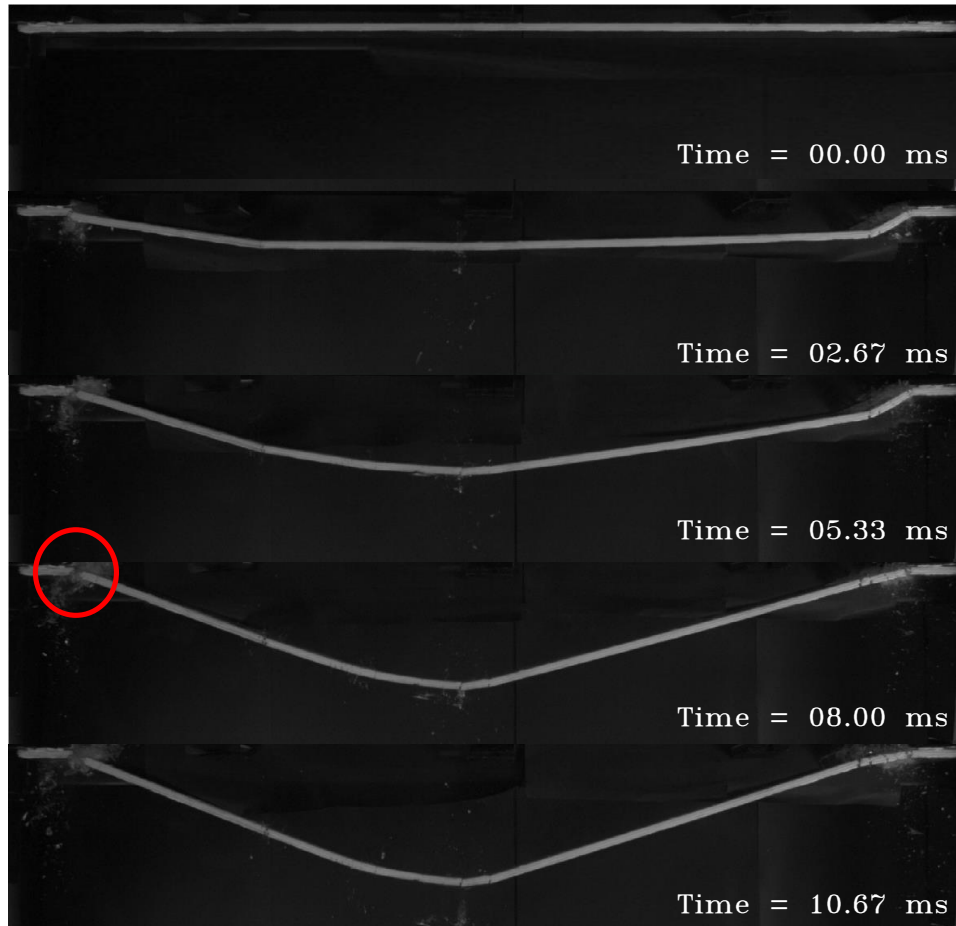


Figure 3.12: Deflection sequence, after impact, for specimen no.11 with a 0.76 mm interlayer tested at 8 m/s (PVB tearing around corner highlighted with a red circle).

However, the specimens with 1.52 mm and 2.28 mm interlayers did not fail (Fig. 3.13). A typical displacement through-time along the length of the specimen is shown in Fig. 3.14.

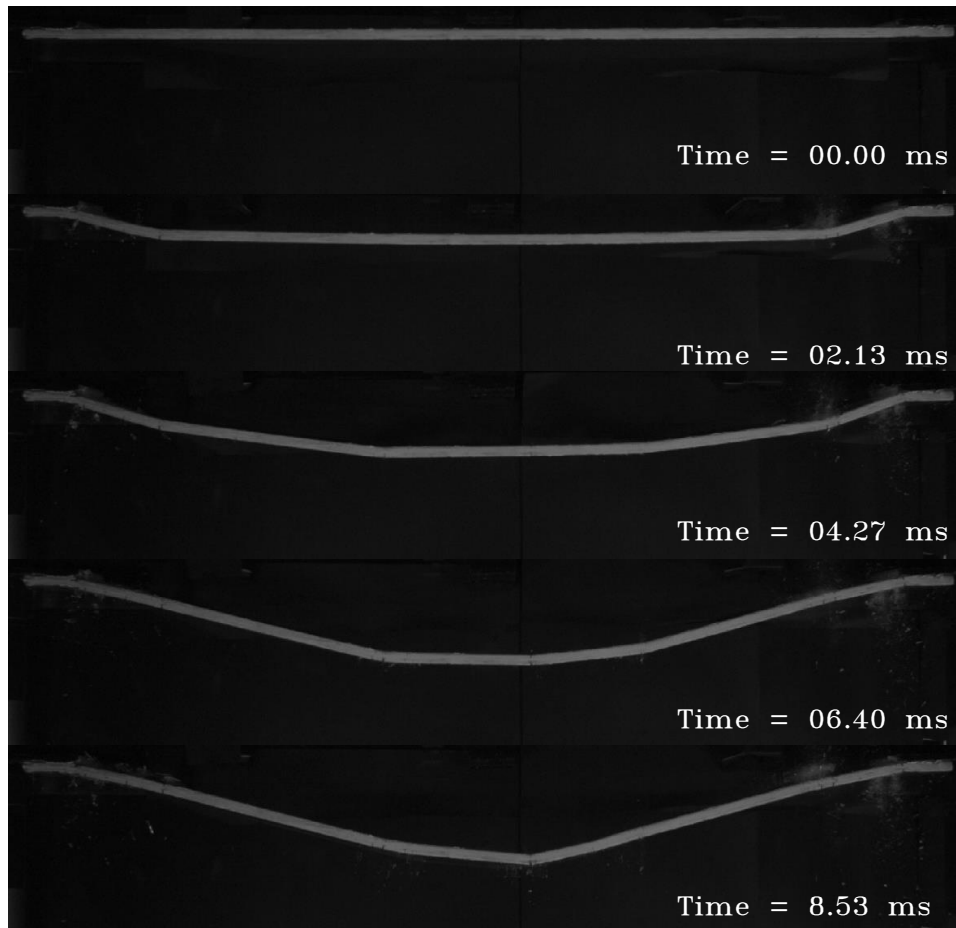


Figure 3.13: Deflection sequence, after impact, for specimen no.14 with a 2.28 mm interlayer tested at 8 m/s.

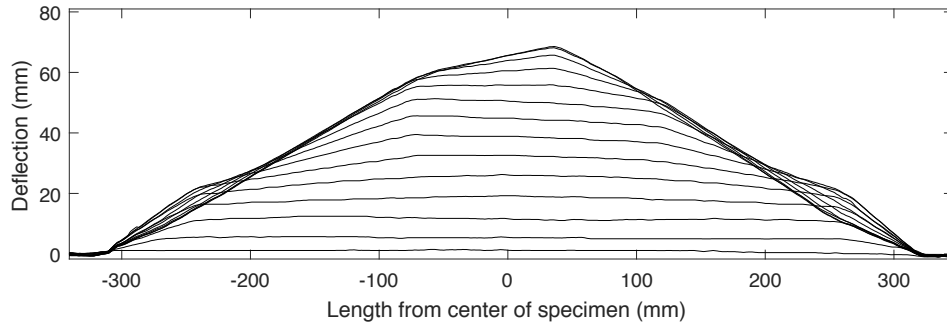


Figure 3.14: Deflection profile, after impact, through time along the length of specimen no.14 with a 2.28 mm interlayer tested at 8 m/s.

The applied speed was further increased from 8 m/s to 10 m/s. The specimen with the 0.76 mm interlayer was not tested again because it had already failed at speeds of 8 m/s, so there was no significance in testing at a greater speed. At 10 m/s the specimens with an interlayer thickness of 1.52 mm and 2.28 mm both failed due to delamination² between the PVB and top glass ply along the edge of the specimen, followed by a minor region of cracking through the top ply of the glass (Fig. 3.15). This caused the full separation of the specimen from the aluminium end tab on one side. Note that the other side did not fail, however, de-bonding of PVB from the glass had already started (Fig. 3.16).

²Delamination is a term used to indicate the de-bonding of the glass from the PVB interlayer.

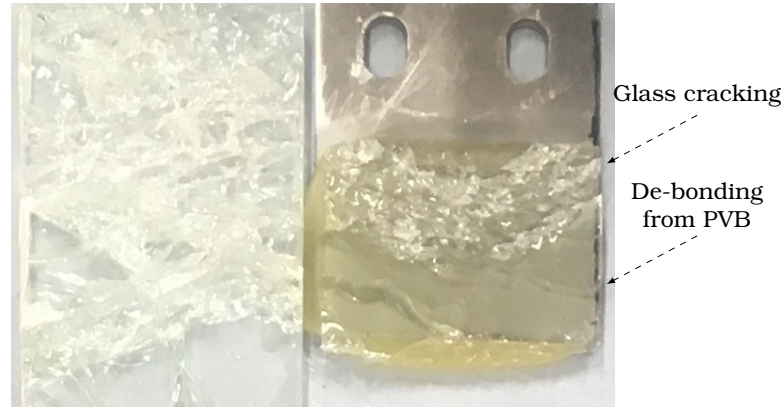


Figure 3.15: Delamination failure of specimen with 2.28 mm interlayer tested at 10 m/s (specimen no.18) – side 1.

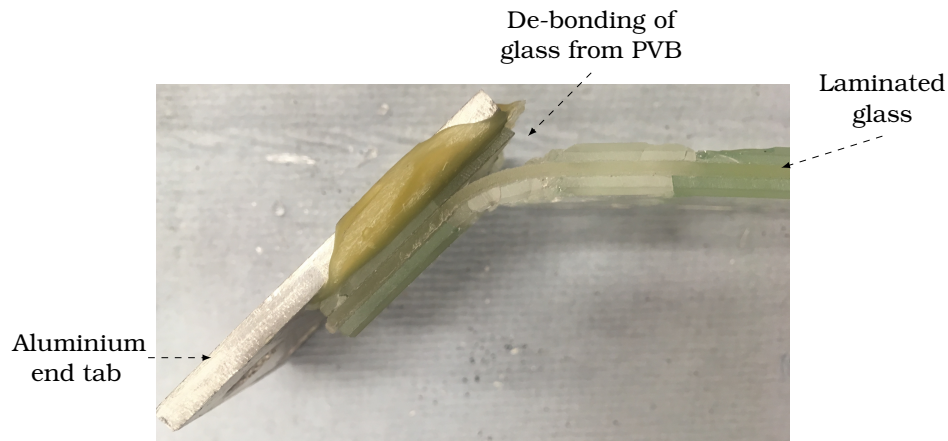


Figure 3.16: Delamination initiation of specimen with 2.28 mm interlayer tested at 10 m/s (specimen no.18) – side 2.

In comparison to the normal specimen, the scored specimens, which were all tested at an applied speed of 10 m/s showed an improvement in the response. The specimens with a 0.76 mm interlayer and 1.52 mm interlayer did not show any form of failure. The specimen with a 2.28 mm interlayer, however, still failed due to side delamination.

Finally, the specimens with a thinner aluminium tab thickness of 2 mm, which were all tested at an applied speed of 10 m/s, did not show any form of failure at all interlayer thicknesses. There was no interlayer tearing, and no

delamination between the glass and PVB. In addition a greater maximum deflection was achieved in comparison to all the other tested specimens.

Crack pattern

The post-crack pattern showed that at greater speeds a longer band of cracking is generally observed, especially at the edges. This is illustrated for specimens with a 1.52 mm interlayer (Fig. 3.17 and Fig. 3.18), and also for specimens with a 2.28 mm interlayer (Fig. 3.19–Fig. 3.22).

Length of
cracking band
↔



Figure 3.17: Post-experiment crack pattern for specimen with 1.52 mm interlayer tested at 6 m/s (specimen no.7).



Figure 3.18: Post-experiment crack pattern for specimen with 1.52 mm interlayer tested at 8 m/s (specimen no.13).



Figure 3.19: Post-experiment crack pattern for specimen with 2.28 mm interlayer tested at 6 m/s (specimen no.8).



Figure 3.20: Post-experiment crack pattern for specimen with 2.28 mm interlayer tested at 6 m/s (specimen no.9).

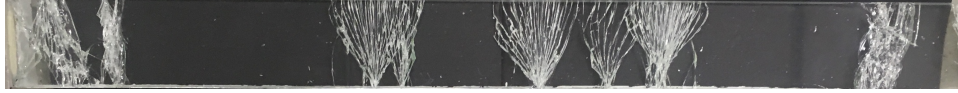


Figure 3.21: Post-experiment crack pattern for specimen with 2.28 mm interlayer tested at 8 m/s (specimen no.14).



Figure 3.22: Post-experiment crack pattern for specimen with 2.28 mm interlayer tested at 8 m/s (specimen no.15).

The specimens which were pre-scored to promote more cracking sites, did not actually crack along all the pre-scored lines. This was true for all the pre-scored specimens. An illustration for the specimen with a 0.76 mm interlayer tested at 10 m/s is shown in Fig. 3.23.



Figure 3.23: Post-experiment crack pattern for pre-scored specimen with 0.76 mm interlayer tested at 10 m/s (specimen no.19).

3.3.2 Modelling results

The FE results are shown in Fig. 3.24. Similar to the experiments the FE results also showed to be dependent on the thickness of the interlayer and Impact speed. At greater impact speed, a greater maximum deflection was achieved.

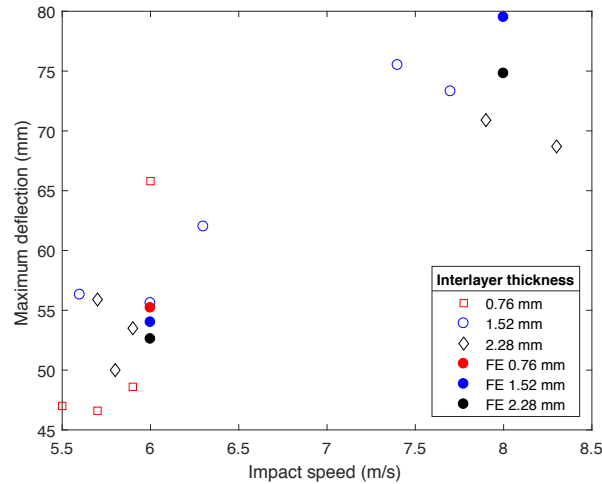


Figure 3.24: Comparison of FE results to experimental results for the normal test specimens that did not fail (specimen numbers 1-10 and 12-15).

3.4. Discussion

At higher impact speeds a greater maximum deflection is achieved because of the greater impact load applied on the specimen. More interestingly, at a given speed and test condition, a greater maximum deflection was generally achieved for a lower PVB interlayer thickness. This is because the thinner interlayer has a greater compliance and is therefore able to stretch more. This greater stretching could prove costly and lead to tearing at higher speeds because thinner interlayers can carry a lower load.

At the higher tested speeds, various modes of failure from PVB tearing to delamination on the edges started appearing. The specimens with a 0.76 mm interlayer tore, and the specimens with a 1.52 mm and 2.28 mm interlayer delaminated from the side of the glass layer. This could be because the glass was too strong and thus this led to a lower number of glass cracking sites and hence a lower length of PVB available for stretching. This caused local strain concentration in the PVB which led to tearing of the PVB for the 0.76 mm interlayer.

In addition, this illustrates the competing mechanisms of tearing and de-bonding. For the specimens with a 0.76 mm interlayer the energy required to de-bond the PVB from the glass is greater than that required

to tear the PVB. Therefore PVB tearing was observed. However, for the specimens with a 1.52 mm and 2.28 mm interlayer, the PVB is thicker and able to carry a greater load. So the energy required to tear the PVB is much greater than that required to de-bond the PVB from the glass. Therefore, delamination failure occurred in this instance.

In order to promote further glass cracking initiation sites, the glass was scored. This solved the problem partially, and PVB tearing and delamination on the edges were no longer observed for the 0.76 mm and 1.52 mm interlayer. For the 2.28 mm interlayer, however, delamination was still present on the side of the specimen. The impact speed for this specimen with a 2.28 mm interlayer was slightly greater than the 0.76 mm and 1.52 mm interlayer, but this could not be the only reason. In addition, not much cracking occurred along the scored lines, this could indicate that the depth of the scored lines were smaller than the previous two specimens with a thinner interlayer thickness.

The final testing arrangement used the thinner 2 mm aluminium end tabs, instead of 5 mm, to connect the glass specimen to the carriage. All the failure modes observed previously, such as PVB tearing and side delamination, were no longer present. This was true for all interlayer thicknesses. This is because some of the energy from the load is absorbed through the plastic deformation of the aluminium end tabs, which leads to lower stresses being experienced by the laminated glass. The idea behind this testing arrangement was to explore new design possibilities on framing members of a laminated glazing system such that the frame is purposely designed to undergo plastic deformation under loading. This intentional plastic deformation increases the blast resistance capabilities of the laminated glazing unit as a whole.

Furthermore, on the specimens with the thicker 5 mm aluminium end tabs, the two failure modes of interlayer tearing and de-bonding occurred close to the edges. The thinner 2 mm aluminium end tabs allow for rotation at the edges as they bend and absorb energy, hence reducing the localised loading intensity at the edges of the laminated glass specimen. This bending of the aluminium end tabs also causes the end boundary to act more like a simple support rather a built-in boundary condition – hence reducing the bending moments in the structure.

On observing the specimens post-impact, the first impression is that there is no relationship between the number of cracks formed in the glass specimen in relation to the impact speed. However, further considerations

suggest that this is not the case. Firstly, it is important to distinguish between the two crack types formed on the glass specimens. The two crack types are termed “spreading cracks” and “uniform cracks” (Fig. 3.25). The “spreading cracks” spread from a single point at the edges, suggesting they have been initiated from a site of stress concentration at the edges. On inspection, most of the “spreading cracks” were on the upper side of the specimen – suggesting that they had failed in tension on the rebound. Whereas, the “uniform cracks” seem to have formed earlier on in the test, as a results of travelling of the longitudinal stress waves.

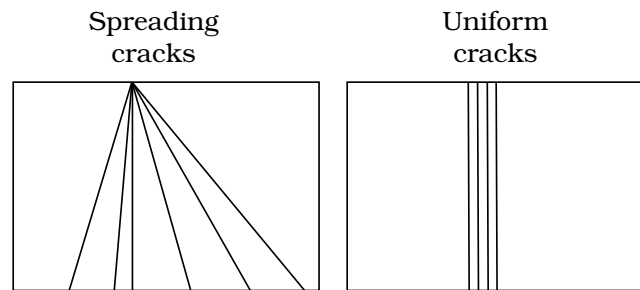


Figure 3.25: The two types of cracks seen on specimens after testing.

In addition, at greater impact speeds, a longer band of “uniform cracks” were seen along the length of the specimens. This is because at greater speeds, a greater deflection is achieved. To achieve this greater deflection, a greater rotation is required at the edges. As a result a wider region of uniform cracks is formed.

The main assumption up to now has always been that there is no bending resistance from the glass layers in the post-cracking phase of the response. However in this experiment, the bending resistance of the glass layers varied depending on the intensity of the load. This is because the intensity of the load determines the number of cracks formed and the cracking density. These two factors will then cause bending stiffness to develop in the glass. At lower speeds, this bending resistance is more apparent.

The material model used in the FE has its limitations. The material model assumed a uniform material properties along the length of the specimen. However, this is not accurate, when comparing with the crack pattern from the experiments. The experimental specimens were not

cracked uniformly along the length. The size of the crack and crack density were both different.

Comparison to full scale blast test

In comparing this test rig to a real blast test, the rig has some advantages and some limitations. The advantages are that a uniform load is applied, which also resulted in a similar deflected shape in comparison to blast test observations. Additionally, this experiment can be carried out at a lower cost compared to blast testing, allowing a study of many different testing variables.

This testing arrangement also has its limitations. The cracking pattern was not similar to the cracking pattern of specimens from a full scale blast test observation. The number of cracking sites were smaller in comparison to crack patterns in a full scale blast test. In a blast test, cracking occurs over all the specimen. This could be because the speeds achieved in a blast test are approximately two or three times greater. In addition, in the experiments carried out here the cracking sites formed as a result of bending and local stretching of the specimen. However, since in a blast test cracking occurs over all the specimen, the question is raised as to whether the cracks formed in a blast test could be due to other reasons such as the shock wave from the explosion. Furthermore, the fact that the specimens tested here were only one-way spanning, meant that the glass was primarily loaded in one direction and hence the cracking pattern was affected.

3.5. Conclusion

In this study, a test rig was designed to allow glass specimens to be uniformly loaded at high testing rates. Specimens with three PVB interlayer thicknesses were tested at three impact speeds. Normal uncracked glass specimens were used. Some specimens were pre-scored to promote cracking sites. A further testing arrangement was conducted on thinner aluminium end tabs. Finally the results from the normal specimens were compared to an FE model.

In the experiments it was found that at greater impact speeds, a greater maximum deflection was achieved. In addition, at a given speed and pre-test condition, the thinner interlayers generally achieved a greater

deflection. It was also found that the failure mode of laminated glass is determined by the thickness of the interlayer. With a thinner interlayer, laminated glass is more likely to fail due to the tearing of the interlayer. Whereas with a thicker interlayer, laminated glass is more likely to fail due to de-bonding of the PVB at glass edges.

With thinner aluminium end tabs, it was demonstrated that the likelihood of failure at higher speeds can be reduced by allowing framing members to absorb part of the impact energy. This could lead to novel framing designs and the idea of sacrificial frames – where the frame is purposely designed to absorb energy under loading, hence reducing the loading intensity on the laminated glass pane.

In the post-cracking phase of response, it has always been common practice to assume that the glass layers have negligible flexural rigidity, such that the laminated glass pane behaves like a membrane [33, 2]. This assumption is based on blast observations where the loading intensity was very high, such that the glass cracked all over the pane. However, it has been demonstrated here that the bending resistance of the glass layers become important at intermediate and lower loading speeds, where the cracking intensity is not as great as blast observations. In this chapter it was also demonstrated that the flexural rigidity is dependent on the cracking density and pattern in the glass.

Effects of Temperature

4.1. Introduction

Laminated glass composite properties may change once environmental conditions diverge from the design point. In some climates, air temperatures can reach up to 50°C and surface temperatures can exceed this value. As with most polymers, PVB's mechanical response is temperature dependent [55, 39]. This can lead to significant divergence in performance from the initial design and predictions. In this chapter the temperature dependence of single-cracked and randomly-cracked laminated glass specimens are experimentally tested.

This chapter investigates this at a range of temperatures from 0°C to 60°C. Single-cracked and randomly-cracked tests were carried out for laminated glass specimens with three different PVB interlayer thicknesses. These tests were carried out at a high loading rate of 1 m/s.

4.2. Materials and methods

4.2.1 Test specimens

The experiments were carried out on laminated glass specimens comprising three different PVB interlayer thicknesses, namely, 0.76 mm, 1.52 mm and 2.28 mm. These interlayers were laminated with 3 mm annealed glass on each side. The specimen length was 150 mm and the width was 60 mm. The specimens were prepared using a standard lamination

procedure by a commercial company (Kite Glass). The PVB used in the laminate was sourced from Everlam (52D PVB).

To enable the machine to grip the specimen, aluminium tabs were bonded to the laminated glass specimens using an adhesive (Araldite 2021). Prior to bonding, the aluminium tabs and the gripping ends of the glass were grit blasted; followed by cleaning of the surface with acetone.

The single-cracked specimens were prepared using a glass scoring tool. The specimens were scored on one side. This was followed by a gentle tap with a light ball hammer on the side opposite to where the first score was created. This propagated the crack on the opposite side of the hammer tap. This procedure was repeated on the opposite side of the glass, thus, enabling a coincident crack to form on either side of the glass. Fig. 4.1 and Fig. 4.2 display a schematic of the single-cracked and randomly-cracked specimens.

The second type of specimens were randomly-cracked. The randomly-cracked specimens more closely replicate the laminated pane condition after blast loading because of the density of cracking and also the random shape of the glass fragments. The randomly-cracked specimens were prepared using a ball hammer. The specimens were tapped with a hammer in approximately five locations on both sides of the specimen. The location of the hammer impact and number of hits were kept almost the same between subsequent specimens, such that the size of the fragments produced was approximately 5 – 10 mm.

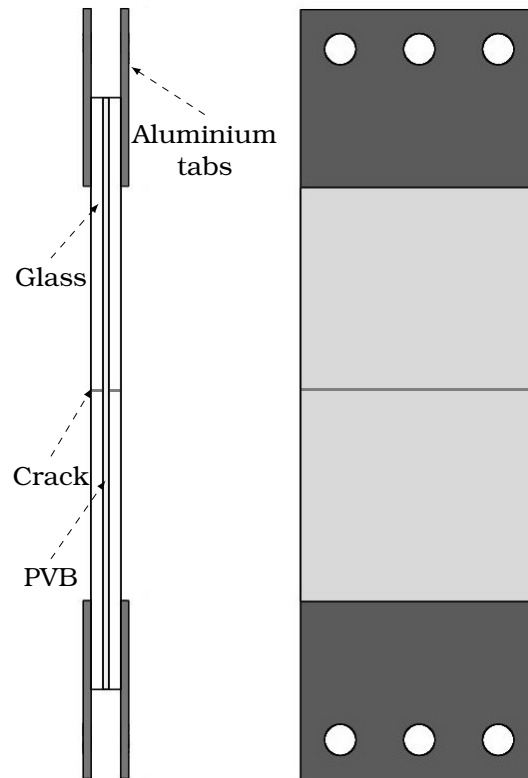


Figure 4.1: Single-cracked test specimen.

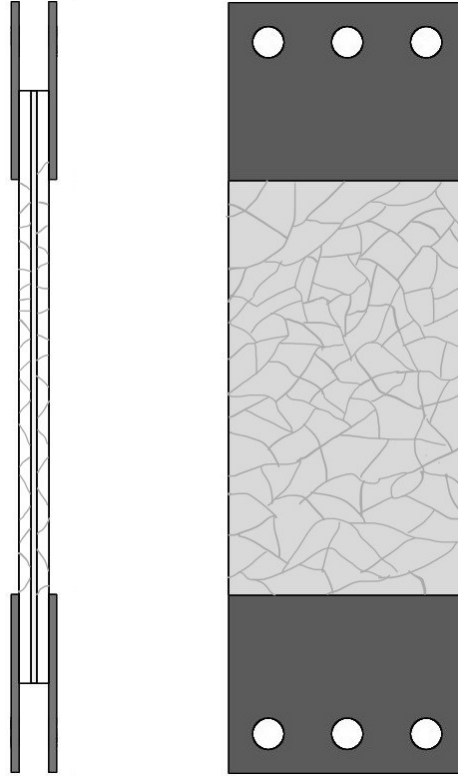


Figure 4.2: Randomly-cracked test specimen.

For the single-cracked tests, 30 specimens were tested, and for the randomly-cracked test, 63 specimens. Experiments were carried out over a temperature range of 0°C to 60°C, in increments of 10°C. Tests were conducted at a constant loading rate of 1 m/s. In some cases up to four repeats per test condition were carried out. Tables 4.1 and 4.2 show the number of specimens tested for the single and randomly-cracked tests respectively, at each interlayer thickness.

Table 4.1: Number of tests for single-cracked specimens.

Thickness of PVB (mm)	Temperature (° C)						
	0	10	20	30	40	50	60
0.76	1	1	2	1	1	1	1
1.52	2	2	2	1	1	2	2
2.28	1	2	1	2	2	1	1

Table 4.2: Number of tests for randomly-cracked specimens.

Thickness of PVB (mm)	Temperature (° C)						
	0	10	20	30	40	50	60
0.76	2	2	3	3	3	3	3
1.52	3	3	4	4	4	4	3
2.28	2	2	3	3	3	3	3

4.2.2 Experimental setup

The tests were carried out using an Instron servo-hydraulic high rate testing machine. Fig. 4.3 shows the experimental setup. The glass specimen was connected to the grips through dowel pins. The load was applied through a lost-motion rod; this allowed for the system to accelerate to the desired test velocity, prior to loading the specimen. The load was measured using a piezoelectric load cell (PCB model 222B). This was placed between the specimen and the machine base.

Since the glass was cracked, what was essentially being tested was the PVB. Since the compliance of the test rig is negligible compared to that of the PVB, the actuator extension was used as a measure of the specimen displacement during the test.

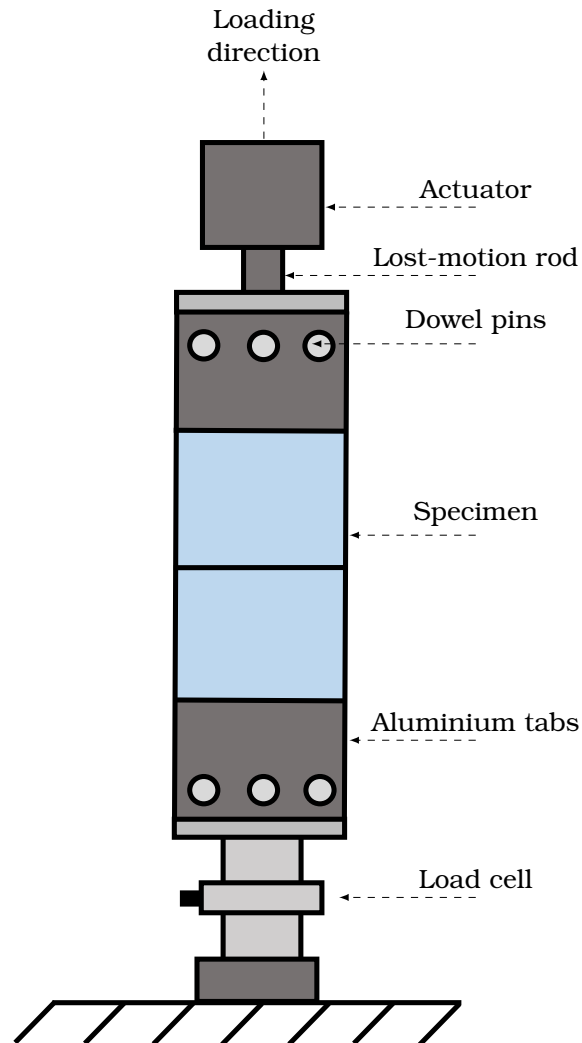


Figure 4.3: Tensile test setup.

In the single-cracked tests, it was important to get an accurate measurement of the delamination¹. This was done through the use of a photoelasticity setup (Fig. 4.4).

Linear polarising filters were placed in front of and behind of the specimen at a 90° angular difference. The camera was placed behind of the front polarising sheet. A Halogen light, with a diffuser in front, was

¹Delamination is a term used to indicate the de-bonding of the PVB from the glass.

placed behind of the rear polarising sheet. Finally, the specimen was placed between the polarising filters. This allowed different regions of the PVB delamination to be identified in the high speed recording. A clear delamination front was visible and using image processing techniques, delamination was quantified.

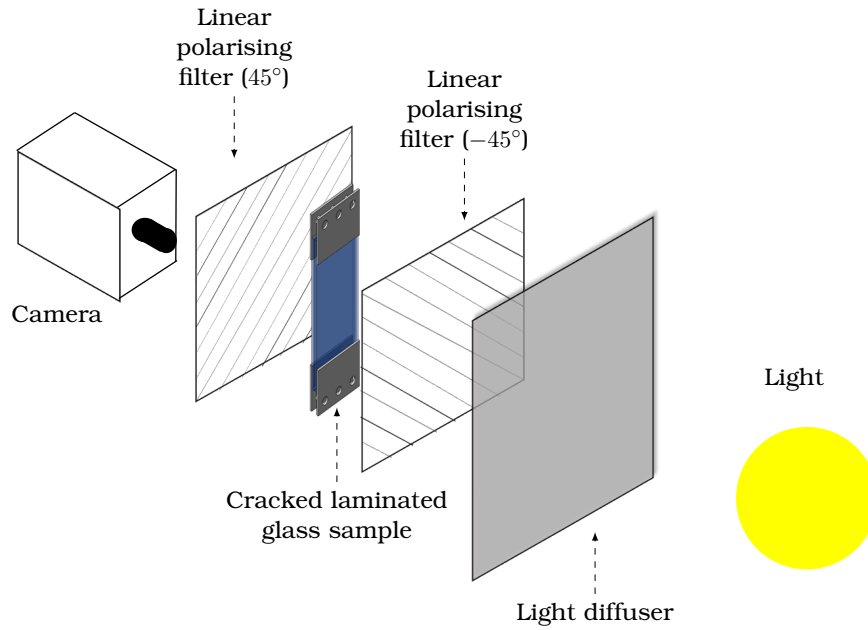


Figure 4.4: Photoelasticity setup.

To achieve the required temperatures, the specimens were kept in an Instron environmental chamber for 24 hours prior to testing. This was to ensure that the PVB was at the same temperature as the surface of the glass. In this case it was impossible to measure the temperature of the PVB interlayer as thermocouples would have had to be located on the PVB during the lamination process. This could have significantly affected the lamination quality. Instead, the temperature of the environmental chamber was measured to $\pm 0.5^\circ$ using air temperature sensors built into the environmental chamber and also independent thermocouples.

In order to enable the photoelasticity setup, a camera was required at the front of the specimen and a light behind the specimen. Linear polarising film was also needed in front of and behind of the specimen. This would have not been possible if the specimens were tested in

the environmental chamber. The specimens were placed inside the environmental chamber 24 hours prior to testing. They were then removed from the chamber and tested. The setup process took less than 30 seconds and the test duration was less than 100 milliseconds in all cases. The glass surface would have cooled slightly in that time; however, the more important temperature is that of the PVB. Since the PVB was sandwiched between two glass layers, the change in temperature during that time was insignificant.

4.2.3 Data Analysis

The load data was extracted and the engineering stress in the PVB ligament was calculated by Eqn. 4.1.

$$\sigma = \frac{P_t}{b \times t_{\text{Interlayer}}} \quad (4.1)$$

Where P_t is the load, b is the initial sample width and $t_{\text{Interlayer}}$ is the initial interlayer thickness.

In the randomly-cracked specimens, a maximum load and an average load (which is the load averaged across the whole time duration of the test), followed by a corresponding stress were calculated. However, in the single-cracked tests, a more relevant parameter is the de-bonding load. This can be defined as the lowest load plateau at which delamination propagates [56]. As an example Figs 4.5 and 4.6 show how the de-bonding load was calculated for different load-displacement profiles. The dashed red line represents the de-bonding load. The de-bonding load is very central to the work presented in chapter 5.

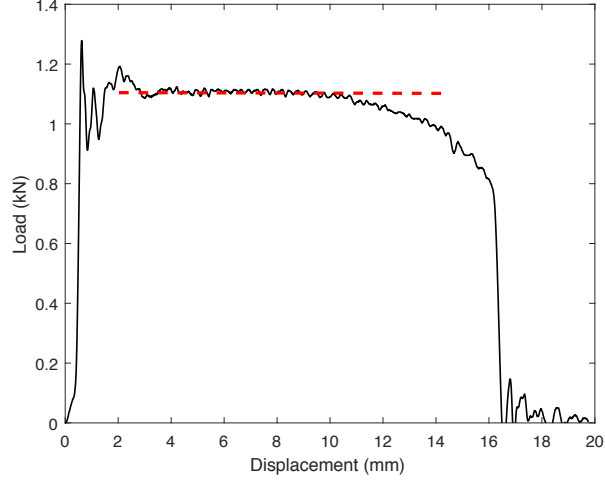


Figure 4.5: Single-crack 0.76 mm interlayer at 20°C.

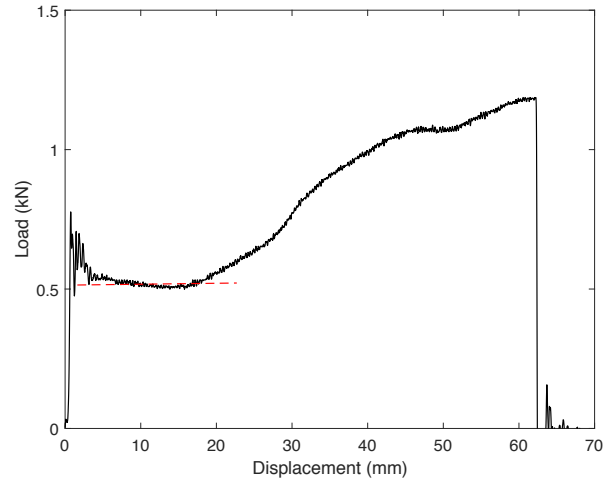


Figure 4.6: Single-crack 0.76 mm interlayer at 40°C.

For the randomly-cracked specimens, the nominal strain, ε , in the cracked laminate was calculated from the gauge length, l_g , at the start of the test which was 105 mm, and the displacement, Δl , using Eqn. 4.2.

$$\varepsilon = \frac{\Delta l}{l_g} \quad (4.2)$$

This is the average strain in the gauge length of the cracked laminate and not the actual strain in the PVB.

In the randomly-cracked experiments, the strain in the PVB was not considered, because it would be almost impossible to account for the strain in every ligament of the PVB. During the experiment, the PVB locally debonds in multiple locations surrounding cracked glass fragments. This occurs in many directions and angles compared to the loading direction. Even though the local strain surrounding each glass fragment can vary greatly, this measure of nominal strain in the cracked laminate as a whole is still useful. This value can be used as input into computational models when considering the failure of the laminate as a whole on a global scale. Local strain becomes important when assessing the fracture mechanism of the PVB on a very local scale.

For the single-cracked tests, it is more relevant if the strain in the PVB is considered. This makes the calculations slightly more complicated. This is because the gauge length of the specimen changes during the experiment as the delamination of the PVB from the glass increases. The photoelasticity setup is then important in allowing the delamination front to become visible.

The pictures taken by the high speed camera during the experiment were processed in MATLAB [54]. An edge was identified and tracked, which allowed for the displacement in pixels to be calculated. The pixel to mm ratio was calculated using a known distance in the picture. This was used to convert the displacement in pixels to displacement in mm. A similar technique was used to calculate the delaminated length, l_{del} , by tracking the delamination front.

The gauge length was calculated as the total PVB length available for deformation, had the PVB ligament not deformed. This is equivalent to the total delaminated length. The total delaminated length was calculated by summing the upper delaminated length (L_U) and the lower delaminated length (L_L). The nominal strain, ε_n , in the PVB ligament for the single-cracked specimens was calculated using Eqn. 4.3. Fig. 4.7 illustrates this. Note that the strain in the PVB, particularly around the edges can vary and the strain defined here is only an average strain across the delaminated PVB length.

$$\varepsilon_n = \frac{\Delta \text{Length}}{\text{Gauge Length}} = \frac{\Delta l}{l_{\text{del}}} \quad (4.3)$$

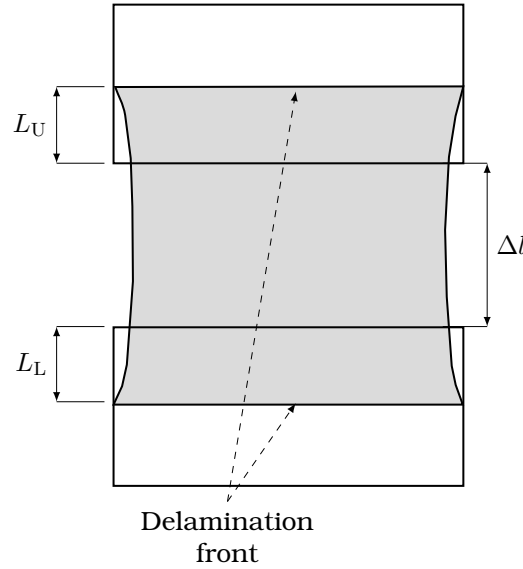


Figure 4.7: Delamination of PVB from laminated glass.

The total energy absorbed during the test was calculated as the area under the load-displacement graph. The trapezoidal numerical integration method was used to calculate this. It should be noted that this includes the energy of delamination, viscoelastic losses, and the strain energy [32].

In all calculations, the standard deviation was calculated. In the plots in the following sections, the error bars represent one standard deviation.

4.3. Results

4.3.1 Single-cracked specimens

Fig. 4.8 illustrates an example of the load-displacement trace obtained at different temperatures from the single-cracked tests. This is for the specific case of an interlayer thickness of 0.76 mm. At lower temperatures, high load with low displacement occurs. As the temperature increases, the load drops and displacement at failure increases. Finally, at the greater temperatures, both the load and displacement decrease. Similar trends were also observed for the other interlayer thicknesses and repeat tests.

At 20°C – 30°C the load increases up to a maximum then drops to an almost constant plateau. At lower temperatures the plateau does not exist.

Whereas at high temperatures, initially there is a small plateau, followed by a gradual increase in load and finally reaches a maximum towards the end of the test.

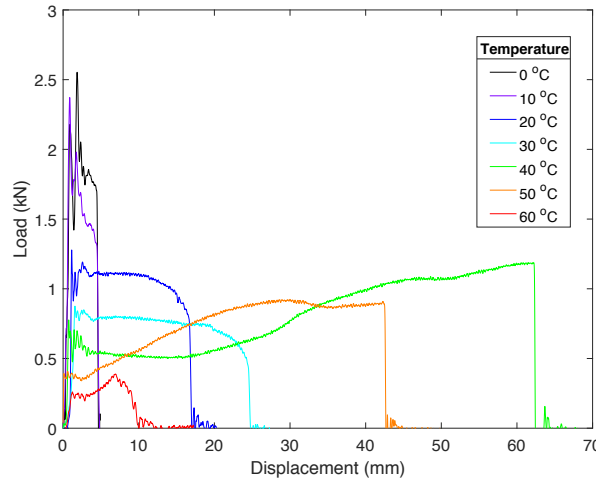


Figure 4.8: Load against displacement at 0.76 mm interlayer thickness for single-crack tests.

Considering the 20°C case as a detailed example (Fig. 4.9), the load initially rises to a peak, followed by a small drop, and then settles at an almost constant load for most of the duration of the test.

Selected frames from the high speed recording are shown (Fig. 4.9). The test begins with a mixture of de-bonding and stretching of the PVB. The PVB is also seen to bridge the gap between the two edges of the cracked glass. As the loading progresses, further de-bonding takes place, such that new PVB material becomes available for stretching, and the PVB simultaneously undergoes stretching in this process. This leads to an extension of the PVB bridge between the two glass pieces. The delamination continues until either all the PVB has separated from the glass or until the point of PVB tearing. The latter occurs in this specific case and in most cases. However for some of the specimens with the thicker interlayer, full delamination did occur. In the discussion, it is further explained why the thicker interlayers have a greater tendency to fully de-bond.

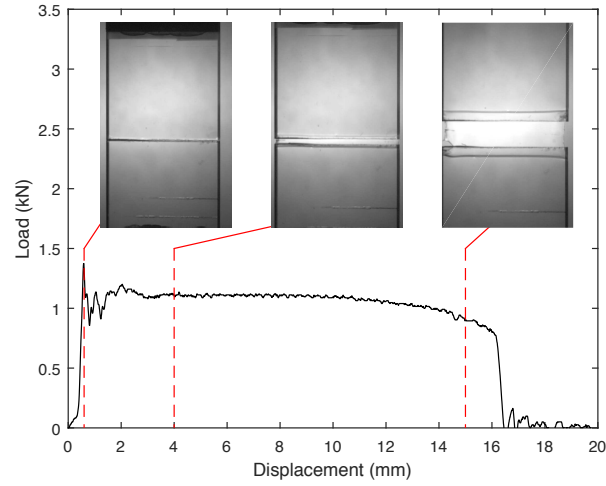


Figure 4.9: Load vs displacement at 20°C for 0.76 mm interlayer thickness in single-crack test.

Fig. 4.10 displays the specimens at half of failure time and Fig. 4.11 displays the final states prior to failure, for the 0.76 mm interlayer. At the lower temperatures (0°C and 10°C) and at 60°C, the amount of de-bonding and interlayer extension is low compared with other temperatures. Similar patterns were also seen for other interlayer thicknesses. At some instances where the delamination front was uneven, the value at the mid-span of the specimen was considered.

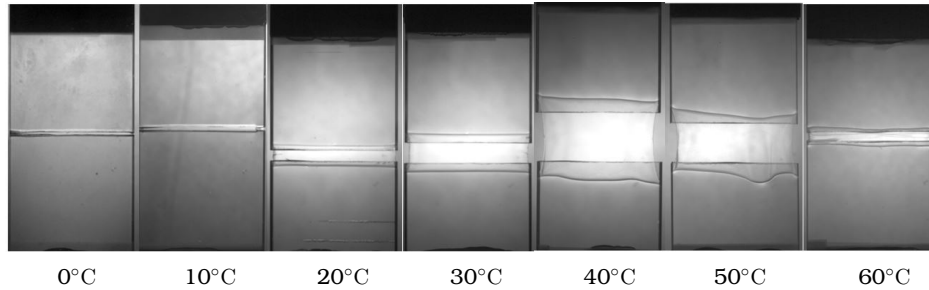


Figure 4.10: State of sample at half of failure time for the 0.76 mm interlayer in single-crack tests.

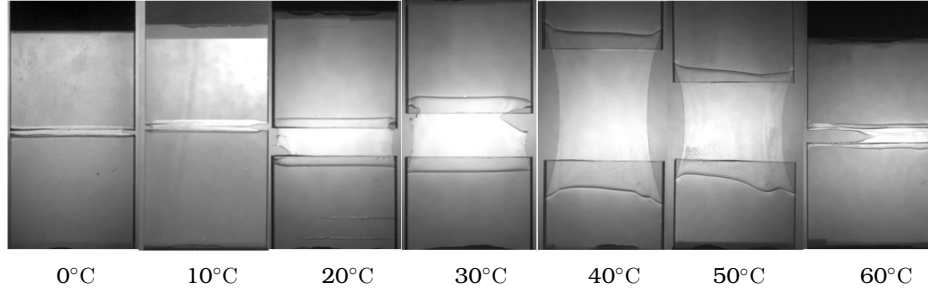


Figure 4.11: Final state prior to failure for the 0.76 mm interlayer in single-crack tests.

At greater temperatures, the load required for delamination to propagate (de-bonding load) is reduced (Fig. 4.12). The thicker interlayers also hold a greater load before delamination propagates. A greater absolute difference in load is observed between the interlayer thicknesses at lower temperatures in comparison to higher temperatures.

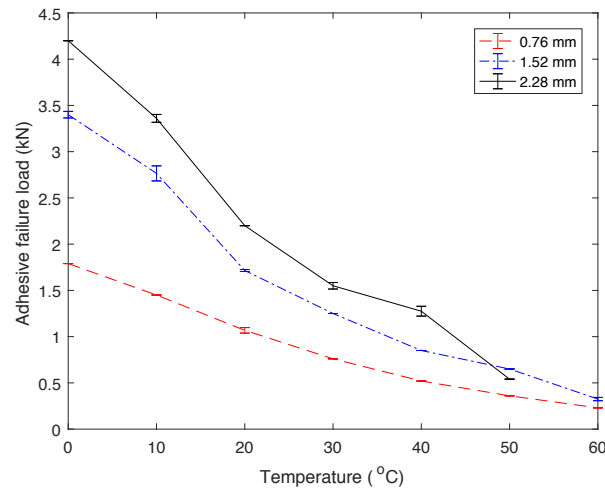


Figure 4.12: De-bonding load against temperature for the single-crack tests at different PVB thickness.

Using the optical methods described in section 4.2.3, the strain in the PVB bridge was calculated. As an example, Fig. 4.13 displays strain against displacement for specimens with a 1.52 mm interlayer. Greater strain was observed for the low temperatures (0°C and 10°C) and also for the high temperatures (50°C and 60°C). Similar trends were observed for the other

interlayers. It should be noted that the strain displayed in the figure was calculated using optical methods. As a result of this, for some of the experiments, the plots shown are not for the full duration of the test due to the sample not being in the boundaries of the camera lens for the full duration of the test.

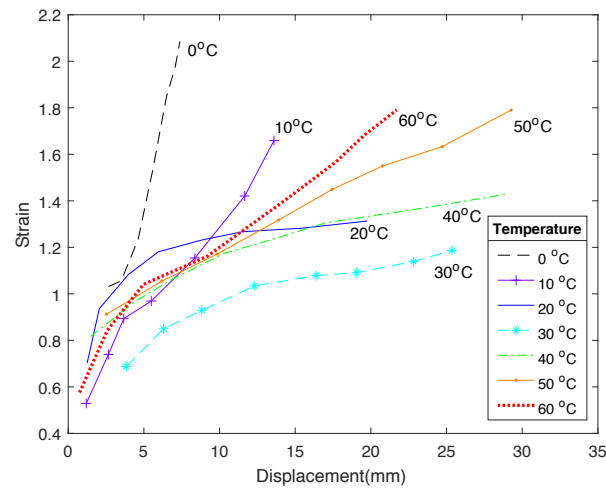


Figure 4.13: Nominal strain in PVB ligament at 1.52 mm interlayer for single-crack tests.

The variation of strain with interlayer thickness was also examined. All temperatures were considered in this study, however only two are displayed in Fig. 4.14 and Fig. 4.15 as a demonstration. The general trend is that the specimens with a thinner interlayer undergo a greater strain and less delamination for a given displacement.

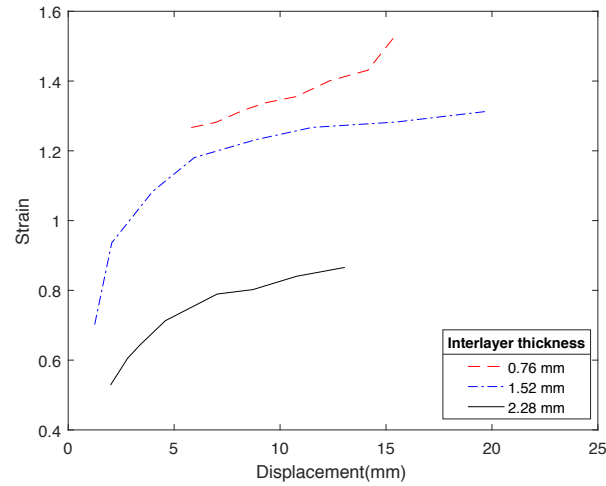


Figure 4.14: Strain at 20°C in single-crack tests.

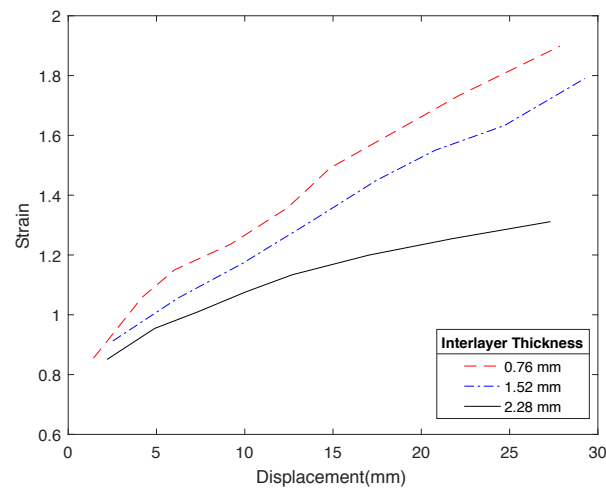


Figure 4.15: Strain at 50°C in single-crack tests.

4.3.2 Randomly-cracked specimens

Fig. 4.16 shows a stress against strain plot for the specific case of the specimen with a 2.28 mm interlayer, in the random-crack experiments. As the temperature increases, the load reduces and failure displacement begins to increase. As the highest temperature is approached, the failure

displacement is reduced. Similar trends were also observed for other interlayer thicknesses and repeat tests.

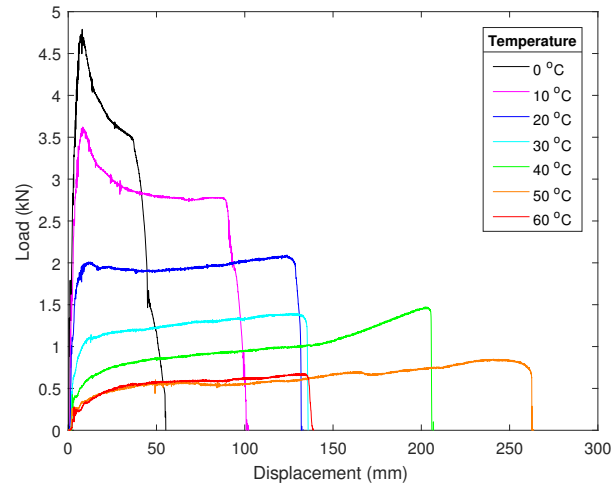


Figure 4.16: Load vs displacement for random-crack tests for the 2.28 mm interlayer.

The maximum load drops with temperature increase, similar to the single-cracked specimens (Fig. 4.17). The rate of load reduction with temperature is greatest for the 2.28 mm interlayer, followed by the 1.52 mm and the 0.76 mm interlayers. However, the trend for drop in maximum stress with increasing temperature is fairly similar for all interlayer thicknesses (Fig. 4.18).

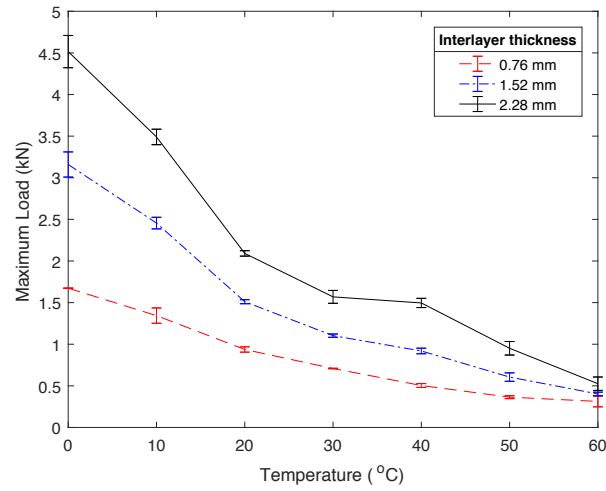


Figure 4.17: Maximum load in random-crack tests.

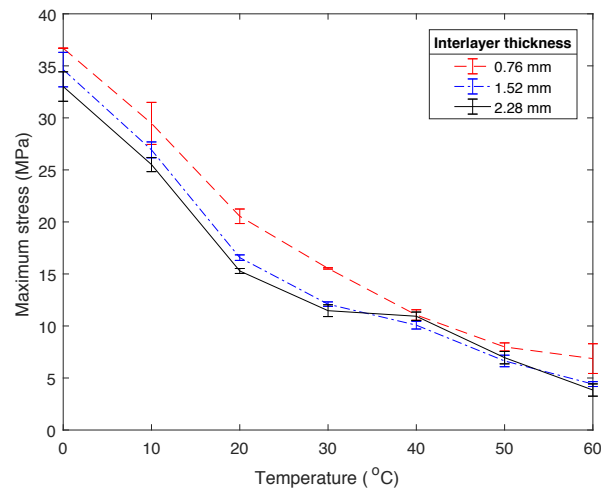


Figure 4.18: Maximum stress in random-crack tests.

The average load and average stress trends are similar to those for maximum load and maximum stress (Fig. 4.19 and Fig. 4.20). The average load and average stress measurement are useful here because averaging minimises the effect of the short duration peaks in the data, and hence are a conservative measurement in comparison to the maximum load and maximum stress.

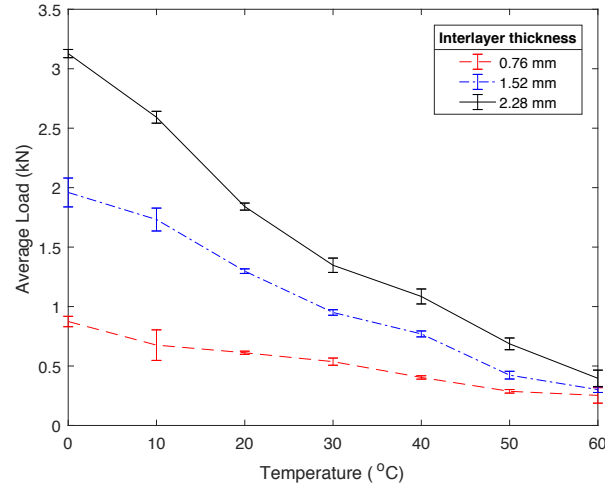


Figure 4.19: Average load in random-crack tests.

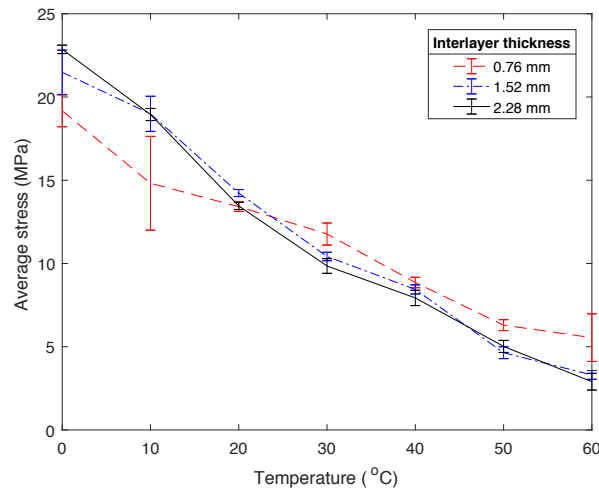


Figure 4.20: Average stress in random-crack tests.

The maximum and average stresses are fairly independent of interlayer thicknesses, however the failure strain, ε_f , is not (Fig. 4.21). At higher temperatures, greater failure strain is achieved. The specimens with a 0.76 mm interlayer achieved the lowest failure strain and the specimens with a 2.28 mm achieved the greatest.

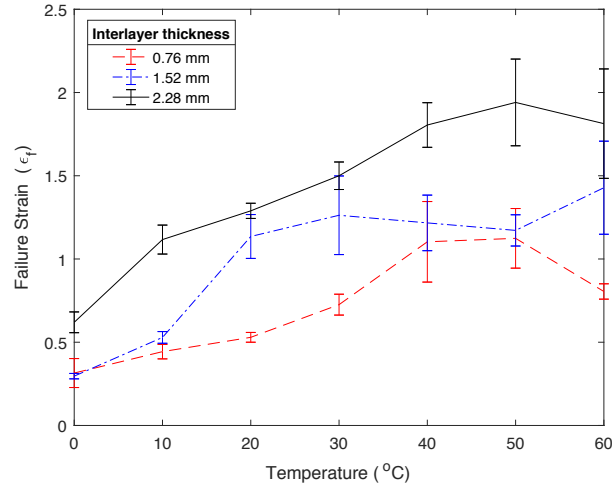


Figure 4.21: Failure strain for random-crack tests.

For all interlayer thicknesses, the total energy absorbed rises as temperature increases up to a maximum, then falls (Fig. 4.22). For the 0.76 mm interlayer specimens, this maximum peak occurs at 40°C, and for the 1.52 mm and 2.28 mm this occurs at 20°C and 10°C respectively. The specimens with a 0.76 mm interlayer thickness absorbed the lowest energy and the specimens with a 2.28 mm interlayer thickness achieved the greatest. This was consistent at all temperatures.

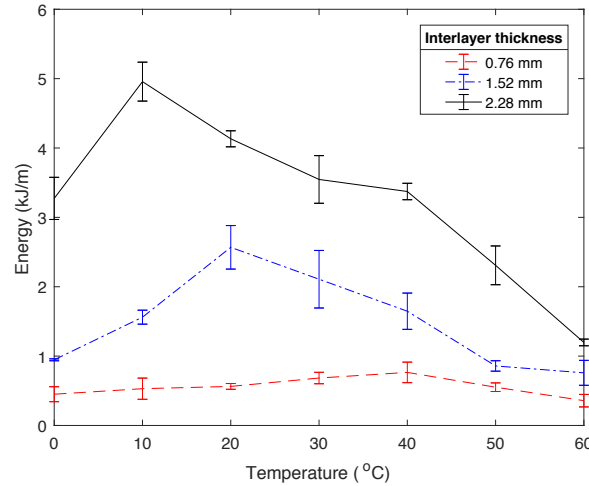


Figure 4.22: Energy per unit width for random-crack tests.

4.4. Discussion

4.4.1 Single-cracked tests

The glass transition temperature, T_g , of the PVB has been reported to be in the range of 15°C to 20°C [35, 39]. At lower temperatures, below T_g , the single-cracked tests showed a sharp rise in load at initial times during the test, prior to delamination. This is because the material behaves glassy and stiff. This stage is followed by a load plateau where the dominated failure mode is through delamination and de-bonding failure.

At the higher end of temperatures tested (above 40°C), where the material is rubbery and compliant, the load gradually increases as the test progresses, and reaches a maximum towards the end of the test. Taking the 40°C case as an example (Fig. 4.6). A plateau is reached initially; this is where the main mode of failure is adhesive (PVB - Glass bond). As the test progresses, since the PVB is compliant, the delaminated front rounds off (no longer as sharp as before), therefore requiring a much greater load to propagate (similar to blunting of the crack tip). The mode of failure at this stage is a mixture of adhesion (de-bonding) and cohesion (PVB - PVB bond). Finally, the de-bonded ligament can no longer support this load, it fails mainly cohesively and eventually leads to tearing.

In a peel test, a load, P , is applied at a peeling angle, θ , creating a slope

of the arm at the peeling front, θ_0 (Fig. 4.23). By reducing the peeling angle, the slope of the arm is also reduced. This leads to a greater de-bonding load [26]. The single-cracked test is similar to the peel test, except that the peeling angle is 0° , however the slope at the delamination front can vary depending on the compliance of the delaminated PVB ligament. This is because the stretching of the PVB creates a local angle prior to separating from the glass layer.

The increase in de-bonding load at lower temperatures can be explained by the fact that at lower temperatures, the PVB is less compliant (more stiff), therefore it is stretched less. This creates a thicker de-bonded PVB ligament, resulting in the angle at the peeling front being smaller than that of higher temperatures. This leads to a greater de-bonding load. The opposite is also true for high temperatures, where the material is more compliant therefore being stretched more, leading to a greater slope at the delamination front, and a lower de-bonding load. This is illustrated in Fig. 4.24. A similar argument also holds for thicker interlayers having a greater de-bonding load due to an increased effective thickness.

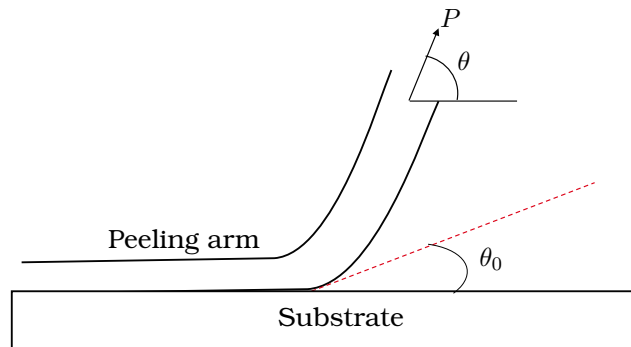


Figure 4.23: Peel test arrangement.

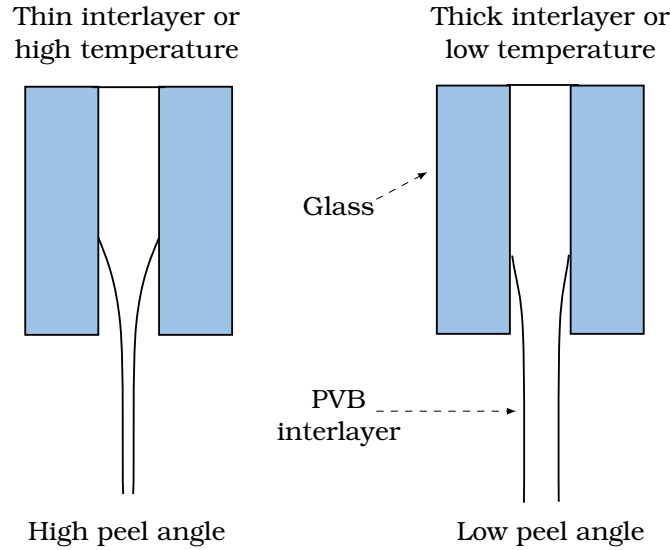


Figure 4.24: Side view of a single-crack tensile test.

At the lower and higher temperatures mentioned, greater values of strain in the PVB bridge are obtained (Fig. 4.13). In this instance, the strain is defined as the ratio between displacement and total delamination length. Greater values of strain means less delamination has taken place in comparison with the stretching of the PVB bridging ligament between the two parts of the specimen.

Lower strain (meaning more delamination) is expected when PVB is more stiff and less compliant at lower temperatures (below its transition temperature), thus a greater load can be transferred through it, leading to a greater delamination. However, this was not the case in the results obtained. This is because adhesive strength is increased at temperatures below the transition temperature, making the propagation of the delamination front more difficult. Tian *et al.* conducted tests on adhesion between glass and PVB interlayer [57]. They concluded that from transition temperature ($15^{\circ}\text{C} - 20^{\circ}\text{C}$) to -30°C , the adhesive tensile strength reduces by 65%, however the adhesive shear strength increases by 172%. In the single-cracked tests, the mode of de-bonding is a mixture of tensile and shear. However, this increase in adhesive shear strength, could be a cause of less delamination occurring.

At temperatures in excess of T_g , it was expected that when the material is more compliant, greater displacements would be observed -

but this was not the case due to reduced delamination. This is because as the PVB becomes more compliant, its load-carrying capabilities are reduced; leading to lower delamination and thus a shorter delaminated PVB ligament. An optimum is reached between 20°C – 40°C, where there is a balance between the adhesive strength and stiffness of the delaminated ligament.

The importance of choosing the correct interlayer thickness is apparent in the single-cracked tests (Fig. 4.14 and Fig.4.15). The thicker interlayer showed the lowest strain. Using the definition of strain for single-cracked specimens, this means a greater ratio of delamination to stretching of the interlayer is obtained for thicker interlayers. The thicker interlayer has a greater effective stiffness, therefore resulting in a smaller stiffness mismatch with glass (even though the difference is still large). This results in a lower strain in the delaminated PVB ligament and therefore greater delamination.

The thicker interlayer generally showed the greatest variability in results. In the manufacturing process, the thicker interlayers are made up of a multiple of the smaller interlayer thickness. So for the thicker interlayers, more thinner layers will need to be stacked. Thus introducing more chances of defects in the lamination quality and a possible source of uncertainty in the data.

A balance between the PVB stiffness and adhesive strength is required to reduce the chances of PVB tearing. By controlling the adhesion and transition temperature of the PVB, the desired optimum performance can be reached. As an example at lower temperatures, below T_g , if the adhesion is reduced in the manufacturing process, greater displacements can be reached, hence the blast performance in colder environments is improved.

4.4.2 Random-cracked tests

The failure mechanism of the single and random-cracked specimens are the same in the sense that when a load is applied, de-bonding occurs; then the de-bonded ligament is stretched until failure occurs usually by tearing of the interlayer. However, the single-cracked specimens cannot directly be related to the random-cracked specimens. This is because in the single-cracked tests, loading is perpendicular to the delamination front, whereas in the random-cracked tests, the angle at which the delamination front propagates varies in the specimen (Fig. 4.25).

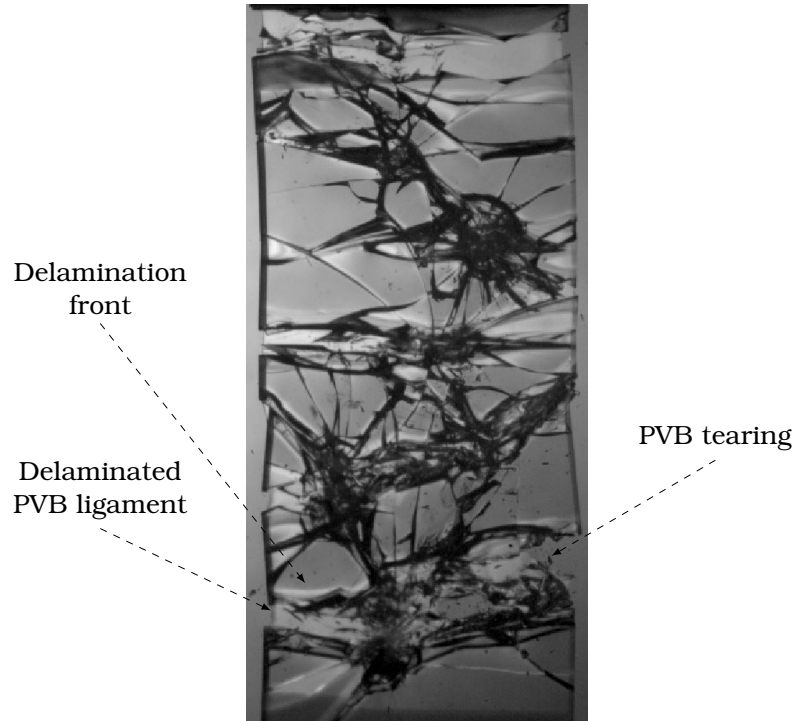


Figure 4.25: Random-cracked sample with 0.76 mm interlayer, tested at 20°C.

The behaviour of random-cracked specimens are more representative of the failure of laminated panels under blast loading than the single-cracked specimens. As a result, the main focus of the experimental program was put on random-cracked specimens. More repeat tests were performed on these specimens. From the low error bars on the random-cracks, it can also be deduced that the variability in crack pattern between specimens was not significant enough to affect the results. However, to ensure a greater consistency in future tests, a drop tower could be used to drop weights at a consistent height on the specimen.

The failure strain showed a general rise with increasing temperature (Fig. 4.21). In this case, the strain is defined as the ratio of displacement to original gauge length. The laminate is effectively being treated as a polymer with glass attached as extra mass. Therefore since at higher temperature, the polymer is more ductile, greater strain at failure is achieved. The failure strain for the specimens with a 1.52 mm interlayer stays almost constant at

temperatures above 20°C. It is unknown why this occurs.

Greater loads are observed at lower temperatures, due to the PVB being more stiff. Greater displacements are observed at higher temperatures, due to PVB being more compliant. This results in the optimum temperature at which maximum energy was dissipated being between the high and low temperature extremes. The fracture behaviour of ductile thermoplastics is typically thickness dependent [58]. This resulted in the optimum temperature at which the maximum energy is absorbed being different for the three interlayer thicknesses (Fig. 4.22). It should be noted that there are a lot of overlapping of error bars (e.g. 0.76 mm), so the optimum temperature is not a single definite temperature.

The tolerable stress is severely affected by temperature. As the temperature is increased from 0°C to 60°C, for the 0.76 mm, 1.52 mm and 2.28 mm interlayer thicknesses, the maximum stress decreases by 81%, 87% and 88% respectively. A graph which can be used as a conservative design guide based on the average stress data for random-cracks is Fig. 4.20. This should be used with a suitable safety factor. The results can be used as input into FE models combined with a failure criterion based on maximum stress and used for design. Furthermore, PVB is rate dependent, especially at high loading rates ([30, 33, 59, 60, 61]), therefore these data are only valid at the experimented strain rate (approximately 20 s⁻¹).

4.5. Conclusion

In this chapter the effects of temperature on the post-cracked response of laminated glass under high rate loading were explored. Experiments were conducted at a high strain rate in the laboratory on specimens consisting of a PVB interlayer laminated between 3 mm annealed glass plies. Specimens with three different interlayer thicknesses consisting of 0.76 mm, 1.52 mm and 2.28 mm were tested. Single-cracked and random-cracked tests were conducted at temperatures in the range of 0°C to 60°C in increments of 10°C.

The post-crack response of the laminated glass is influenced by the compliance of the PVB interlayer. The glass transition temperature of the PVB is just below room temperature. As a consequence, the compliance of the PVB is very temperature dependent, in the temperature ranges tested. Based on the results from the literature, the adhesion between the glass and PVB is temperature dependent below the transition temperature

(15°C – 20°C). The next chapter assess the effect of temperature on the adhesion between the glass and the PVB.

The results from this study indicate that temperature dependence of laminated glass is important. By designing for a certain load at room temperature, it cannot be assumed the laminated pane will still survive blast loading at different temperatures. If extreme temperatures (above 50°C or below 10°C) are required, it is recommend that the adhesion level should be modified to shift the temperature range of the optimum performance. Alternatively, the transition temperature of the PVB should be altered in the polymer forming process, thus allowing it to perform better at the extreme temperatures.

Interfacial Adhesion

5.1. Introduction

In this chapter, the effect of temperature and testing rate on the bond fracture toughness (energy dissipated per unit area of de-bonding), is presented, using experimental methods. The de-bonding load from the through-cracked tests in the previous chapter is used here, along with new experimental data to calculate bond fracture toughness. This is then extended through a finite element model to simulate the bonding behaviour for different loading rates and temperatures in laminated glass. The loading rate or temperature dependency in laminated glass affect the response in two ways. These two parameters will affect the material properties of the PVB whilst stretching, and will also affect the bond between the PVB and Glass [35].

5.2. Experiments

5.2.1 Experimental theory

Kinloch *et al.* have proposed a method for calculating the bond fracture toughness from peel tests on flexible laminates [26]. That method is based on an energy balance approach. In this chapter a similar method to that proposed by Kinloch *et al.* has been applied. However, the method has been modified so that it can be applied to the through-cracked tensile tests.

In the through-cracked tensile test, a tensile load is applied to the upper edge of the glass, while the bottom edge is anchored firmly (Fig. 5.1). Two competing mechanisms occur in this test. The first mechanism is the de-bonding of the PVB interlayer, and the second mechanism is the stretching of the interlayer. In this test, the load is carried by the PVB interlayer, as it undergoes stretching, whilst simultaneously, the PVB progressively de-bonds from the glass.

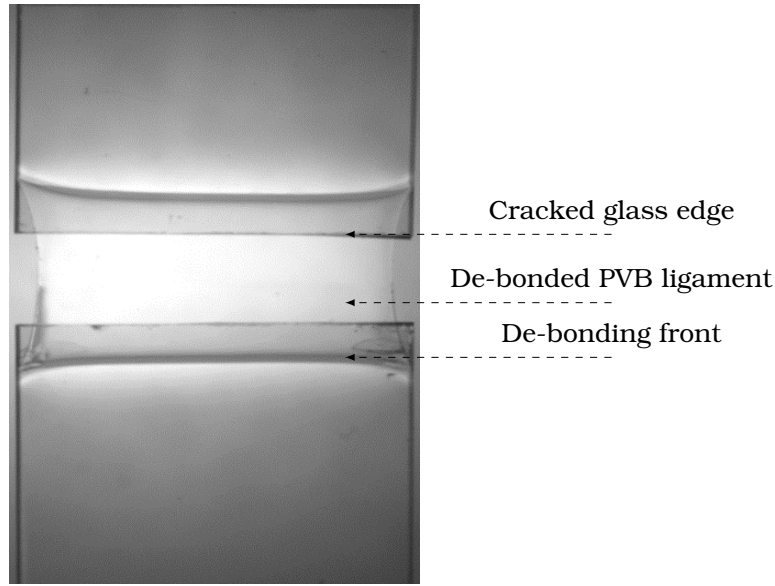


Figure 5.1: Through-cracked specimen with 1.52 mm PVB thickness, viewed through a polariscope (test temperature of 20°C at a test rate of 1 m/s).

The bond fracture toughness, G_a , which is in fact the de-bonding energy per unit area of de-bonding¹, can be calculated through an energy balance approach. For a through-cracked specimen with width b , which undergoes a de-bonding length of a , the bond fracture toughness can be calculated by Eqn. 5.1.

$$G_a = \frac{1}{b} \left(\frac{dU_{\text{ext}}}{da} - \frac{dU_s}{da} - \frac{dU_{\text{dt}}}{da} \right) \quad (5.1)$$

Where U_{ext} is the external work, U_s is the stored energy in the de-bonded PVB ligament and U_{dt} is the energy absorbed by the stretching of the de-bonded PVB ligament.

¹This is sometimes also referred to as the delamination energy per unit area.

The through-cracked tensile test gives only the external work. A further test is required to evaluate the other energy terms. A tensile test on pure PVB can be used to calculate the stored energy and the energy dissipated due to the stretching of the PVB. For a de-bonding load, P , interlayer thickness, h , and specimen width, b , which undergoes a displacement of $d\delta$, the energy terms can be calculated from Eqn. 5.2 and Eqn. 5.3.

$$dU_{\text{ext}} = P d\delta = P da \cdot \varepsilon_a \quad (5.2)$$

$$dU_s + dU_{\text{dt}} = bh da \int_0^{\varepsilon_a} \sigma d\varepsilon \quad (5.3)$$

where σ and ε_a are the tensile stress and tensile strain in the de-bonded interlayer ligament.

The de-bonding load, P , is obtained from the through-cracked tensile experiments. This is defined as the load at which de-bonding propagates at a steady state. According to the ESIS protocol [56], the de-bonding load is the lowest load plateau observed in the graph of load against displacement from the through-cracked tensile tests (Fig. 5.2).

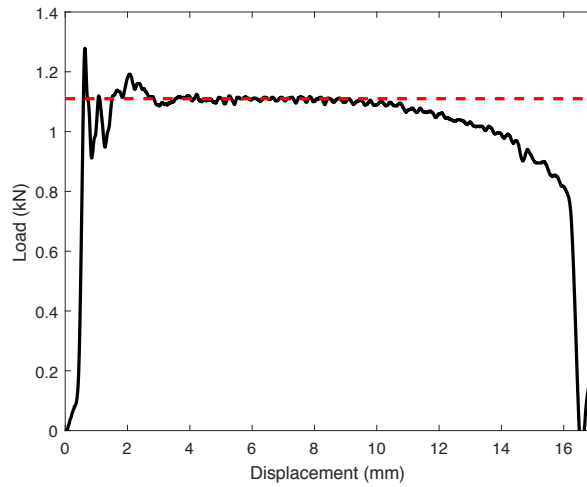


Figure 5.2: Load (P) vs displacement ($d\delta$) at 20°C for 0.76 mm PVB thickness for through-cracked test at 1 m/s (20 /s) with the de-bonding load shown in red dashed line.

The tensile strain in the PVB ligament, ε_a , is calculated from the tensile test carried out on pure PVB material. From the graph of stress against strain, this is the strain corresponding to a stress caused by the de-bonding load P .

The bond fracture toughness can now finally be determined by substituting Eqn. 5.2 and Eqn. 5.3 into Eqn. 5.1. In the through-cracked tensile test four new crack surfaces are formed, as a load is applied. The final bond fracture toughness, G_a , is therefore, a quarter of the value calculated in Eqn. 5.4.

$$G_a = \frac{P}{b}(\varepsilon_a) - h \int_0^{\varepsilon_a} \sigma d\varepsilon \quad (5.4)$$

5.2.2 Test specimens

The experiments were carried out on two different specimen types. The first were laminated glass specimens and the second were of pure PVB. The laminated glass specimens consisted of a PVB interlayer sandwiched between two 3 mm annealed glass plies. Three different PVB thicknesses of 0.76 mm, 1.52 mm and 2.28 mm were used. The pure PVB specimens were all 0.76 mm thick.

Through-cracked tests

The laminated glass specimens were used for the through-cracked tensile tests. The specimen size was 150 mm long by 60 mm wide. The specimens were prepared to architectural grade by Kite Glass. The PVB used for the lamination was manufactured by Everlam (52D PVB).

The first stage in the preparation process included bonding aluminium tabs to the gripping ends of the glass using Araldite 2021 adhesive. Before the adhesive was applied, the aluminium and glass bonding surfaces were grit blasted to create a rougher surface and this was followed by cleaning the surfaces with acetone.

The second stage in the preparation process included creating a single coincident crack on each surface of the specimen. This crack was created by scoring the glass using a diamond tip scribing tool, followed by a gentle tap using a hammer on the side opposite to where the score was created. This procedure was carried out on both sides of the glass.

A total of 19 through-cracked specimens were tested at a loading rate of 1 m/s for a temperature range of 20°C – 60°C (Table 5.1). The experiments previously carried out by Hooper [33], provided data for a range of testing rates (0.01 m/s to 10 m/s), at 20°C.

Table 5.1: Number of tests for single-cracked specimens.

Thickness of PVB (mm)	Temperature (°C)				
	20	30	40	50	60
0.76	2	1	1	1	1
1.52	2	1	1	1	2
2.28	1	2	2	1	-

Unlaminated PVB tests

Tensile tests were carried out at corresponding temperatures and loading rates on pure PVB. The specimens were prepared by following the guidelines in ASTM standard D 638-02a [62]. The PVB specimens were punched out with a die (Figure. 5.3) from a sheet of pure PVB with a thickness of 0.76 mm.

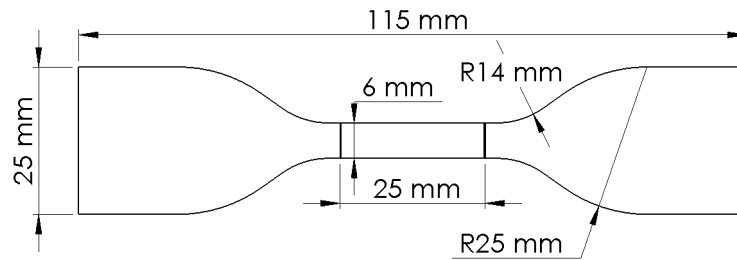


Figure 5.3: PVB tensile test specimen dimensions.

A total of 15 specimens were tested. Three repeat tests at each temperature were performed at a rate of 1 m/s. Similar to the through-cracked tests, for the experiments at 20°C for a range of testing rates (0.01 m/s to 10 m/s), results were used from the experiments previously carried out by Hooper [33].

5.2.3 Experimental methods

In chapter 4 the testing method was explained. However, to allow the current chapter to be self-contained, the testing method is briefly repeated. The through-cracked experiments were carried out on a high rate Instron servo-hydraulic testing machine (Fig. 5.4). The specimen was gripped by the aluminium tabs at both ends, using dowel pins. The bottom of the

specimen was connected to a high rate piezoelectric load cell (PCB model 222B). The top of the specimen was connected to a lost-motion device and eventually to the actuator of the testing machine. The lost-motion device ensures that the loading train is accelerated to the correct velocity, prior to any load being applied to the specimen. The displacement was taken as the cross-head displacement. For the experiments at different temperatures, to achieve uniform temperature across the specimen, the specimen was placed inside an Instron environmental chamber at the desired temperature and allowed to soak for 24 hours.

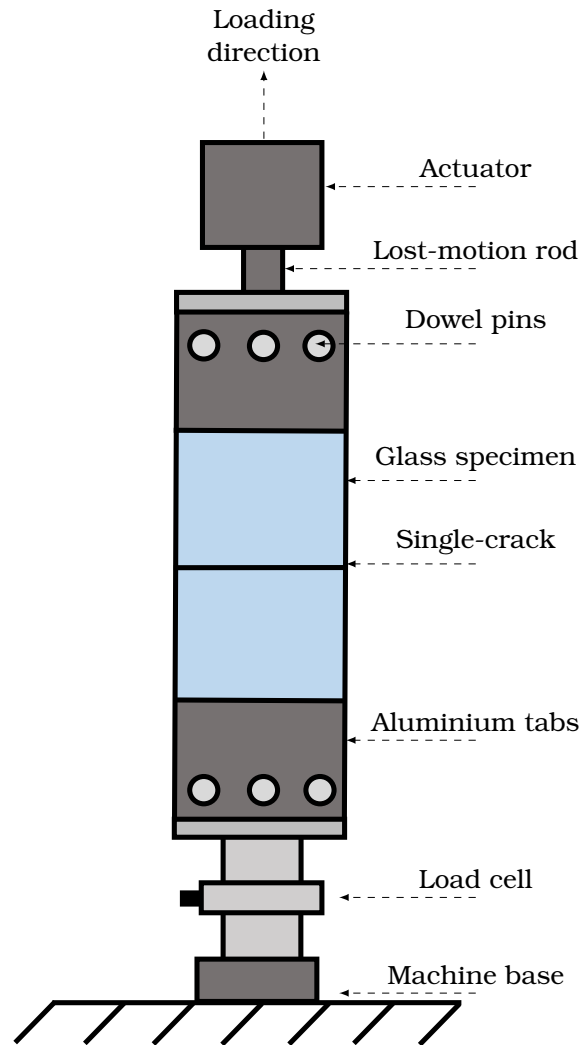


Figure 5.4: Through-cracked tensile test setup.

For the tensile tests on pure PVB, the same testing machine with a similar test setup was used (Fig. 5.5). The load was measured using a different piezoelectric load cell to that in the through-cracked test due to the lower loading demands (PCB model 208A203). The displacement was measured using optical techniques. The range of the gauge length was marked using two lines. High speed video recording was used to record the movement of the lines. Image processing techniques on MATLAB were

then used to track the movement of marked lines between every frame. This allowed the pixel displacement to be measured and using a known distance in the image, the displacement in mm was calculated.

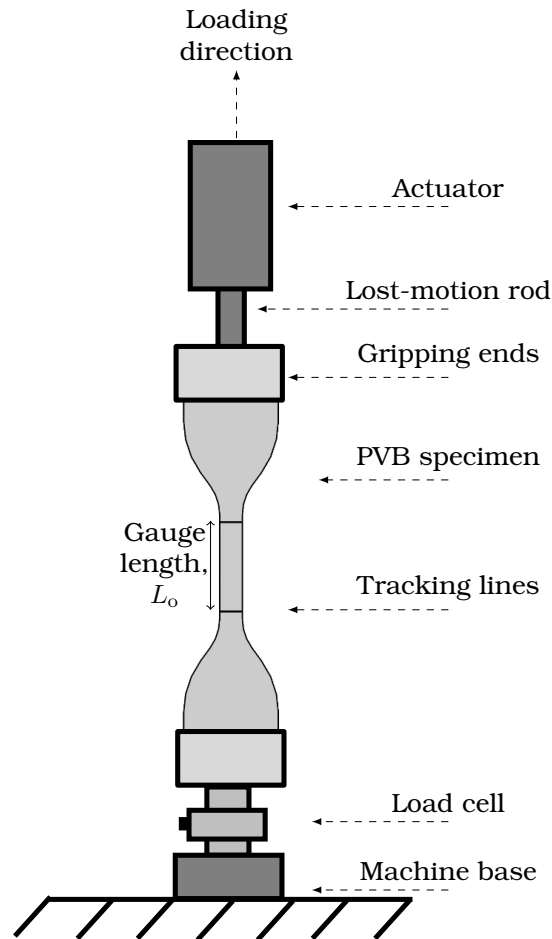


Figure 5.5: Pure PVB tensile test setup.

5.2.4 Experimental results

Through-cracked tests

The de-bonding load, at a loading rate of 1 m/s, showed a decrease with increasing temperature (Fig. 5.6). The thickness of the interlayer also proved to be important in the de-bonding process. As expected, the thicker interlayers carry a greater load during the de-bonding period at different

temperatures. As the temperature increases, the thickness of the interlayer becomes less significant. For example, at 20°C, the difference between the de-bonding load for different thicknesses is much greater than the difference at 50°C or 60°C. Note that at conditions where a repeat test was carried out, the variations were calculated and represented as error bars on the graph.

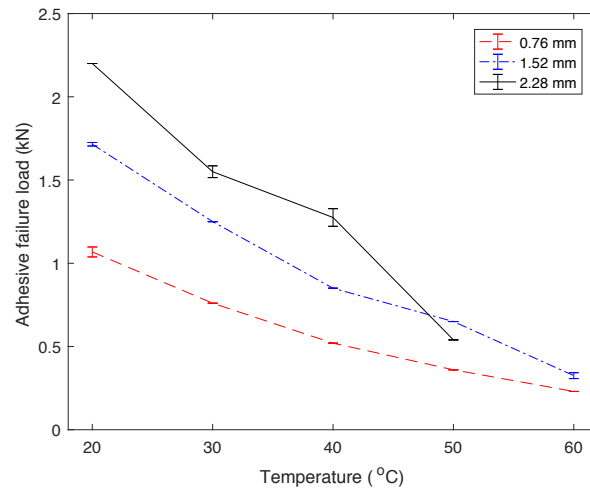


Figure 5.6: De-bonding load against temperature at three different interlayer thicknesses at 1 m/s (20 /s).

In contrast to the effect of temperature, as the loading rate was increased, the load required for de-bonding to propagate showed an increase (Fig. 5.7). At different rates, the thicker interlayers consistently showed that they are able to carry a greater load during the de-bonding period. At a given temperature, as the strain rate increases, the interlayer thickness becomes more significant in determining the de-bonding failure load. The effect of increasing the rate at a given temperature is in fact very similar to decreasing the temperature at a given test rate.

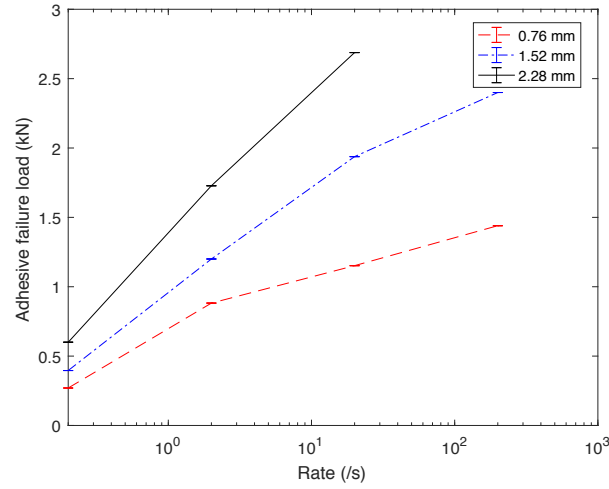


Figure 5.7: De-bonding load against test rate at three different interlayer thicknesses at 20°C.

Unlaminated PVB tests

The experiments on pure PVB over a range of different temperatures and loading rates reveal a bigger picture of the behaviour of the polymer. At a constant rate of 1 m/s, the PVB is seen to be temperature dependent (Fig. 5.8). The behaviour of the polymer can be split into small strain and large strain. At small strains, for temperatures 20°C to 40°C, the stiffness was significant with a linear stress-strain relationship. At temperatures of 50°C and 60°C, the PVB showed little initial stiffness with almost no linear relationship at small strains. At large strains, the polymer showed typical hyperelastic properties at all temperatures.

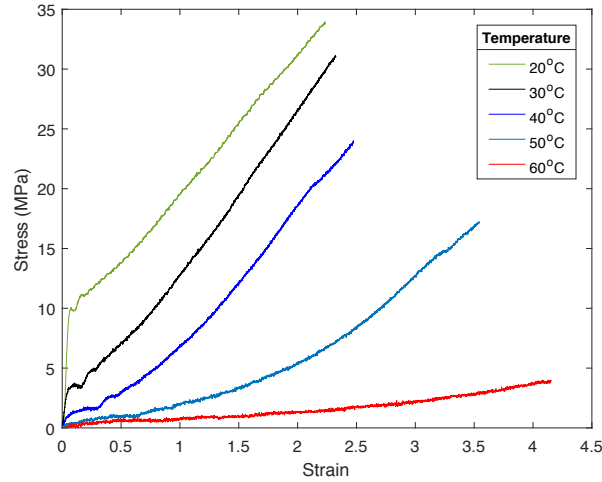


Figure 5.8: Stress against strain for pure PVB tensile tests at different temperatures at a loading rate of 1 m/s (20 /s). Note: results shown for one test only; other repeat tests showed very similar results.

The results at different loading rates showed similar behaviour to the results at different temperatures. The response can be considered in two different phases; small strains and large strains. At small strains and higher testing rates, the PVB is stiffer with a linear stress-strain relationship. At the lowest testing rate (0.01 m/s), there was almost no initial linear trend and very low stiffness. At large strains, all testing rates behaved very similarly and the typical expected hyperelastic response was observed.

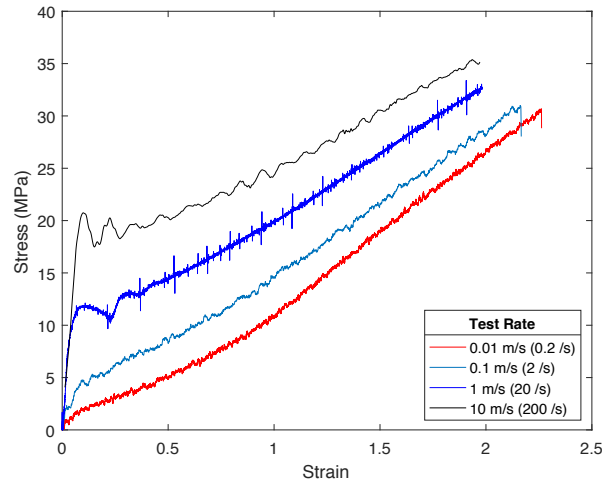


Figure 5.9: Stress against strain for pure PVB tensile tests at different test rates at 20°C. Note: results shown for one repeat only, other repeats showed very similar results.

Bond fracture toughness

The bond fracture toughness was calculated based on the through-cracked tensile tests and at every repeat of the tensile tests on pure PVB. A standard deviation was calculated at every test condition and plotted as error bars on the graphs presented in this section. It should, however, be noted that only a limited number of repeat tests were performed (three repeat tests).

In the temperature range tested at a rate of 1 m/s, the bond fracture toughness was not sensitive to temperature (Fig. 5.10). The fracture toughness appeared to become dispersed for different thicknesses as the temperature was increased from 20°C to 60°C. The 1.52 mm interlayer consistently showed greater values of fracture toughness compared to the 0.76 mm interlayer. Despite this fact, however, a conclusion cannot be made that the adhesion is thickness dependent. This is because as the interlayer thickness was increased to 2.28 mm, a similar increasing trend could not be observed.

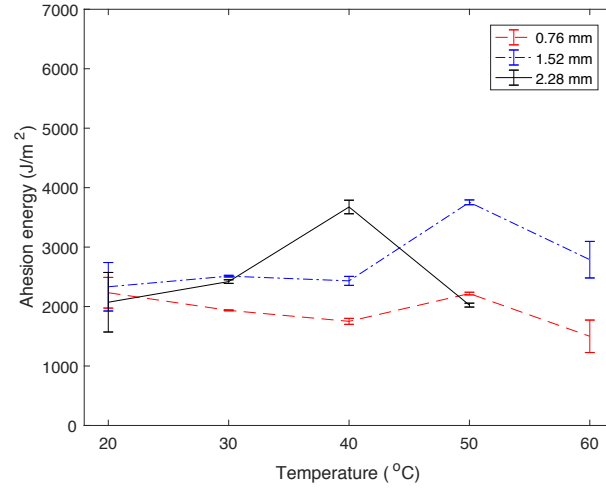


Figure 5.10: Bond fracture toughness against temperature at three different interlayer thicknesses at 1 m/s (20 /s).

In contrast to the behaviour at varying temperature, at a loading rate up to 20 /s, the bond fracture toughness did show to be rate-dependent (Fig. 5.11). The fracture toughness showed an increase as the rate was increased from 0.2 /s to 20 /s, however, as the rate was further increased to 200 /s, the fracture toughness remained almost in the same range as the results for 20 /s. The results at different test rates also show that fracture toughness is thickness independent.

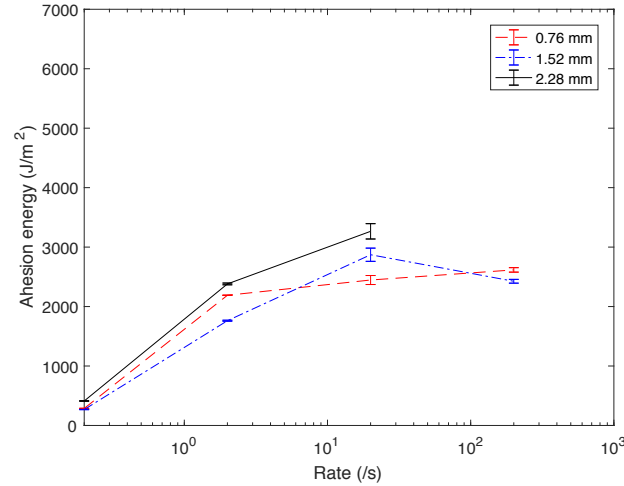


Figure 5.11: Bond fracture toughness against test rate at three different interlayer thicknesses at 20°C.

5.3. Modelling

5.3.1 Modelling techniques

The adhesion between the glass and interlayer in through-cracked tensile experiments was modelled using FE methods. The de-bonding load was calculated from the model analysis and was compared to the experimental results. This was carried out for a range of loading rates and temperatures.

Cohesive zone model

The adhesion between the polymer and the glass was simulated using the cohesive zone model (CZM). The CZM is based on the tractions and the separations between the two adhered interfaces. The traction and the separation can be present in the normal and both tangential directions. Chandra *et al.* have reviewed the literature on the different cohesive zone models such as linear, bi-linear and exponential [63].

Prior to proceeding with the modelling, it is important to define a relationship between the traction and the separation. A bi-linear relationship has been chosen in this case because of its simplicity and also due to the fact that it has been successfully implemented in the literature for various materials [64, 65]. The bi-linear relationship is defined by using

a set of parameters, namely, the bond fracture toughness, G_a , a stiffness factor, k , and a maximum traction, $t^{\max}$.

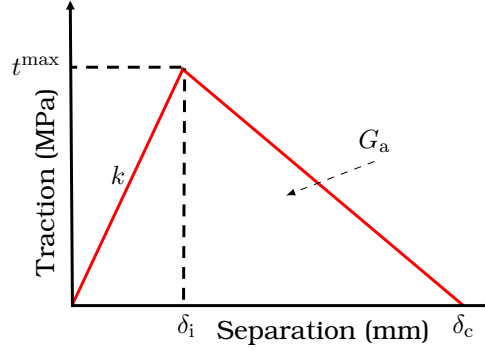


Figure 5.12: Traction–Separation graph (where δ_i is the separation at initiation of de-bonding and δ_c is the separation at complete failure).

The cohesive zone model can be implemented into Abaqus using two methods. The first method is using cohesive elements. Cohesive elements are a set of elements added in the model between two connecting surfaces to represent the adhesion. In cohesive elements, issues such as mesh refinement also become important. The second method is using cohesive surfaces. Cohesive surfaces can be easily implemented in Abaqus by defining contact interactions and cohesive interaction properties.

In the model presented in this chapter, the cohesive surface method was used. The cohesive surface method is more representative of the reality, as in the lamination process no adhesive is used between the glass and the PVB. Whereas, the cohesive element method is more suited to adhesives with a finite thickness.

The CZM was implemented in Abaqus using damage modelling techniques to simulate the de-bonding [66]. When using damage modelling for the failure between two surfaces (glass and PVB in this case), it is important to define the damage initiation and the damage evolution. For damage initiation, the maximum stress criterion was selected (Eqn. 5.5). In this criterion, damage occurs once the maximum contact stress ratio in any direction reach unity. The normal direction is denoted with a subscript I, and the two shear directions with subscripts II and III.

$$\max \left\{ \frac{t_I}{t_I^{\max}}, \frac{t_{II}}{t_{II}^{\max}}, \frac{t_{III}}{t_{III}^{\max}} \right\} = 1 \quad (5.5)$$

The damage evolution was defined using a energy approach. The energy dissipated per unit area of bond failure, G_a , in the power law form was used (Eqn. 5.6).

$$\left(\frac{G_I}{G_{Ia}} \right)^\alpha + \left(\frac{G_{II}}{G_{IIa}} \right)^\alpha + \left(\frac{G_{III}}{G_{IIIa}} \right)^\alpha = 1 \quad (5.6)$$

where α is the power law exponent (set as 1 here), and I, II and III represent the failure in normal and the two tangential directions respectively.

The three mentioned parameters were assigned a suitable value. The bond fracture toughness, G_a was taken from the experimental value calculated. The stiffness factor, k , in this case does not have a physical representation. It is common practice to choose a very high value, such that the final obtained results do not vary if the value of k is increased further [64]. A value greater than $5 \times 10^{12} \text{ N/m}^3$ sufficed in this case. The maximum traction, $t^{\max}$, was chosen such that the output load would match that of the experimental results for the case of the 20°C at 1 m/s . An iteration was carried out; following a convergence, $t^{\max}$ was found to be 2 MPa and was kept at this constant value at all temperatures and testing rates.

The issue of mixed-mode fracture was next considered. It is also important to establish the magnitude of the parameters G_a , k and $t^{\max}$ present in the normal and two tangential directions. In this study looking at mixed-mode was important because the bond fracture toughness obtained from the experimental results was used to define bond failure in the FE model.

In pure tensile and pure shear tests it is appropriate to define the failure mode (mode I for tension, mode II or mode III for the two shear directions). When dealing with de-bonding of soft polymers, the situation is quite different. In the through-cracked tensile tests the loading angle is parallel to the separating surfaces. So the fracture toughness calculated from this should ideally be pure mode II. In this case, however, the local angle at which the adhesive separates from the substrate (in this case PVB separating from glass) is different to the loading angle. Kinloch *et al.* have also demonstrated this for a variety of flexible laminates [26].

When dealing with peeling of flexible laminates, it is therefore difficult, if not impossible to conduct a test which is pure mode I or pure mode II.

Kinloch *et al.* conducted peel tests at a range of loading angles and proposed that the bond fracture toughness in mode I could be equal to mode II [26]. In the current study, when inputting the experimental parameter into the model, it was established that it is not necessary to distinguish between the normal and tangential directions. The following has therefore been assumed: $G_{Ia} = G_{IIa} = G_{IIIa} = G_a$. Similarly for the tractions it was assumed that $t_I^{\max} = t_{II}^{\max} = t_{III}^{\max} = t^{\max}$. Where the subscripts I, II and III denote the maximum values of the contact stress when the separation is purely normal to the peeling surface, or purely in one of the two directions of shear to the peeling surface.

As concluded from the experimental results (Fig. 5.10), the bond fracture toughness is temperature insensitive. Therefore an average was taken at all temperatures and thicknesses to be 2488 J/m^2 and this was used as the G_a value in the model at all temperatures at a rate of 1 m/s (20 /s). From the experimental results (Fig. 5.11), it was also evident that at a temperature of 20°C the fracture toughness did not vary between the rates of 1 m/s (20 /s) to 10 m/s (200 /s), thus, the same value of 2488 J/m^2 was used. For the rates less than 1 m/s (20 /s) at 20°C , however, the fracture toughness did vary. Therefore, the experimentally calculated values were averaged for all interlayer thicknesses. Therefore, G_a was taken as 322 J/m^2 and 2110 J/m^2 for rates of 0.01 m/s and 0.1 m/s respectively.

FE model

The FE model was created in Abaqus. The problem was simplified by using a half model in the lengthwise direction at the location where there was a coincident crack through the glass in the experimental specimens (Fig. 5.13). A three layered model was created having a PVB layer sandwiched between two glass layers. Bonding properties were defined at the interfaces between the glass and the PVB. The loading was applied to the top edge of the glass. A symmetry boundary condition was applied to the bottom edge of the PVB.

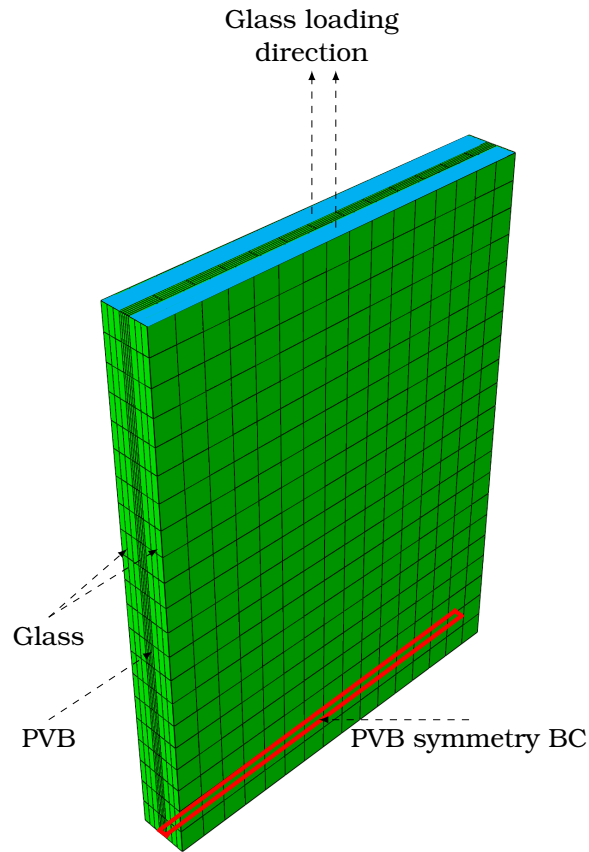


Figure 5.13: Illustration of FE model with boundary conditions (BC).

The mesh was created using an 8-node linear brick element (C3D8). A mesh sensitivity analysis was conducted by running the model at three different mesh densities, and by assessing the time taken to complete the analysis and the accuracy of the results. Following this mesh sensitivity analysis, a total of 1001 elements were used on each glass ply with three elements through the thickness. For the PVB, 1365 elements were used with five elements through the thickness.

Material model

The material properties used are summarised in Table 5.2. For the glass a linear elastic material model was used and for the PVB a viscoelastic material model developed by Del-Linz was used [61].

Table 5.2: Material model summary.

Material	Model	Parameter
Glass	Linear elastic	$\rho = 2530 \text{ kg/m}^3$, $E = 70 \text{ GPa}$, $\nu = 0.22$
PVB	Viscoelastic	$\rho = 1100 \text{ kg/m}^3$ (for material constants see Tables 5.3 and 5.4)

Table 5.3: Material constants for reduced polynomial hyperelastic model.

Constant	up to 8 /s	above 20 /s
$C_1(\text{MPa})$	40.7	311
$C_2(\text{MPa})$	75.7	0.0112
$C_3(\text{MPa})$	0	1.3

Table 5.4: Prony series constants for reduced polynomial viscoelastic model.

Constant	up to 8 /s	above 20 /s
$\tau_1(\text{s})$	1×10^{-4}	1×10^{-6}
$\tau_2(\text{s})$	1×10^{-3}	1×10^{-5}
$\tau_3(\text{s})$	1×10^{-2}	1×10^{-3}
$\tau_4(\text{s})$	0.1	0.1
$\tau_5(\text{s})$	1	10
$\tau_6(\text{s})$	100	1000
g_1	0	0
g_2	0.898	0.948
g_3	0	0.0151
g_4	0	0.00112
g_5	0	0
g_6	0.102	0

To run the model at different temperatures and rates, two different approaches were taken. The loading rate-dependency was accounted for in the material model, by simply adjusting the loading rate being applied to the glass. The temperature dependency, however, was treated differently. Time-temperature superposition was used to predict the behaviour of the PVB at different temperatures, by shifting the test rate.

The PVB interlayer is an amorphous polymer. In amorphous polymers, it is possible to predict the mechanical behaviour at temperatures above that of transition based on the constants from the Williams-Landel-Ferry (WLF) equation [67]. Hooper has obtained these constants based on experimental results [39]. Based on the data collected by Hooper, a time-temperature superposition was used to represent an increase in

temperature by a decrease in test rate. According to this, in order to represent an increased temperature from 20°C to 30°C, the loading rate would have to be reduced by a factor of approximately 16. For a further temperature increase to 40°C, the loading rate would be reduced by a factor of 10. At 40°C, the rate at which the simulation was carried out was 0.1 /s. The material model developed by Del-Linz begins to break down at rates below 0.01 /s [68], therefore, higher temperatures of 50°C and 60°C have not been modelled, because this would have required the model to be run at rates of 0.01 /s and lower, producing inaccurate results.

5.3.2 Modelling results

The modelling was carried out using the methods outlined in section 5.3.1. Fig. 5.14 displays a sequence of the FE model at a loading rate of 1 m/s. Similar to the experiments, the PVB interlayer undergoes two different mechanisms. The interlayer deforms whilst at the same time de-bonding and separating from the glass.

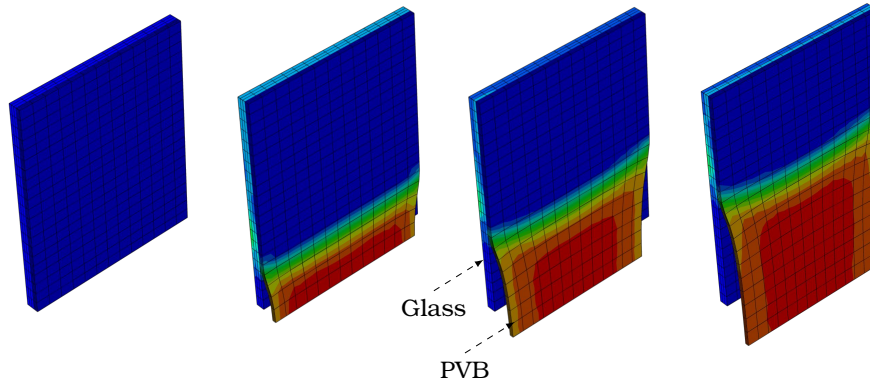


Figure 5.14: Sequence of FE model illustrating the de-bonding of PVB from glass (with front glass removed for clarity) at a rate of 1 m/s (20 /s).

The developed FE model was applied to a range of testing rates. The de-bonding load was extracted from the model and compared to the experimental results. For the test strain rates in the range of 0.2 /s to 200 /s, the FE results were in good agreement with experimental results (Fig. 5.15). The FE results can be described by Eqn. 5.7.

$$P_{FE} = 0.709R_t^{0.227} \quad (5.7)$$

Where R_t is the strain test rate and P_{FE} is the predicted de-bonding load from the FE results.

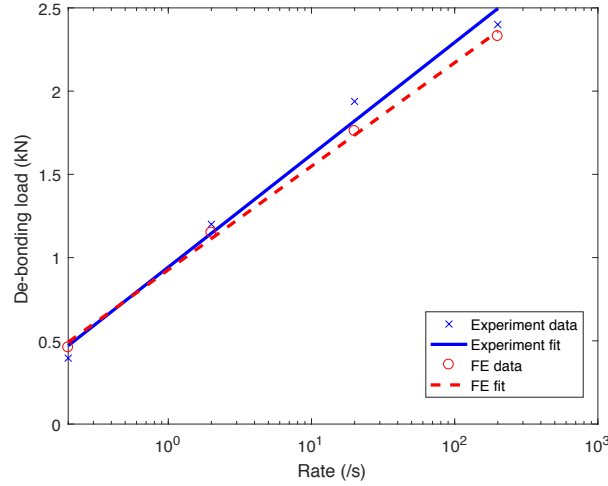


Figure 5.15: Comparison of the de-bonding load between experimental results and FE model at 20°C at different strain rates for an interlayer thickness of 1.52 mm.

The FE model developed for different test rates, along with time-temperature superposition was used to predict the de-bonding load at different temperatures. This model analyses could only be carried out up to a temperature of 40°C because at higher temperatures than this, the model would have needed to be run at a rate lower than the material model was validated for. For the temperatures at which the FE model was used (20 – 40°C), the FE model results showed good agreement with experimental results. The FE results can be described by Eqn. 5.8.

$$P_{FE} = -0.033T_t + 2.33 \quad (5.8)$$

Where T_t is the temperature and P_{FE} is the predicted de-bonding load from the FE results.

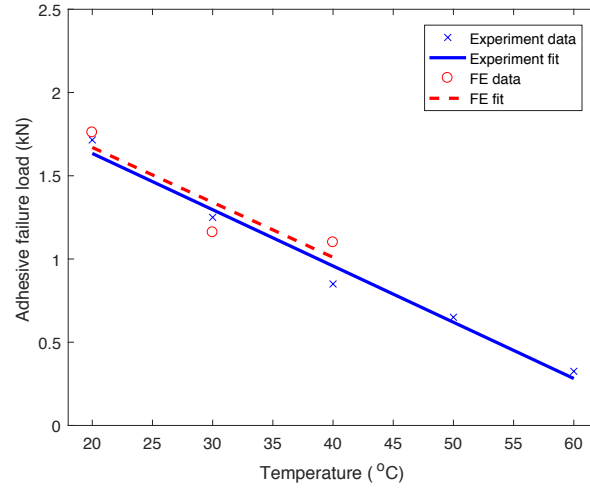


Figure 5.16: Comparison of de-bonding load between experimental results and FE model at a rate of 20°C at different temperatures for an interlayer thickness of 1.52 mm.

As explained in the section 5.3.1, the stiffness factor, k , used in the cohesive zone model can be set so large such that it does not result in unwanted compliance in the model. There is a threshold value for k in which any further increase will not result in a change of the de-bonding load (Fig. 5.17). In this case this value was $5 \times 10^{12} \text{ N/m}^3$. As k is increased above $5 \times 10^{12} \text{ N/m}^3$, there is no significant change in the de-bonding load plateau.

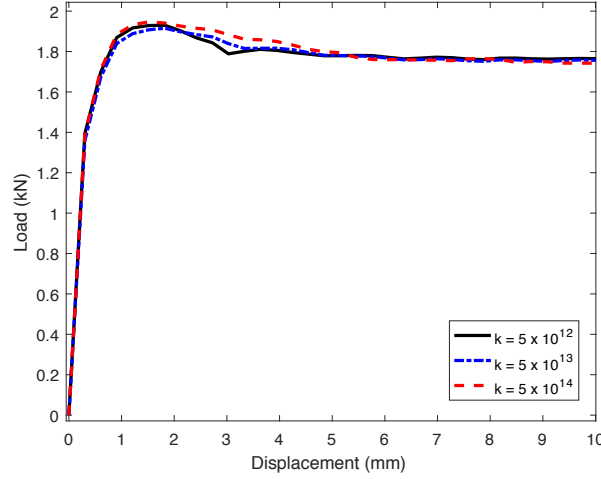


Figure 5.17: Independence of k shown at a rate of 1 m/s (20 /s) at a temperature of 20°C.

5.4. Discussion

The through-cracked tensile tests revealed that the de-bonding load is dependent on temperature, loading rate and PVB thickness. As the temperature increases, the polymer softens and stiffness reduces, and this leads to a lower de-bonding load. As the loading rate increases, the stiffness of the PVB interlayer increases, and a greater de-bonding load is observed. In addition, as the thickness of PVB increases, the effective stiffness of the interlayer increases, leading to a greater de-bonding load.

In all the three cases of temperature, rate and thickness, the key parameter influencing the de-bonding load is the stiffness of the PVB interlayer. As the polymer stiffens, the local angle at which the polymer separates from the glass reduces. This local angle is different to the loading angle due to the deformability of the PVB. Similar observations regarding the relationship between the local separating angle and the de-bonding load have also been reported in the literature [26].

In the tensile tests on pure PVB, the polymer had a low stiffness, for low loading rates at 20°C and also for high temperatures at a loading rate of 1 m/s. Therefore, the PVB showed almost no initial stiffness at small strains. This suggests the viscous behaviour of the polymer is the decisive factor in determining the small strain response. At higher strain,

however, it is mostly the general elastic properties of the polymer which determine the response. This observation was consistent at all tested rates and temperatures, with the exception of 50°C and 60°C at 1 m/s. This behaviour was because above 50°C the polymer becomes very soft and its load carrying capabilities are affected.

The experimental results showed that the bond fracture toughness is loading rate dependent, however, this was up to a strain rate limit (20 /s) and any further increase in rate (up to 200 /s) did not result in a change in fracture toughness. This stability limit in toughness could be due to two distinct reasons. At higher rates the polymer is stiffer and therefore the stretching of the polymer is less likely to cause discrepancies in the calculation of the fracture toughness. Hence, the fracture toughness is more consistent at higher rates and therefore raising questions about the rate-dependence of the de-bonding process. Alternatively, the bond between the glass and PVB may be truly rate-dependent and experimental uncertainties at the highest strain rate (200 /s) may have caused this result. This is because at the highest test rate, vibrations superimposed on the results of the through-cracked tensile tests. In the peeling of flexible laminates, Kinloch *et al.* consider the rate dependence of the bond fracture toughness as normal because the fracture toughness is a factor of the local plastic or viscoelastic energy dissipation in the zone ahead of the peeling front [26].

In contrast to the loading rate effects, the experimental results showed that the bond fracture toughness between the PVB and glass is independent of temperature in the range of 20°C – 60°C. It is still possible that the bond between PVB and glass is temperature-dependent over a greater temperature range. At temperatures above 60°C the PVB softens significantly and almost all the energy will contribute into deforming the polymer rather than progressing the de-bonding front. The through-cracked tensile test is thus not a suitable testing method at higher temperatures. A different approach should therefore be taken; a peel test could be conducted with a reinforced backing of the PVB peeling arm. The reinforced backing would restrict the stretching of the PVB and promote separation between the PVB and glass layer.

Furthermore, as the temperature is increased, the bond fracture toughnesses become more dispersed between the three interlayer thicknesses. This could be because at higher temperatures the polymer is more compliant and is therefore able to deform more. This resulted in

more energy to be dissipated in the deformation of the PVB (depending on thickness) rather than the steady state propagation of the de-bonding front.

In addition to the effects of rate and temperature, the effect of thickness on the bond fracture toughness was also considered in the experimental results. The fracture toughness showed to be independent of interlayer thickness. In the peel test, the independence of the peeling arm thickness from the fracture toughness has been seen by various researchers [26, 65]. This is important because it shows the bond fracture toughness is geometry independent to some extent. However, it could also be the case that the thickness must be below a threshold value for the fracture toughness to be severely affected and trends to be clearly identified.

In the FE model for different testing rates, the de-bonding load showed good match with the experimental results. This is due to two reasons. The first reason is that a good viscoelastic model was implemented in FE. The second reason is that the constants used for the CZM to model the interface adhesion were chosen correctly.

The time-temperature superposition worked well and in the temperature range of $20^{\circ}\text{C} - 40^{\circ}\text{C}$ FE results at different temperatures showed a good match in comparison to the de-bonding loads obtained from the experimental results. Time-temperature superposition had limitations because it could not be implemented at temperatures above 40°C because the rate would have had to be reduced to below 0.01 /s , at which the material model is no longer valid at [68].

5.5. Conclusions

In this chapter a set of experiments was conducted on single-cracked laminated glass and on pure PVB polymer. Fracture mechanics methods were used to calculate a bond fracture toughness. This energy value along with cohesive zone modelling techniques was used to develop a FE model for the separation of the PVB from the glass.

In the temperature range tested ($20^{\circ}\text{C} - 60^{\circ}\text{C}$) it is concluded that the bond is not temperature dependent at a constant loading rate. This is important in design applications because as far as adhesion is concerned, the design assumptions at room temperatures are still valid at higher temperatures. It can be assumed that the same amount of cracked glass fragments are attached at higher temperatures in comparison to initial

design. However when considering the overall response of the laminated glass pane, factors such as the temperature dependence of the polymer deformation must also be considered.

In addition, at a constant temperature, the adhesion is strain rate dependent up to strain rates of 20 /s. Above the strain rates of 20 /s the adhesion remains constant to 200 /s.

During blast loading, partial glass de-bonding allows the PVB interlayer to have longer ligaments which stretch and absorb the energy from the blast wave. This is important when designing the thickness of the glass plies. A thicker glass ply, will crack at a greater failure strength, which means smaller number of crack initiation sites. Therefore this results in a smaller total length of de-bonded PVB ligament which is available to absorb the energy from the blast wave.

The FE model is able to simulate the interface behaviour at a range of strain rates and with assistance from time-temperature superposition, the model is able to operate at a range of temperatures. This model was validated against experimental data at a range of temperatures and testing rates, thus, it can serve as a design tool. This is an important development which allows the de-bonding of laminated glass to be studied. In blast loading, other factors such as deformation of the PVB and cracking of the glass are also important. This model can be used alongside understanding of the cracking of glass and deformation of the PVB to predict failure of laminated glass subject to blast loading.

Prediction of Response

6.1. Introduction

This chapter revisits the traditional methodology for predicting the response of glazing systems subject to blast loading. The *single-degree-of-freedom* (SDOF) model has been the early and the current design methodology used in the prediction of response in structures subject to blast loading. A finite element (FE) model is presented to propose improvements to the current methodology for three different boundary conditions. The main focus of this chapter is on two-way spanning frames, however, one-way spanning frames have also been considered under some loading conditions for verification against theoretical results. The pre-crack and post-crack phases of response of laminated glass, as well as the effect of aspect ratio of the frame have been considered.

6.2. Methods

6.2.1 Transformation factors

In the SDOF model, the actual system is represented as a mass on a spring (Fig. 6.1). In this example, a distributed load, $p(x)$, is applied to the actual system with a mass, M_t , and it is converted into an equivalent system with mass, M_e , load, F_e , and stiffness, k_e .

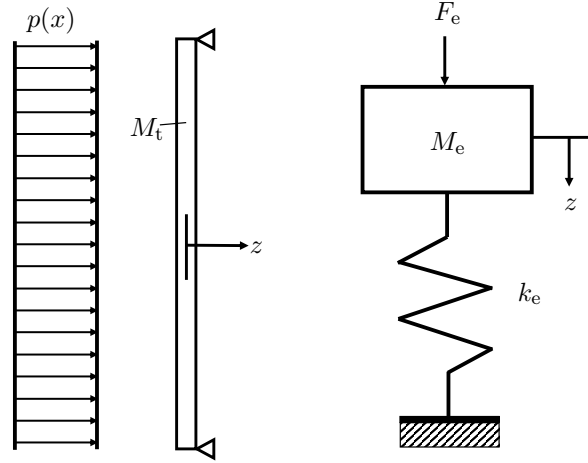


Figure 6.1: The actual and equivalent system in the SDOF model.

Many authors have extensively dealt with the details of the design steps and design requirements [2, 1]. Glazing design is briefly summarised in the following three steps:

1. Calculation of a resistance function covering the pre-crack and post-crack phases.
2. Representation of the system as a SDOF model using the calculated resistance function and the selection of suitable transformation factors (K_L/K_M), as well as the calculation of the dynamic reaction force.
3. Calculation of the displacement of the pane at the failure limits (*i.e.* the limit for glass cracking and the limit of failure of the interlayer material or edge joints), and if required, the design should be improved and updated.

The transformation factors perform an important role in representing the actual system by an equivalent SDOF model. In the calculation of the transformation factors, the principles demonstrated by Biggs were followed for one-way spanning systems [51], and were then extended to two-way spanning systems.

The transformation factors derived here are a function of the shape factor, ϕ , which is a function of the central deflection. Specifically the load transformation factor is the function of ϕ and the mass transformation

factor is a function of ϕ^2 . The ϕ term arises from requiring that the work done in the actual and the equivalent system is the same when the central deflection in the actual system is the same as the equivalent system. The ϕ^2 term arises from equating the appropriate energy terms in the actual system to the equivalent system, when the central deflection in the actual system is the same as the equivalent system. In addition, it should be noted that the formulation of the transformation factors presented in this study have been based on the work by Biggs [51], so that the results from this study can be directly compared to those of Biggs.

For a one-way spanning system with uniformly distributed mass, the equivalent mass, M_e , in the SDOF model is given as a function of mass per unit length, m , and the shape function, $\phi(x)$ (Eqn. 6.1).

$$M_e = \int m\phi^2(x) \, dx \quad (6.1)$$

The mass transformation factor, K_M , is defined as the ratio of the equivalent mass, M_e , to the actual mass of the structure, M_t (Eqn. 6.2).

$$K_M = \frac{M_e}{M_t} \quad (6.2)$$

Similarly for a structure with distributed load, the equivalent load, F_e , is calculated based on the distributed load, p , and the shape factor (Eqn. 6.3).

$$F_e = \int p(x)\phi(x) \, dx \quad (6.3)$$

The load transformation factor, K_L , is then defined as the ratio of the equivalent load, F_e , to the actual load of the structure, F_t (Eqn. 6.4).

$$K_L = \frac{F_e}{F_t} \quad (6.4)$$

In one-way spanning systems, with a uniformly distributed mass and a uniformly distributed load over the length, L , the transformation factors can be simplified to the following form (Eqn. 6.5 and 6.6).

$$K_M = \frac{1}{L} \int \phi^2(x) \, dx \quad (6.5)$$

$$K_L = \frac{1}{L} \int \phi(x) \, dx \quad (6.6)$$

Similarly, in two-way spanning systems, by assuming a uniformly distributed mass and a uniformly distributed load over the area, A_s , the transformation factors can be calculated (Eqn. 6.7 and 6.8).

$$K_M = \frac{1}{A_s} \int \int \phi^2(x, y) \, dxdy \quad (6.7)$$

$$K_L = \frac{1}{A_s} \iint \phi(x, y) \, dxdy \quad (6.8)$$

As it is seen here, the assumed shape function is very important in transforming the mass and load of the actual system into an equivalent system. The dynamic equation of motion of the equivalent SDOF system is given by Eqn. 6.9.

$$K_M M_t \ddot{x} + K_L k_t x = K_L p(x) \quad (6.9)$$

where k_t is the stiffness of the actual system.

6.2.2 Finite element model

A FE model was created for one-way spanning systems with length, l , and for two-way spanning systems with dimensions $a \times b$ (Fig. 6.2). Two types of FE solver were used, the implicit and the explicit solver. The Abaqus implicit solver (standard) was used for the low rate model, and the Abaqus explicit solver was used for the high rate model [66]. The results from the implicit solver can be directly comparable to Biggs [51], whereas, the results from the explicit solver are more representative of blast loading, and are expected to differ from the Biggs solutions.

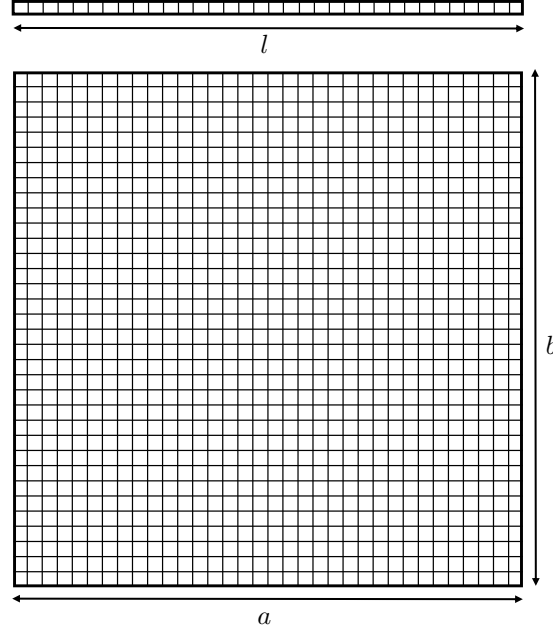


Figure 6.2: Schematic of the one-way spanning system model with length l and the two-way spanning system model with dimensions $a \times b$. Note: The boundary conditions of Fig. 6.4 was applied to both the one-way and two-way spanning system models.

From the FE model, the displacement was extracted for all nodes, at each time step. The shape functions were determined from the model by normalising the displacement with the maximum displacement at each time step. Using the shape functions, the transformation factors were calculated by using the trapezoidal numerical integration method in 1D for one-way spanning and in 2D for two-way spanning systems.

In the FE model, a selection was made between solid elements and shell elements. Each of the element types has its advantages and disadvantages. For example a solid element model is able to fully capture the cross-sectional behaviour of the laminated glass, however, it has the limitations of having a significantly greater computation time compared to the shell element model. In calculating the transformation factors, the deflection profiles are of concern. In the literature, for the application of an idealised blast load, the deflection profiles of the two element types have been compared, and have been shown to be broadly similar [33]. For the purpose of analysis in this paper, the four-noded shell element (S4R) was selected

so that the computation time is kept to a minimum.

6.2.3 Material properties

The response of laminated glass occurs in two phases, before the glass layers have started to crack (the pre-crack phase), and after the cracking of the glass layers has occurred (the post-crack phase). Laminated glass responds differently during each phase, therefore in its modelling, each phase is treated using different material properties.

The modelling of the pre-crack behaviour of laminated glass may be treated in two different ways [69]; as the linear case (Fig. 6.3a) or as the non-linear case (Fig. 6.3b). In the linear case, the flexural strain varies linearly through the neutral axis which lies at the center of the PVB interlayer (note: In this case, the flexural stress across the interlayer is orders of magnitude lower than in the glass, and therefore it has not been explicitly made visible in the figure). In the non-linear case, each glass layer is considered to bend independently, with neutral axes lying at the center of each glass layer. In this non-linear case, there is no shear transfer between the glass and the interlayer, and the interlayer simply acts as a separator. Some early researchers, have drawn comparisons between the non-linear case and the layered glass case [40, 70, 71].

There is, however, a third option in which the model is simplified by assuming the whole thickness comprises a single monolithic glass plate (Fig. 6.3c). Some authors have shown that the pre-crack response of laminated glass is similar to that of monolithic glass with a thickness equal to the sum of only the glass layers in the laminate [72]. Other design guidelines [2] have suggested that in the pre-crack phase, it is acceptable to model the laminate as monolithic glass only with a thickness equal to the sum of all the layers (glass and interlayer). In this paper, the latter has been assumed. In section 6.3.1 it will be shown that the calculated transformation factors for the monolithic case with a thickness equal to the glass layers only, are effectively the same as in the monolithic case with a thickness equal to that of the whole laminate. The material properties used in the pre-crack model are shown in Table 6.1.

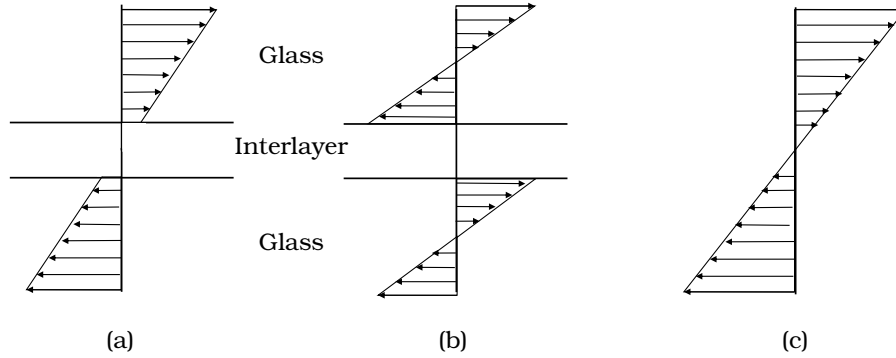


Figure 6.3: Variation of stress through thickness of laminated glass: a) Linear laminate b) Non-linear laminate c) Monolithic glass.

Table 6.1: Pre-crack material properties.

Material	Density ρ (kg m ⁻³)	Young's Modulus E (GPa)	Poisson's ratio ν
Glass	2530	72	0.22

The post-crack model was defined based on a material model developed by Hooper [33]. In this model, a shell composite is constructed using three layers (glass/PVB/glass). The glass does not carry any load and is only present as additional mass on the PVB. The PVB interlayer carries all the load in membrane action. The main assumption is that after the explosion, the glass cracks soon after behaving monolithically and the laminated glass is treated as a membrane with insignificant flexural stiffness. This assumption is often made by design guidelines [2] and may not be what actually happens on a micro-scale in a blast event. However, Hooper has demonstrated that this model is in good agreement with experimental blast observations [37]. In addition, the post-crack response does not take into account the deflection from the pre-crack phase and assumes that the glass is cracked at the beginning. This is in fact a conservative estimate. If an energy comparison is made, only 3% of the energy dissipated by a glass pane under blast loading is during the pre-crack phase, the remaining is dissipated in the post-crack phase of the response [2].

In the post-crack model, the glass component is treated as having elastic properties with zero stiffness. However, the lowest possible stiffness on Abaqus is 1 Pa. Also note that the Poisson's ratio that is used for the

glass in the post-crack model is the same as that of the pre-crack model; in this case since the glass is carrying no load, the value of the Poisson's ratio is insignificant. The PVB is modelled using elastic properties (Table 6.2) and a rate dependent Johnson-Cook plasticity model (Eqn. 6.10):

$$\sigma = (A + B\varepsilon_p^n) \left(1 + C \ln \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \right) \quad (6.10)$$

where $\dot{\varepsilon}$ is the strain rate, $\dot{\varepsilon}_0$ is a reference strain rate, ε_p is the plastic strain and A , B , C and n are material constants. The material constants used in the model are shown in Table 6.3.

Table 6.2: Post-crack material properties.

Material	Density ρ (kg m ⁻³)	Young's Modulus E (GPa)	Poisson's ratio ν
Glass	2530	1x10 ⁻⁹	0.22
PVB	1100	0.53	0.485

Table 6.3: Constants used in the Johnson-Cook plasticity model.

A (MPa)	B (MPa)	C	n
6.72	10.6	0.248	0.303

6.2.4 Model dimensions

For one-way spanning systems, the model was 1 m long. For two-way spanning systems, three different aspect ratios were considered, thus, three different dimensions were used (Table. 6.4). In the pre-crack phase, the model was constructed of monolithic glass with a thickness of 7.52 mm. In the post-crack phase, the model was constructed of glass/PVB/glass with thickness of 3 mm/1.52 mm/3 mm, respectively.

Table 6.4: Model dimensions and mesh size for two-way spanning systems.

Aspect ratio	Dimensions	No. of elements
1	1 m x 1 m	6561
0.8	1 m x 1.25 m	8181
0.5	1 m x 2 m	13041

6.2.5 Boundary conditions

Three different boundary conditions (BC) were considered, namely, built-in, simply supported and pinned. Fig. 6.4 displays this for the one-way

spanning systems; similar boundary conditions were applied to the two-way spanning systems (not shown in Fig. 6.4).

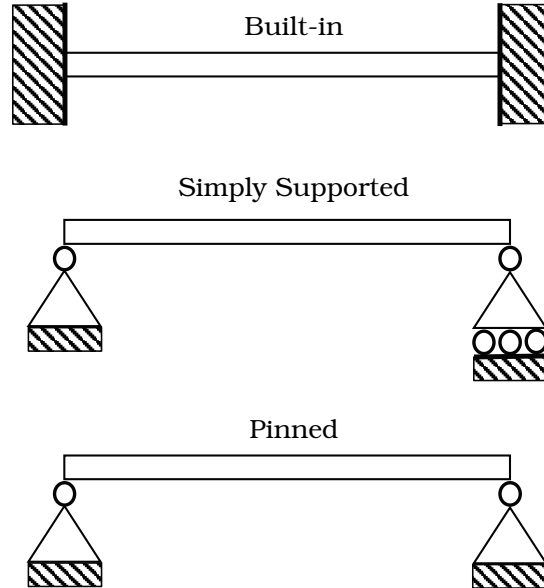


Figure 6.4: The boundary conditions applied to the one-way spanning system. Note: Similar boundary conditions were applied to the two-way spanning system – not shown here.

6.2.6 Loading conditions

Depending on the loading conditions, a suitable solver was selected in Abaqus. For the low-rate loading conditions the implicit solver (standard mode in Abaqus) was used. Whereas, for high-rate loading condition, the explicit solver in Abaqus was used.

A loading profile suited to the loading condition was selected. A ramp load profile was applied to allow the gradual displacement of the mass under true low-rate conditions. For the ramp profile, an increasing pressure, p , was applied with a load rise time, t_r (Fig. 6.5a). In the high-rate loading condition, as blast loading is the focus of this study, an idealised blast load profile was applied, with an initial pressure, p , an impulse, i , and duration of the positive phase of the blast pressure, t_d (Fig. 6.5b).

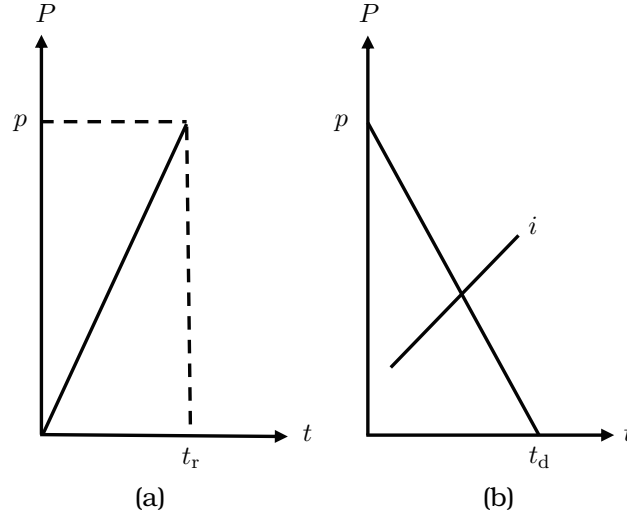


Figure 6.5: Loading profiles shown on a pressure (P) against time (t) graphs: a) Ramp load b) Idealised blast load.

The intensity of a blast load is usually best denoted on a graph of pressure, P , against impulse, i (Fig. 6.6). The blast load is usually categorised in terms of regions on a pressure-impulse diagram. The three regions are quasi-static, impulsive region or the transition regions (alternatively known as the dynamic region). The three different regions can be defined in terms of the duration of the positive phase of blast pressure (t_d) and the natural period of vibration of the structure (T).

In the quasi-static region, the duration of the positive phase is much greater than the natural period of the structure ($t_d \gg T$). In this region the response now only depends on the peak pressure, and the response is not greatly affected by further increase of impulse. In the impulsive region, the duration of the positive phase is much smaller than the natural period of the structure ($t_d \ll T$). In this region, the response now only depends on the impulse, which is dependent on both the pressure and positive phase duration. The transition region is known as the pressure-time region, in which both the pressure and the impulse affect the response. This is alternatively also known as the dynamic region. In this region, the duration of the positive phase is of the same order as the natural period of the structure ($t_d \sim T$) [1].

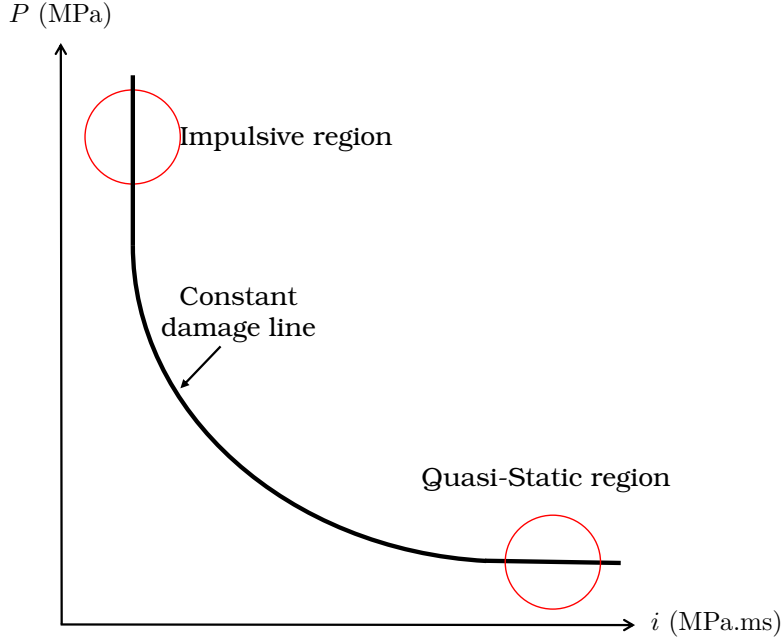


Figure 6.6: Example of an pressure (P) against impulse (i) diagram.

To capture the two loading extremes, loading conditions were selected from the impulsive region and the quasi-static region on the pressure-impulse graph (Fig. 6.6). The pressure and impulse values were selected such that they lie on the constant damage line.

In the pre-crack case, the constant damage line was selected to be when the bending stress in the glass reached a value at which cracking initiates. Standards exist for cracking values of glass, however, they are mainly for static or low rate loading. The prEn13474 standard is one of the most comprehensive standards that has been developed [73]. This standard was based on a series of ring-on-ring tests carried out by glass manufacturers to determine the stress at which glass begins to crack at a slow rate of bending. The findings from the standard were later extrapolated by Smith to estimate a conservative fracture strength for glass, under high rate dynamic loading, as 80 MPa [2]. Based on this finding from the literature, the current study takes 80 MPa as the bending stress at which glass cracks.

The post-crack case was treated differently to the pre-crack case. Currently, the SDOF model assumes a maximum displacement at which the interlayer begins to tear [2]. In this study, however, the tearing limit of

the PVB interlayer has been based on a maximum in-plane strain of 15%. Hooper has demonstrated that a strain of 15% is similar (and in fact more conservative) to the limit of tearing assumed in the SDOF model [37].

6.2.7 Model verification

Comparison with theory

To verify the FE model, the results obtained from the low-rate implicit simulations were compared with the results derived from theory. For one-way spanning systems, the the FE model was compared with beam theory, and for two-way spanning systems, the FE model was compared with plate theory. The comparisons were made only for the simply supported boundary conditions as a demonstration. Under low-rate loading, where the deflected shape does not change throughout the time step, the FE implicit results were extracted at a given time step and compared to theoretical results.

For one-way spanning systems, with simply supported boundary conditions, under uniform loading, the normalised deflection (shape function) can be obtained (Eqn. 6.11), using beam theory.

$$\phi(x) = \frac{16}{5L^4}(L^3x - 2Lx^3 + x^4) \quad (6.11)$$

For two-way spanning systems, under a uniform load q , the partial differential equation (PDE) in Eqn. 6.12 satisfies the simply supported boundary conditions [74]:

$$\frac{\partial^4 w}{\partial x^4} + 2\frac{\partial^4 w}{\partial x^2 \partial y^2} + \frac{\partial^4 w}{\partial y^4} = \frac{q}{D} \quad (6.12)$$

where w is the deflection and D is the flexural rigidity of the plate. For a plate with dimensions $a \times b$, if only the first mode is considered, a solution to the PDE to find the deflection is given by Eqn. 6.13:

$$w = A_1 \sin\left(\frac{\pi x}{a}\right) \sin\left(\frac{\pi y}{b}\right) \quad (6.13)$$

where A_1 is a constant. The deflection was normalised with the maximum deflection and an expression for the shape function was found (Eqn. 6.14). The maximum deflection occurs at coordinates $\left(\frac{a}{2}, \frac{b}{2}\right)$.

$$\phi(x) = \sin\left(\frac{\pi x}{a}\right) \sin\left(\frac{\pi y}{b}\right) \quad (6.14)$$

To calculate the transformation factors, the shape functions were inserted into Eqn. 6.5 and Eqn. 6.6 for one-way spanning systems, and Eqn. 6.7 and Eqn. 6.8 for two way spanning systems.

Comparison with previous research

The main source that has extensively published transformation factor values is by Biggs [51]. This work is based on methods stated in EM 110 – 345 – 416 [50]. Biggs has published the data for both elastic and plastic analysis. In current SDOF design methods used for laminated glass under blast loading, Biggs' elastic data are usually used for the pre-crack phase of the response and the plastic data are used for the post-crack phase of response. Throughout the work presented in this paper, a comparison has been made with the results published by Biggs. For the pre-crack phase of response, the FE results have been compared to Biggs' elastic data, and for the post-crack phase of response, the FE results have been compared to Biggs' plastic data.

6.3. Results and discussion

6.3.1 Pre-crack

Model verification

In this section, a comparison is made between theoretical solutions and the low-rate implicit FE model. The one-way and the two-way spanning systems were both considered. This comparison is only made for the simply supported boundary conditions.

For one-way spanning systems, the shape function (normalised deflection) from beam theory was graphically compared with that obtained from the low-rate FE implicit model (Fig. 6.7). The shape function shows a close match between the two results. The shape function was used to calculate the transformation factors. As expected, the transformation factors from the FE model, beam theory and the literature are in exact agreement to two significant figures (Table. 6.5), therefore, verifying the FE model.

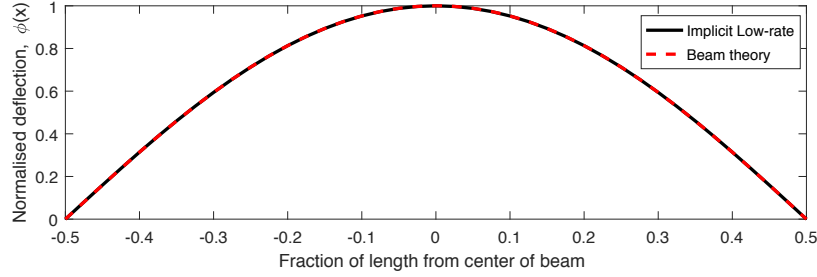


Figure 6.7: Comparison of the deflected shape from the Abaqus low-rate implicit model for the one-way spanning system to beam theory – for the simply supported boundary condition.

Table 6.5: Simply supported one-way spanning.

	K_L	K_M
Beam theory	0.640	0.504
Abaqus low-rate implicit model	0.640	0.504
Smith and Hetherington [1]	0.640	0.504
Biggs [51]	0.64	0.50

For two-way spanning systems, the deflected shape from the low-rate FE implicit model was compared with the deflected shape from plate theory (Fig. 6.8). To have a better view of the deflected shape, the data were plotted along the center line of the plate ($y = 0.5$), along x (Fig. 6.9). The plate theory solutions show a close agreement with the FE model. At the center and edges of the plate exact agreement is shown with the Abaqus model.

The slight difference in the deflected shape between the model and plate theory in two-way spanning systems could be due to the fact that in FE the 4th order partial differential equation (PDE) is solved using numerical methods, whereas, in plate theory a sinusoidal solution which satisfies the simple supports at the edges is used.

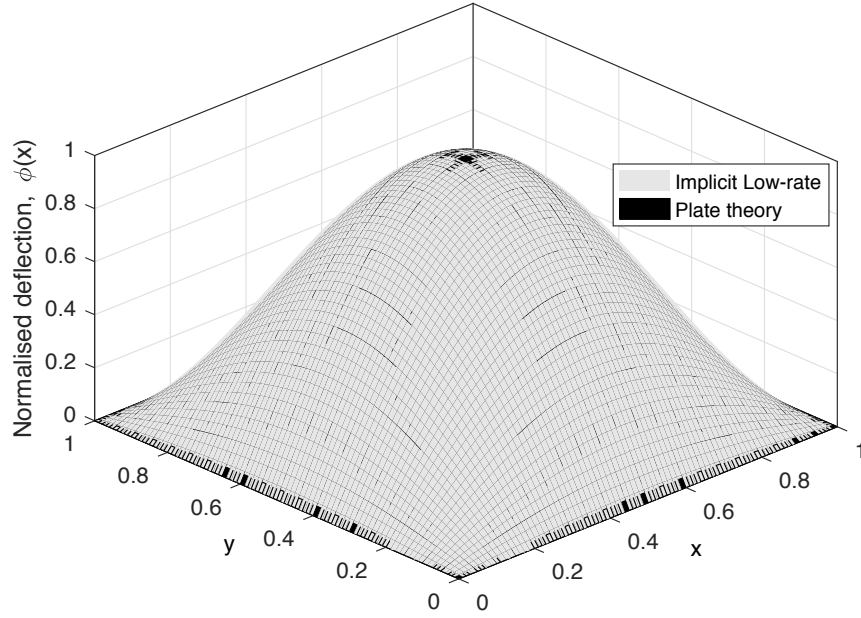


Figure 6.8: Comparison of the deflected shape from the Abaqus low-rate implicit model for the two-way spanning system to plate theory – for the simply supported boundary condition (the darker regions at the edges and at the peak, show the overlap between the two solutions). *Note: The scale has been exaggerated on the deflected shape.*

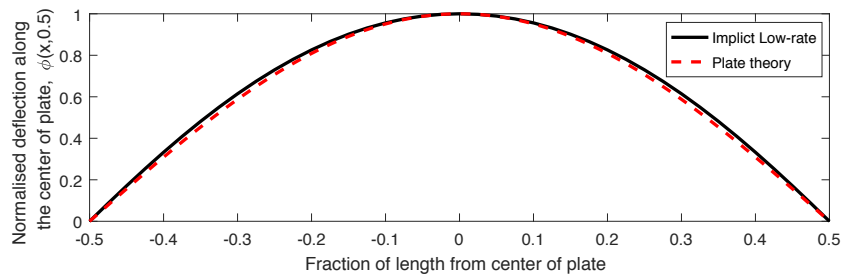


Figure 6.9: Comparison of the deflected shape from the Abaqus low-rate implicit model for the two-way spanning system to plate theory at center of plate ($y = 0.5$) – for the simply supported boundary condition.

The transformation factors were calculated for the two-way spanning systems (Table. 6.6). The transformation factors calculated from the plate

theory are in close agreement with the Abaqus model. The solution from Biggs, however, is slightly different. It was initially expected that the results from Biggs will match those of plate theory, however, this was not the case. Biggs published the transformation factors based on the methods outlined in EM 110-345-416 [50]. In EM 110-345-416, for two-way spanning systems, the shape function has been approximated based on the one-way spanning shape function (Eqn. 6.11). The transformation factors from Biggs are, therefore, different from the plate theory and the FE model because a different deflected shape has been used by Biggs.

Table 6.6: Simply supported two-way spanning.

	K_L	K_M
Plate theory	0.405	0.250
Abaqus low-rate implicit model	0.419	0.264
Biggs [51]	0.46	0.31

Two-way spanning systems

In the SDOF model, for a given deformation phase (elastic or plastic), it has always been assumed that the transformation factors (K_L and K_M) are constant throughout the deformation of the laminated pane, thereby ignoring the displacement-time history. In this study, the transformation factors have been calculated throughout the increasing deflection of the pane for a square geometry with aspect ratio $\frac{a}{b} = 1$. Each transformation factor was plotted against the normalised deflection. The normalised deflection was calculated by dividing the maximum deflection on the surface of the glass pane with the final value at its peak deflected shape (prior to rebounding). The majority of real life problems deal with two-way spanning systems, therefore, in this study only the two-way spanning systems were considered.

For the results obtained from the FE explicit solver, as expected, the transformation factors change as the deflection increases. The change in the transformation factors showed a variation depending on the loading region on the pressure-impulse diagram (quasi-static or impulsive). For example, in the quasi-static response, in the deflected shape for the built-in boundary condition, it is possible to see that the transverse waves initially have an impact, but they dissipate prior to the glass pane reaching its maximum deflection (Fig. 6.11(a)). As a result of the transverse waves being

dissipated in the structure, the transformation factors reach an almost constant value prior to maximum deflection of the pane (Fig. 6.10).

For impulsive loading in the FE explicit solver, it is slightly different (Fig. 6.11(a)). As under impulsive loading, the response rate is much greater, the glass pane does not have time to dissipate the transverse waves prior to the response. The transverse waves, then superimpose on the deflected shape, which results in the observation of higher modes of response on the glass pane. The higher modes of response, cause sharp peaks to appear in the transformation factor against deflection curve (Fig. 6.10). In addition, in the impulsive case in comparison to quasi-static, the acceleration is greater, thus higher modes of response are expected to be present. The transformation factor, then exponentially drops to a final value which is different to the quasi-static loading case. This difference could be due to the fact that, in the impulsive loading case, as the response approaches the final deflection, the transverse waves superimpose at the center. This phenomenon, causes the center of the glass pane to continue to move forward, while part of the pane (closer to the edges) has started to rebound (Fig. 6.11(a)). Similar trends were also seen for the simply supported boundary conditions (Fig. 6.13(a) and Fig. 6.13(b)) and the pinned boundary conditions (Fig. 6.15(a) and Fig. 6.15(b)).

The results were explored further and a comparison was made to the results from Biggs [51]. This comparison was made for all boundary conditions (BC), namely, built-in (Fig. 6.10), simply supported (Fig. 6.12) and pinned (Fig. 6.14). The final value of the transformation factors for the quasi-static loading case reached a value close to that of Biggs. The final value of the transformation factors for the impulsive loading case, however, did not reach a final value that was in agreement with Biggs. This difference is because, under impulsive loading, the transverse waves distorted the deflected shape of the response. Since the loading rate was high in the impulsive case, the glass pane could not dissipate the transverse waves during the response.

In the FE implicit solver, the transformation factors did not vary throughout the deformation period of the laminated pane. This trend was true for all boundary conditions. The transformation factors did not vary because the loading rate was low. The low-rate loading allows the glass pane to initially take the fully deflected shape that it is usually taken finally, under high rate loading. As the loading continues and the deflection increases, the deflected shape remains constant. The reason why the initial

shape is taken and why it remains constant during loading is because under low-rate loading the acceleration is very small, so the contribution of the inertial terms in the response are negligible. This was true for all boundary conditions (Fig. 6.11(c), Fig. 6.13(c) and Fig. 6.15(c)).

The transformation factors calculated using the implicit solver, show a small difference compared with those of Biggs [51]. The difference is because the transformation factors presented by Biggs are calculated using approximations to the deformed shape, which is different to that calculated from the FE model (further details were discussed in the model verification topic in section 6.3.1).

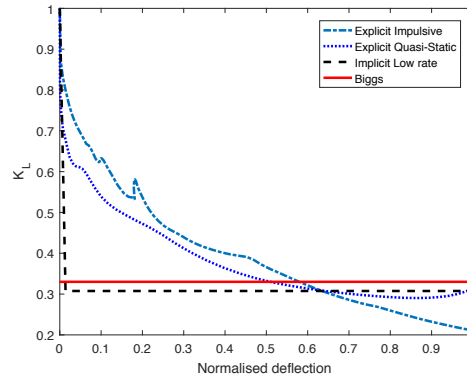
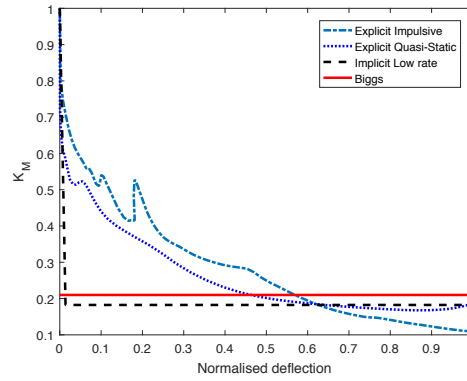
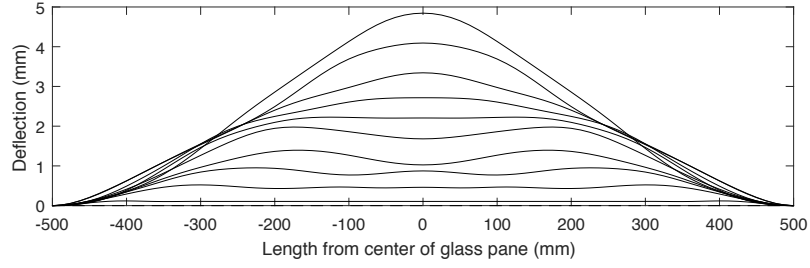
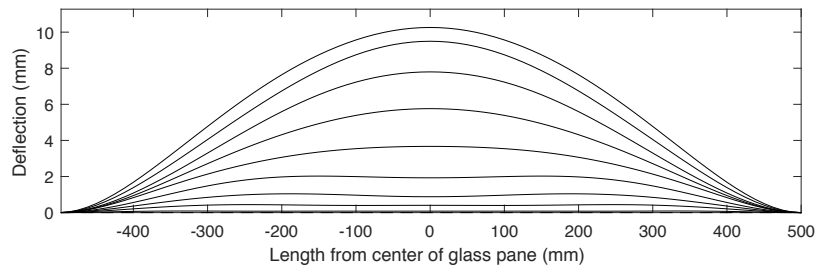
(a) K_L (b) K_M

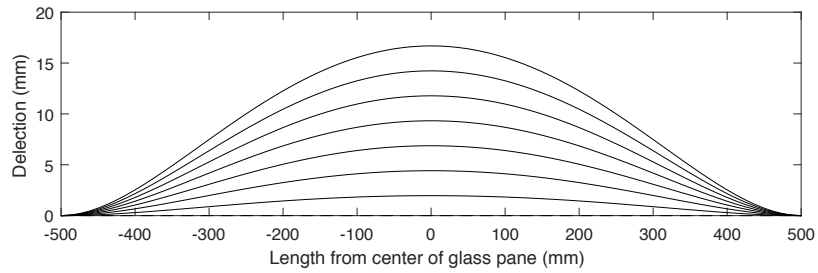
Figure 6.10: Transformation factors under built-in BC, for the pre-crack case, in two-way spanning systems with $\frac{a}{b} = 1$.



(a) The explicit impulsive case.



(b) The explicit quasi-static case.



(c) The implicit low rate case.

Figure 6.11: Displacement through time along the centreline length of the pane, under built-in BC with $\frac{a}{b} = 1$, for the pre-crack case.

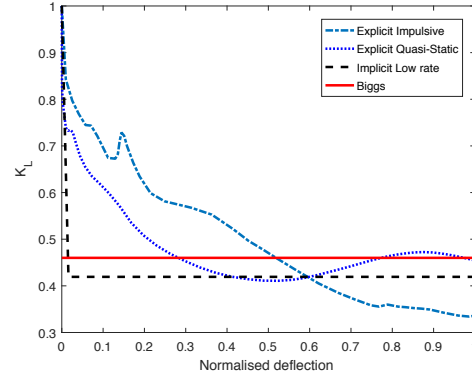
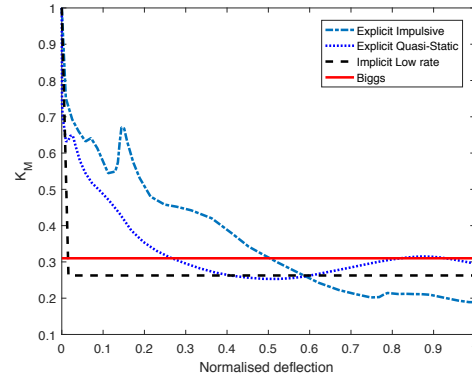
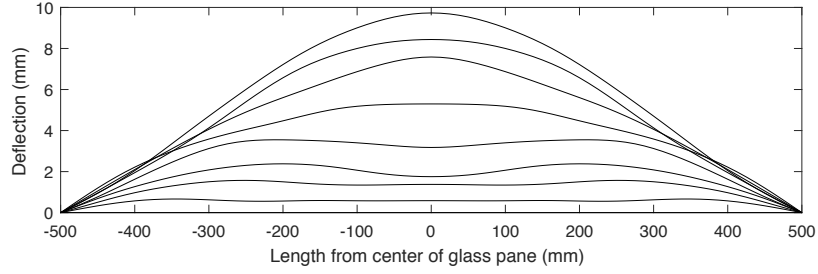
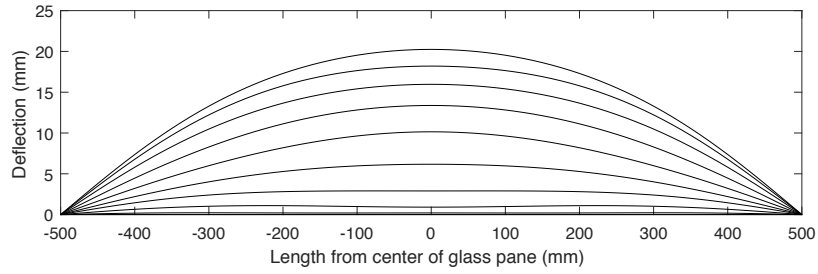
(a) K_L (b) K_M

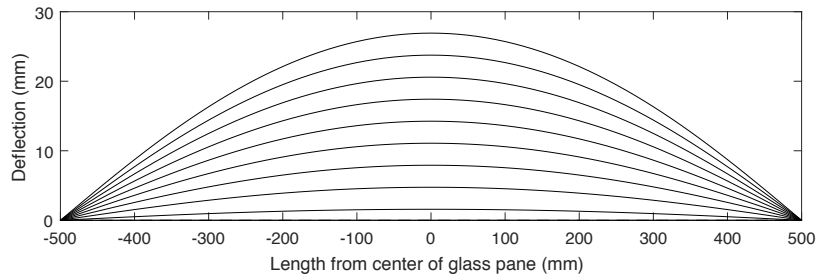
Figure 6.12: Transformation factors under simply supported BC, for the pre-crack case, in two-way spanning systems with $\frac{a}{b} = 1$.



(a) The explicit impulsive case.



(b) The explicit quasi-static case.



(c) The implicit low rate case.

Figure 6.13: Displacement through time along the centreline length of the pane, under simply supported BC with $\frac{a}{b} = 1$, for the pre-crack case.

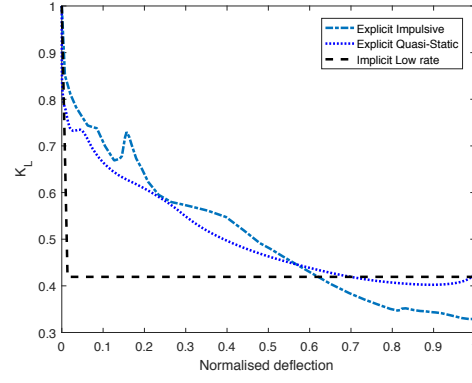
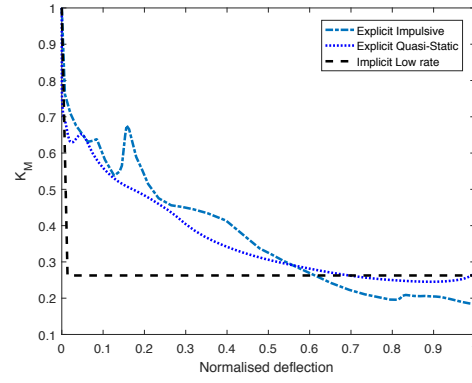
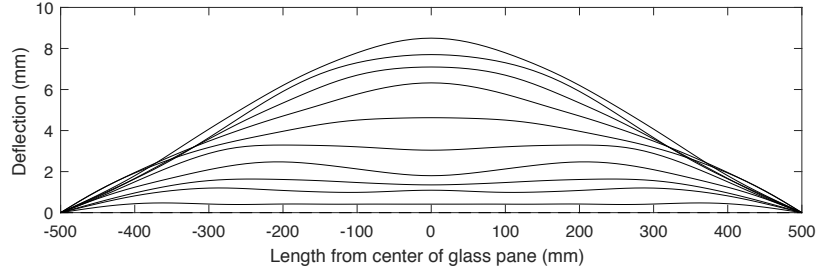
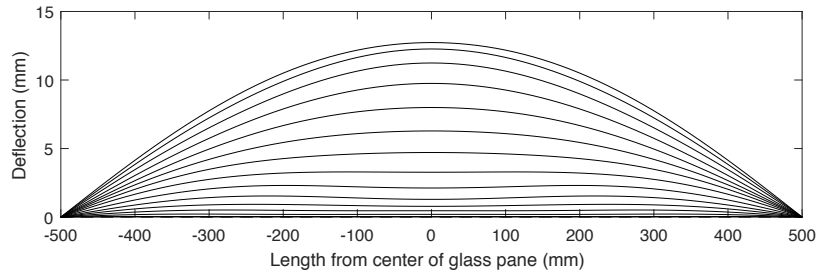
(a) K_L (b) K_M

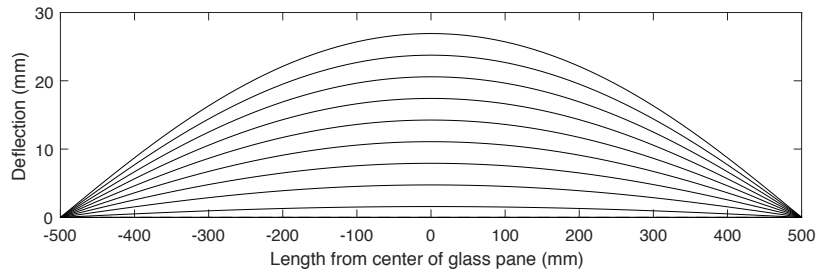
Figure 6.14: Transformation factors under pinned BC, for the pre-crack case, in two-way spanning systems with $\frac{a}{b} = 1$.



(a) The explicit impulsive case.



(b) The explicit quasi-static case.



(c) The implicit low rate case.

Figure 6.15: Displacement through time along the centreline length of the pane, under pinned BC with $\frac{a}{b} = 1$, for the pre-crack case.

Aspect ratio

The glass panes are not always a square ($\frac{a}{b} = 1$). The analyses was therefore conducted for other aspect ratios of $\frac{a}{b} = 0.8$ and $\frac{a}{b} = 0.5$. The analyses was carried out on the two most common type of boundary conditions (built-in and simply supported) [51]. Both the FE implicit and the FE explicit solvers were used. The results for $\frac{a}{b} = 0.8$ are shown in Fig. 6.16 and Fig. 6.17, and the results for $\frac{a}{b} = 0.5$ are shown in Fig. 6.18

and Fig. 6.19.

The effect of changing the aspect ratio from unity ($\frac{a}{b} = 1$), shows a greater deviation between the transformation factors, calculated by Biggs and the values from FE (*note: for the implicit model, the transformation factors compared here are the ones throughout the response – as the transformation factors stay constant during the response. For the explicit model, the transformation factors compared here are the ones reached at maximum deflection – as the transformation factors change during the response*). Even though the transformation factors vary with aspect ratio, the factors are not as sensitive to aspect ratio, as apose to what Biggs has suggested. The difference becomes much more noticeable as the aspect ratio reduces from $\frac{a}{b} = 0.8$ to $\frac{a}{b} = 0.5$. This difference suggests that the simplistic assumptions made in the derivation of the shape factors in Biggs are no longer valid at other aspect ratios.

This study on the effect of aspect ratio on the transformation factors was not limited to a comparison to Biggs' data. Interesting results were also observed from the FE explicit model, for both the quasi-static and impulsive loading types. In the quasi-static case, where the aspect ratio was unity, the transformation factor trend reached the implicit solution at a normalised deflection value of between 0.4 – 0.6, depending on the boundary condition. This value was about 0.7 for $\frac{a}{b} = 0.8$, and about 1 for $\frac{a}{b} = 0.5$. This trend could be because, as the aspect ratio moves away from unity, the transverse waves take a longer time to dissipate during the response. In the impulsive case, the transverse waves showed the most severe effect for the aspect ratio of $\frac{a}{b} = 0.5$. For this aspect ratio, sharp peaks in the transformation factor trend were observed both at the beginning and near the end of the response. The sharp peaks could possibly be as a results of the transverse waves from the two directions of the panel reaching the center, at different times during the response. For an aspect ratio of 1, the modal contributions in both the x and y directions are the same. As the the aspect ratio moves away from 1, the stiffness in the two directions diverge, which will affect the natural frequency of the mode and hence the modal mass and modal participation in the overall response of the structure. This bifurcation of the natural frequency of the response function becomes much more noticeable as the aspect ratio diverges away from unity.

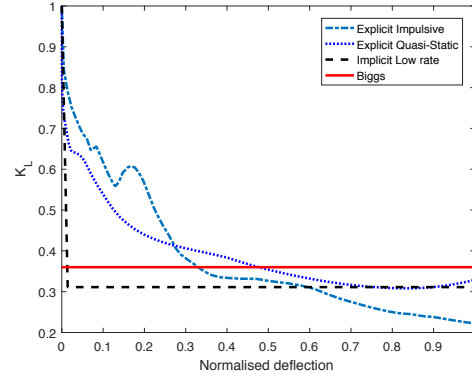
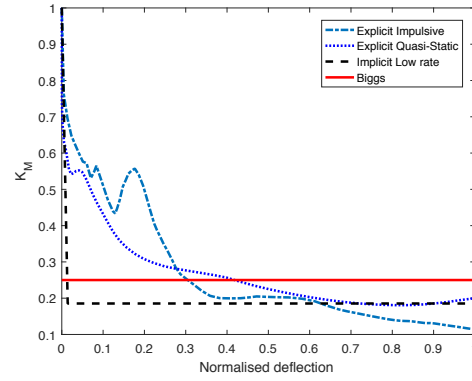
(a) K_L (b) K_M

Figure 6.16: Transformation factors with pre-crack material properties, for two-way spanning systems with $\frac{a}{b} = 0.8$ under built-in BC.

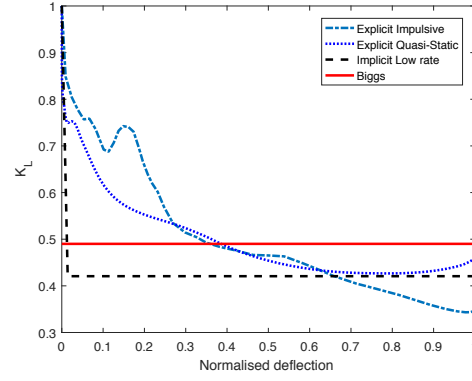
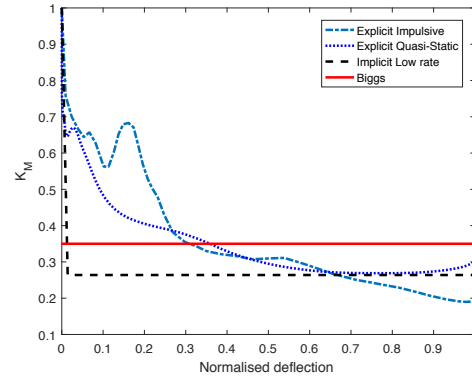
(a) K_L (b) K_M

Figure 6.17: Transformation factors with pre-crack material properties, for two-way spanning systems with $\frac{a}{b} = 0.8$ under simply supported BC.

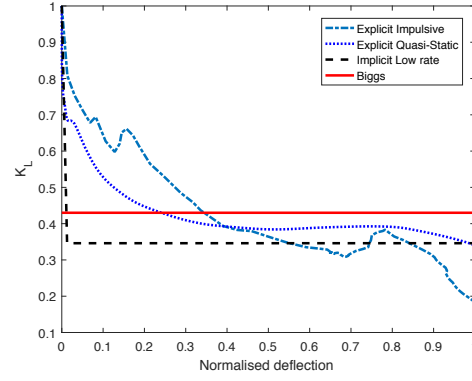
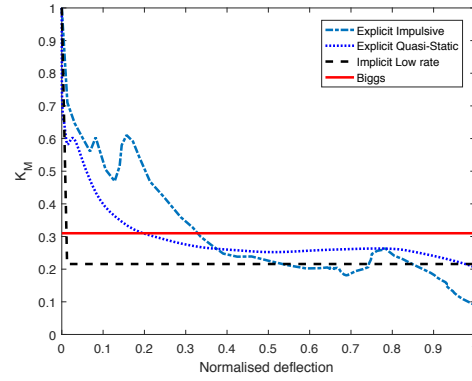
(a) K_L (b) K_M

Figure 6.18: Transformation factors with pre-crack material properties, for two-way spanning systems with $\frac{a}{b} = 0.5$ under built-in BC.

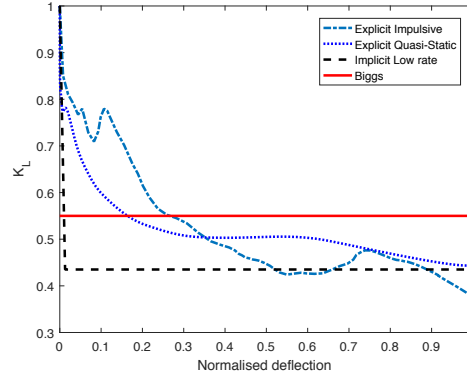
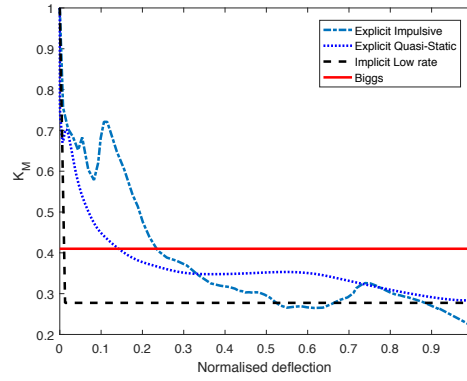
(a) K_L (b) K_M

Figure 6.19: Transformation factors with pre-crack material properties, for two-way spanning systems with $\frac{a}{b} = 0.5$ under simply supported BC.

Effect of thickness

As previously mentioned, some authors have suggested a monolithic glass model with a total thickness equal to the thickness of only the glass layers to represent the laminate thickness in the pre-crack phase of response. Whereas, others have suggested a monolithic glass model with a total thickness equal to the total thickness of the composite laminate. In this study, using FE analysis, the thickness was compared between that of glass layers only (6 mm) and the total thickness of the laminate (7.52 mm). This comparison has been made only with the built-in boundary conditions (Fig. 6.20). In this comparison, idealised blast loads were considered for

the impulsive and the quasi-static regions of the $P - i$ curve.

For the impulsive loading condition, the transformation factors for the different thicknesses are in agreement. For the quasi-static loading condition, the transformation factors vary slightly, however the difference is not significant. It can, therefore, be concluded that the difference in transformation factor values for the two thicknesses is not great enough to raise a concern with regards to what thickness is more appropriate to represent laminated glass.

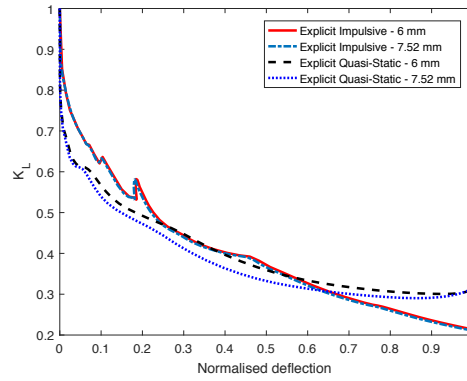
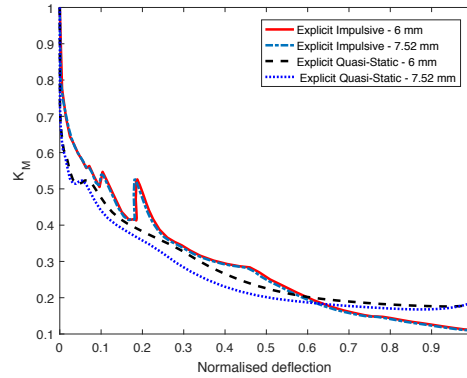
(a) K_L (b) K_M

Figure 6.20: Thickness effects on transformation factors under built-in BC, for the pre-crack case, in two-way spanning systems with $\frac{a}{b} = 1$.

6.3.2 Post-crack

As discussed previously, the behaviour of laminated glass under blast loading occurs in a pre-crack and a post-crack phase. Section 6.3.1 deals only with the pre-crack phase. In this section, the post-crack phase is considered. The FE analysis was conducted using both the implicit and the explicit solvers. The results presented in this section are valid for all boundary conditions because in the post-crack phase the pane behaves as a membrane with very little out-of-plane resistance and hence the boundary conditions no longer affect the deflected shape. This is similar to results from Biggs' analysis [51], where under plastic response the transformation factors are identical for the different boundary conditions.

In the explicit analysis, where the impulsive and quasi-static loading conditions were applied, in comparison with the pre-crack model, the transverse waves no longer interrupt the deformed shape; this causes the transformation factor curves to appear much smoother (Fig. 6.21). The transverse waves travel much slower in the post-crack model because the stiffness of the laminated glass is much lower [75]. The deflection of the laminate achieves its maximum value, prior to the transverse waves interrupting the deflected shape (Fig. 6.22(a) and Fig. 6.22(b)). As the deflected shape does not get interrupted, the final value of the transformation factors for both quasi-static and impulsive loading reach a similar value. In the pre-crack phase (section 6.3.1), however, this was not the case, and the transverse waves significantly affected the deflected shapes and resulted in a difference in the final value of the transformation factors between the two loading types (quasi-static and impulsive).

In the implicit analysis, where a low ramp was applied, similar to the pre-crack case (section 6.3.1), the laminated glass initially reached a deformed shape that it would usually reach near the end of the response in high rate loading, and this shape remained constant during the analysis (Fig. 6.22(c)). As the shape was constant, the transformation factors remained constant too. The value for the transformation factors were still not identical to that derived by Biggs [51]. As discussed previously, approximations have been made in the derivation of the deflected shapes presented by Biggs which are different to both the sinusoidal plate theory solutions and the numerical methods used in FE, hence, the transformation factors are different.

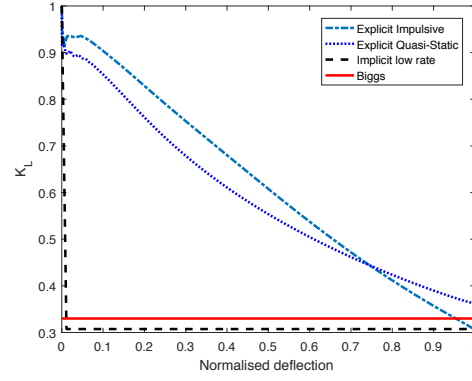
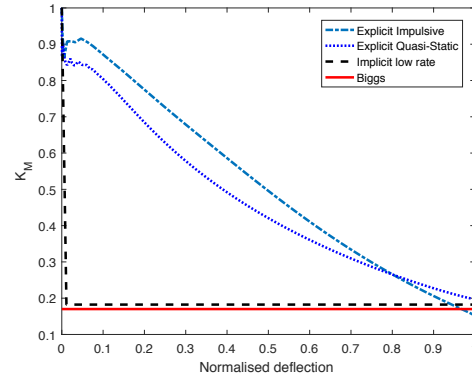
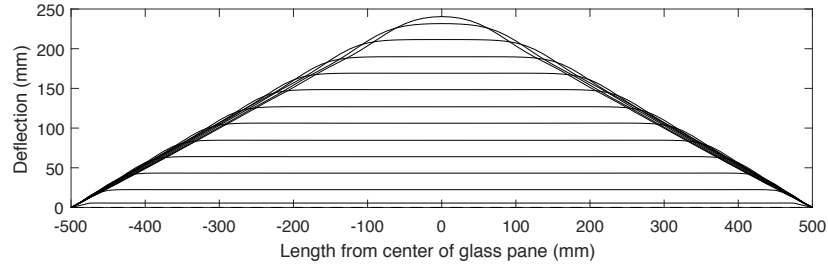
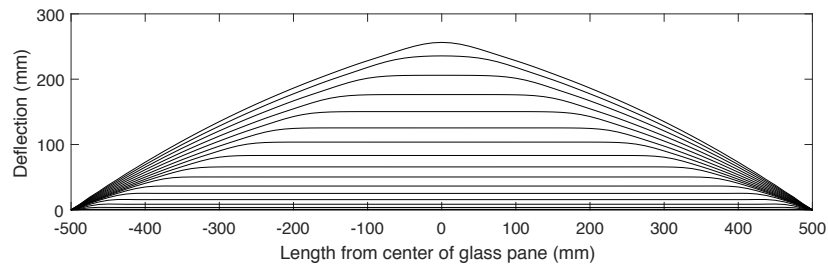
(a) K_L (b) K_M

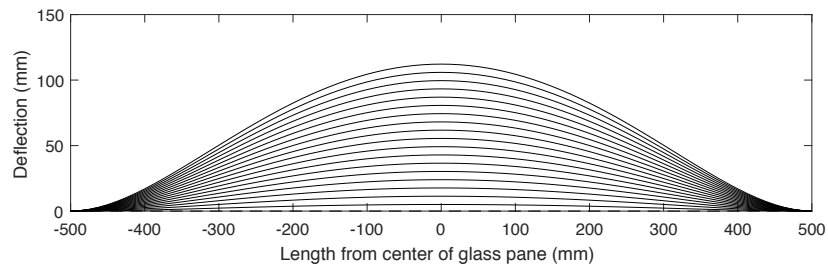
Figure 6.21: Transformation factors with post-crack material properties, for two-way spanning systems with $\frac{a}{b} = 1$.



(a) The explicit impulsive case.



(b) The explicit quasi-static case.



(c) The implicit low rate case.

Figure 6.22: Displacement through time along the centreline length of the pane, with post-crack material properties, for two-way spanning systems with $\frac{a}{b} = 1$.

6.4. Conclusion

In this study, a FE model was developed to calculate load and mass transformation factors for use in the SDOF model. The FE model was used to obtain results for a laminated glass pane under blast loading, at low and high loading rates. The FE model was verified (for low loading rates) with beam theory for one-way spanning systems, and with plate theory for two-way spanning systems. A comparison was also made with early

work carried out on the SDOF model with application to blast loading (for example EM 110-345-416 [50] and Biggs [51]).

The response of laminated glass under blast loading occurs in two phases, the pre-crack phase, and the post-crack phase. In the pre-crack phase, it was demonstrated that the transverse waves are an important factor in predicting what value the transformation factors take and how they vary during the deformation period. In the post-crack phase however, transverse waves did not disturb the deflected shape and hence left the transformation factors unaffected.

In this study the importance of the deflected shape-time history was demonstrated. At low rates it was found that the transformation factor does not vary during the deflected shape-time history. For high rate loading, however, this is not the case. The results obtained from the FE model at high rates indicate that the assumptions regarding the deflected shape, and transformation factors, which are usually made in the SDOF model for blast response are debatable.

The transformation factors have been shown to vary during high rate response, thus, it is very simplistic to overlook this fact and assume a constant transformation factor value during the high rate deformation period of the pane. As a result of this study, it is recommended that the accuracy of the SDOF model can be improved, if the transformation factors are updated as the deflection of the structure is increased.

The SDOF method, despite its simplistic nature, has much merit for design of structures. Using the SDOF model, a design can be developed in a short time frame, rather than introducing the time, cost and complications associated with full finite element analysis. In the future, for application of the SDOF model, it is recommended that the deflected shape-time history should be taken into account, particularly for high rate loading.

Conclusion

This research project was carried out to improve the current understanding of the behaviour of laminated glass panes subject to blast loading. A uniform loading test rig was developed and specimens were tested at different loading rates, interlayer thickness and framing arrangements. The temperature effects on the post-crack response of laminated glass was also studied. The cracked laminate experiments were then extended to investigate the de-bonding of the laminated glass from the PVB interlayer at different loading rates, different temperatures and for three different interlayer thicknesses. Finally the current method of employing the *single-degree-of-freedom* model in the assessment of structures under blast loading was investigated using FE methods. This investigation was carried out for different loading rates, boundary conditions and frame aspect ratios.

7.1. High rate bending tests

A new experimental method was developed to test laminated glass under high rate uniform loading in the laboratory. Specimens with three interlayer thicknesses of 0.76 mm, 1.52 mm and 2.28 mm were tested. The experiments were carried out at testing speeds of 6 m/s, 8 m/s and 10 m/s. In addition to the normal uncracked specimens, cracked specimens and specimens with a thinner frame were tested.

At higher speeds the uncracked specimens started failing due to either PVB tearing or delamination in the side between the PVB and glass. The

failure mode was found to be dependent on the interlayer thickness. With the thinnest interlayer, PVB tearing was observed, whereas, with the thicker interlayers, the failure mode was side delamination. Both these failure modes were thought to be due to inadequate PVB thickness being present to absorb the impact energy. This is primarily because of the low number of cracking sites on the glass. To induce increased cracking and hence to allow further exposure of the PVB, some specimens were pre-scored. By pre-scoring the specimens, further crack initiation sites were promoted. This solved the problem partially, however, failure was still observed in the 2.28 mm interlayer. It should be noted that not all pre-scored lines lead to cracks. This shows that the testing speeds were not high enough to overcome the glass cracking strength.

To solve the problem of specimen failure at higher speeds, thinner specimen end tabs were used. The specimen failure problem was solved, as a result. This is because the thinner end tabs deformed during the test, thereby reducing the load on the interlayer. The end tabs here are comparable to that of the frame in a real window structure. This idea of using sacrificial frames is a novel finding, as in a real blast scenario the frames could be designed to purposefully deform and hence to reduce the load on the laminated glass specimen. This is a significant point that could be implemented in design of laminated glazing frames.

7.2. The effects of temperature

The temperature effects on the behaviour of cracked laminated glass were studied. Single-cracked and randomly-cracked specimens were tested with interlayer thicknesses of 0.76 mm, 1.52 mm and 2.28 mm at a testing rate of 1 m/s. The experiments were conducted over a temperature range of 20°C – 60°C in increments of 10°C. The post-crack response of the laminated glass was shown to be temperature-dependent and also interlayer thickness-dependent.

In this test, it was found that if energy absorption is to be maximised, a balance between de-bonding of the interlayer and stretching of the PVB is required. These two factors are inter-related. To achieve greater stretching, a greater length of de-bonded PVB is required, otherwise the PVB can tear early in the test. At lower temperatures the PVB is stiff, whereas at higher temperatures, the PVB is soft and compliant. Soft PVB is unable to carry the loads required to propagate the de-bonding fronts, thus leading to early

tearing. The maximum energy absorption was found to occur between 10°C–40°C depending on the interlayer thickness. While thinner interlayers have maximum energy absorption at higher temperatures. The transition temperature of the PVB tested in this experiment was approximately 15°C. The chemical composition of the PVB could be modified in the manufacturing phase to shift this transition temperature. Given that the post-cracking response of laminated glass is temperature dependent, it is recommended that for use in extreme temperatures, the composition of the PVB should be modified so that maximum energy absorption can occur at the required design temperature.

7.3. De-bonding of PVB

The single-cracked experiments together with tensile tests on pure PVB were used to calculate a bond fracture toughness at a temperature range of 20°C – 60°C for three different interlayer thicknesses. The bond fracture toughness between the PVB and the glass was found to be temperature-independent in the range of temperatures tested. This is a significant design fact because the laminated glass pane provides its protection capability through two properties. Firstly, the pane deforms to absorb the energy of the blast wave, and secondly, the cracked glass fragments remain bonded to the PVB. This finding is important because it can now be assumed that the number of glass fragments that remain intact at 20°C will be the same in hotter climates.

This study was extended by using the experimental data from Hooper [37] to calculate a bond fracture toughness at different testing rates using energy balance methods. Additionally, FE methods along with time-temperature superposition was used to create a model that is able to predict the de-bonding load at a range of rates and temperatures. This de-bonding model along with understanding of the stretching behaviour of the PVB can be used in the design of laminated glass by allowing an in depth understanding of the post-cracking response of laminated glass. It is the two competing mechanisms of stretching and de-bonding which dictate the failure mode depending on temperature, testing rate and interlayer thickness.

7.4. Prediction of response

The SDOF model is widely used by the industry to predict the response of laminated glass panes subject to blast loading. The SDOF model is based on a set of load and mass transformation factors, which are based on the deflected shape of the structure. The current industry standard is to ignore the deflection–time history of the structure. The main aim of the exercise carried out in this thesis was to use FE to derive deflected shapes which vary during the response, and to examine the difference to the current industry design assumptions. This exercise was carried out for both pre-crack and post-crack laminated glass. In addition this comparison was conducted at a range loading intensities, pane boundary conditions and pane aspect ratios. Also, one-way spanning and two-way spanning frames were considered.

It was found that at low loading intensities, the transformation factors are similar to those used in the current SDOF models. However, at higher loading intensities, the deflected shape versus time history should not be ignored. SDOF continues to be widely used by industry because of its relative low cost in comparison to FE and its simplicity. As a result of the study conducted in this thesis it is recommended that in order for the accuracy of the SDOF model to be improved, the assumptions made in current SDOF models should be updated to allow the deflection–time history to be included – specifically for higher loading intensities.

7.5. Future work

In this research, many additional topics which were beyond the scope of this project, were identified. Some are additions to the topics presented in this thesis and some are new ideas which could be looked into.

7.5.1 Cause of glass cracking

The mechanism which initiates cracking across the whole glass pane when a blast wave hits it, is a subject of debate. One theory is that the stress wave travelling through the glass thickness initiates cracking. Another is that the cracking starts as the glass pane deflects. The reality could reveal that cracking could originate from both, depending on loading intensity.

Field blast testing previously performed has never had this question as a point for concern. Many researchers were mainly concerned with the

post-cracking response of the laminated glass pane. Techniques such as high speed video recording (10,000 frames-per-second) and digital image correlation (DIC) have been used. Future full-scale blast tests could be conducted, with state-of-the-art high speed video recording technology (up to 100,000 frames-per-second), to be able to capture the cause of glass cracking.

7.5.2 Effect of glass thickness on pane resistance

In laminated glazing systems, thicker glass plies will provide a greater strength required to crack the glass. Therefore this will result in a lower number of cracks formed in each glass ply. The fact that there is a smaller number of cracking sites, it will mean less de-bonded PVB will be exposed. Hence less PVB is available for stretching and membrane action required to absorb the pressure waves. This leads on to tearing of the interlayer at an earlier stage than expected. Full-scale blast tests could be conducted to assess this phenomena. It is important that a systematic approach is taken, where a variety of glass thicknesses are exposed to an identical load.

7.5.3 Frame design

In this thesis, the high rate bending test rig was used to explore the energy absorbing capacity of the glazing system as a whole unit. Promising results were obtained that showed that if the frame was designed such that it was allowed to deform under loading, the loading intensity on the actual glass pane could be reduced.

New framing arrangements should be designed and full-scale blast tests should be conducted to assess their resistance in comparison to conventional glazing systems. The idea behind this is that the glazing unit will have to be replaced following an explosion. So there is no harm in purposefully designing the frame such that it deforms more than usual, as this will result in a greater energy absorption by the glazing unit. Thus the laminated glass can withstand greater blast loading threats.

7.5.4 Silicone sealant bond strength

One of the main reasons that laminated glazing systems fail is because of the silicone sealant between the laminated glass and the frame. The work up to date on silicone sealants has been mainly either shear or tensile

tests. An important factor to note is that the silicone joint usually fails on the rebounding of the pane. This means that the silicone sealant survives the first load, but fails when the pane rebounds. It could also be that tearing initiates in the first deflection and fails in the rebound because the silicone is now weaker than the virgin silicone and can no longer withstand a second loading cycle.

An experiment in the laboratory could be designed such that the silicone sealant is tested in the first cycle up to a certain point. The test is then brought to a halt. The same silicone specimen should then be tested again in the second loading cycle, but this time up to failure. The test rig should also allow for loading angle variation. So for example if the first loading cycle is at 45 degrees, the second cycle should be at -45 degrees, such that the reality is represented correctly. In addition, this test should allow different bite depths and loading angles to be studied.

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Blast Simulator Crack Pattern



Figure A.1: Post-experiment crack pattern for specimen no.2.

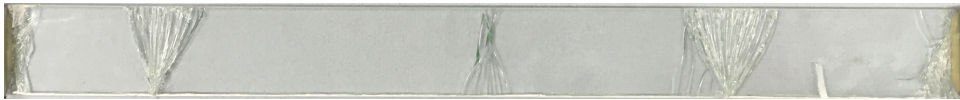


Figure A.2: Post-experiment crack pattern for specimen no.3.



Figure A.3: Post-experiment crack pattern for specimen no.4.

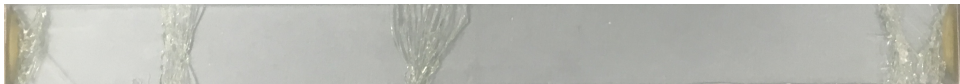


Figure A.4: Post-experiment crack pattern for specimen no.7.

A. Blast Simulator Crack Pattern



Figure A.5: Post-experiment crack pattern for specimen no.8.



Figure A.6: Post-experiment crack pattern for specimen no.9.

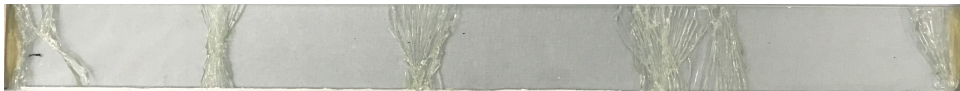


Figure A.7: Post-experiment crack pattern for specimen no.10.



Figure A.8: Post-experiment crack pattern for specimen no.13.

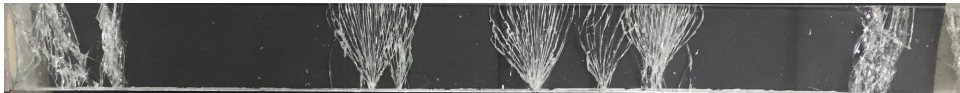


Figure A.9: Post-experiment crack pattern for specimen no.14.



Figure A.10: Post-experiment crack pattern for specimen no.15.

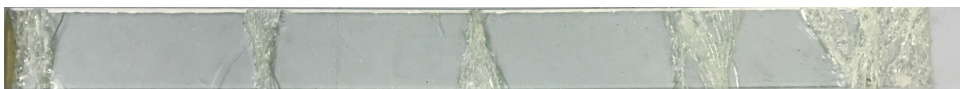


Figure A.11: Post-experiment crack pattern for specimen no.16.

A. Blast Simulator Crack Pattern

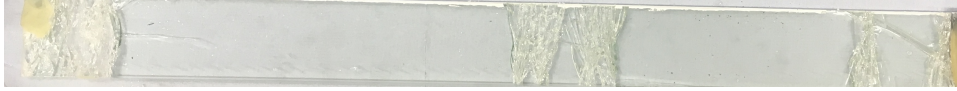


Figure A.12: Post-experiment crack pattern for specimen no.17.



Figure A.13: Post-experiment crack pattern for specimen no.18.



Figure A.14: Post-experiment crack pattern for specimen no.19.

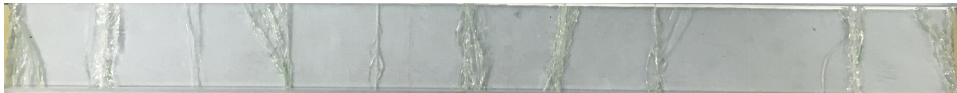


Figure A.15: Post-experiment crack pattern for specimen no.20.

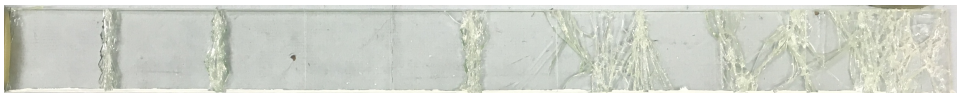


Figure A.16: Post-experiment crack pattern for specimen no.21.



Figure A.17: Post-experiment crack pattern for specimen no.22.



Figure A.18: Post-experiment crack pattern for specimen no.23.

A. Blast Simulator Crack Pattern



Figure A.19: Post-experiment crack pattern for specimen no.24.



Figure A.20: Post-experiment crack pattern for specimen no.25.



Figure A.21: Post-experiment crack pattern for specimen no.26.

Cracked Specimen Temperature Data

B.1. Single-cracked data

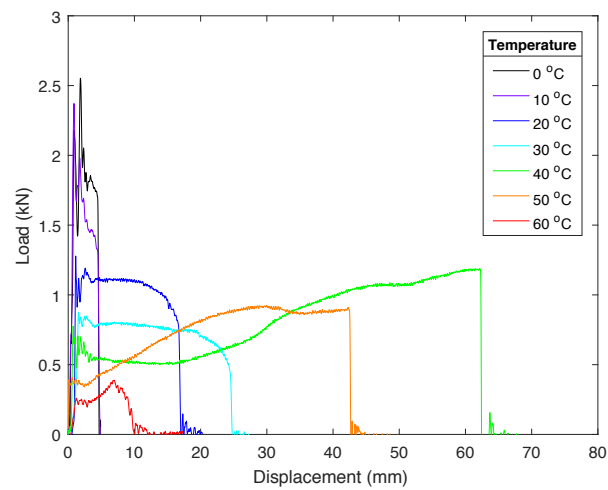


Figure B.1: Load against displacement at 0.76 mm interlayer thickness for single-crack tests – repeat 1.

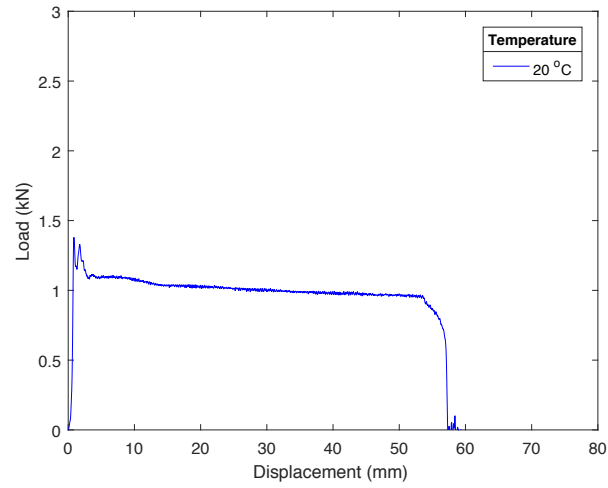


Figure B.2: Load against displacement at 0.76 mm interlayer thickness for single-crack tests – repeat 2.

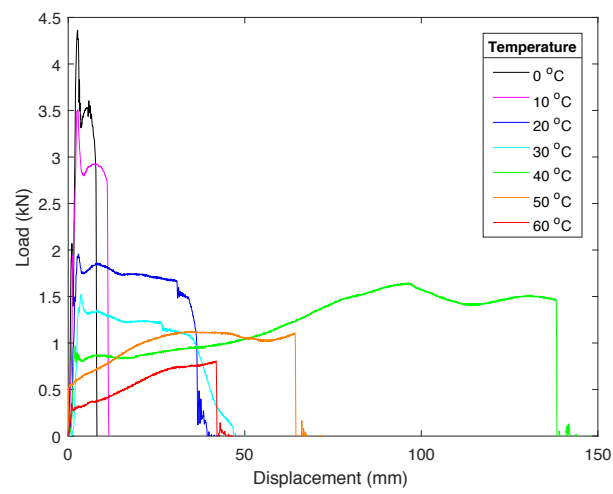


Figure B.3: Load against displacement at 1.52 mm interlayer thickness for single-crack tests – repeat 1.

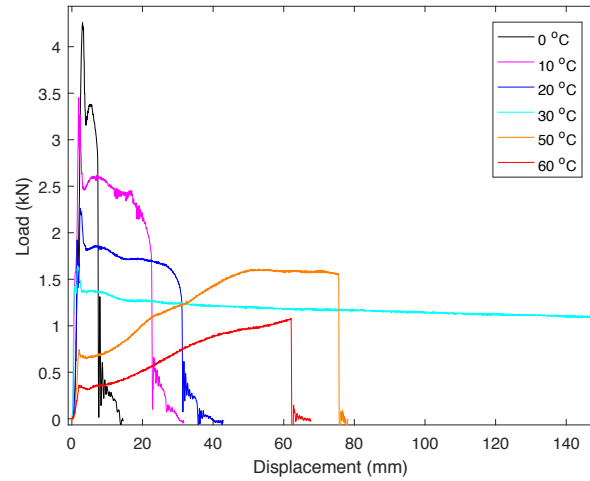


Figure B.4: Load against displacement at 1.52 mm interlayer thickness for single-crack tests – repeat 2.

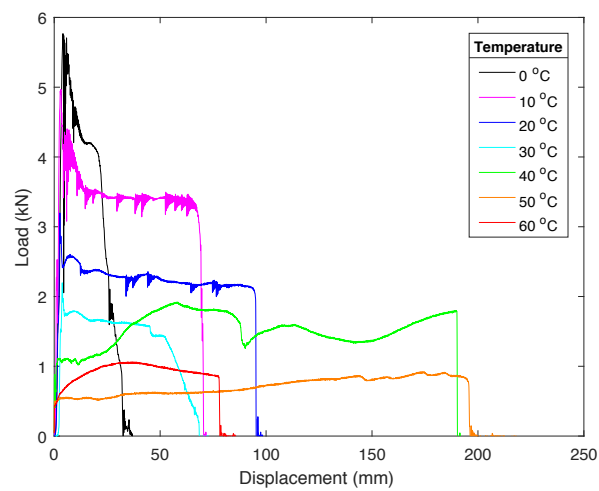


Figure B.5: Load against displacement at 2.28 mm interlayer thickness for single-crack tests – repeat 1.

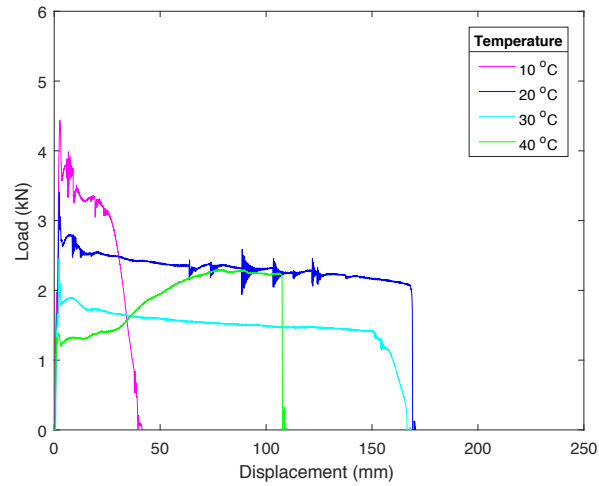


Figure B.6: Load against displacement at 2.28 mm interlayer thickness for single-crack tests – repeat 2.

B.2. Randomly-cracked data

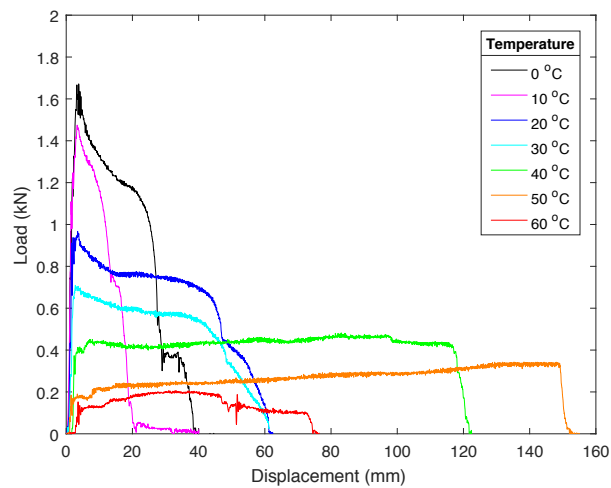


Figure B.7: Load against displacement at 0.76 mm interlayer thickness for randomly-crack tests – repeat 1.

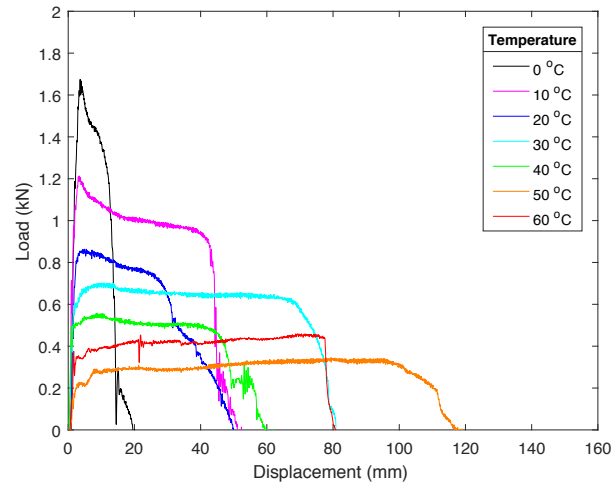


Figure B.8: Load against displacement at 0.76 mm interlayer thickness for randomly-crack tests – repeat 2.

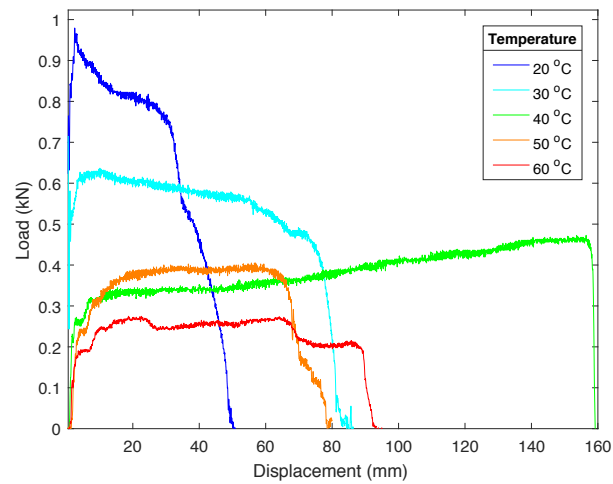


Figure B.9: Load against displacement at 0.76 mm interlayer thickness for randomly-crack tests – repeat 3.

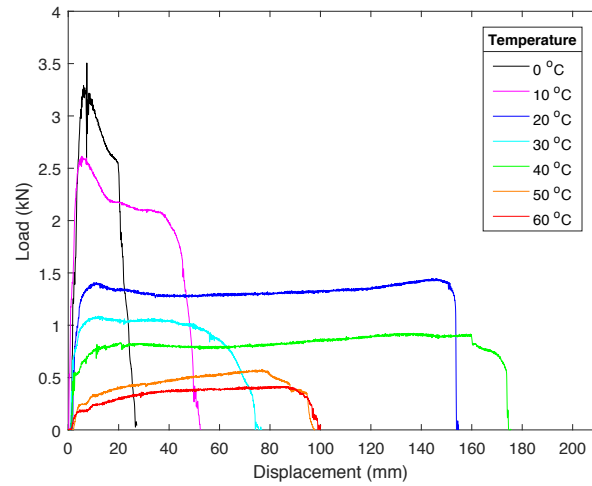


Figure B.10: Load against displacement at 1.52 mm interlayer thickness for randomly-crack tests – repeat 1.

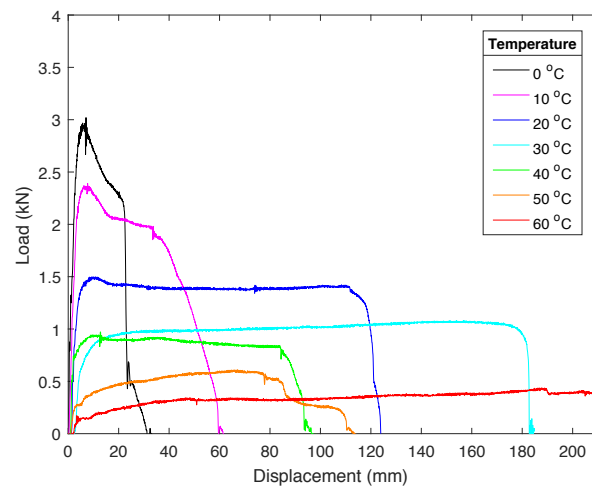


Figure B.11: Load against displacement at 1.52 mm interlayer thickness for randomly-crack tests – repeat 2.

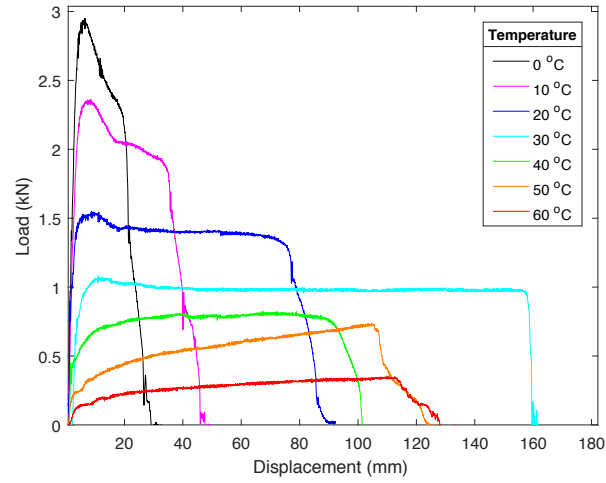


Figure B.12: Load against displacement at 1.52 mm interlayer thickness for randomly crack tests – repeat 3.

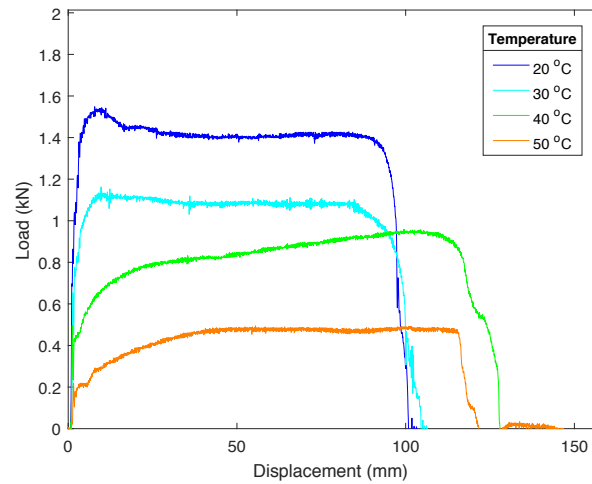


Figure B.13: Load against displacement at 1.52 mm interlayer thickness for randomly-crack tests – repeat 4.

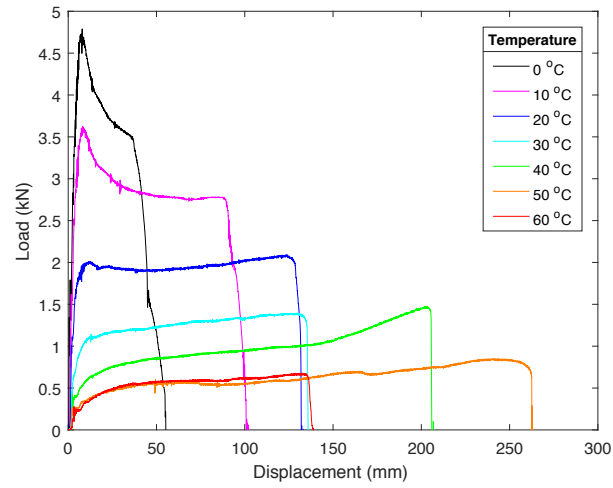


Figure B.14: Load against displacement at 2.28 mm interlayer thickness for randomly-crack tests – repeat 1.

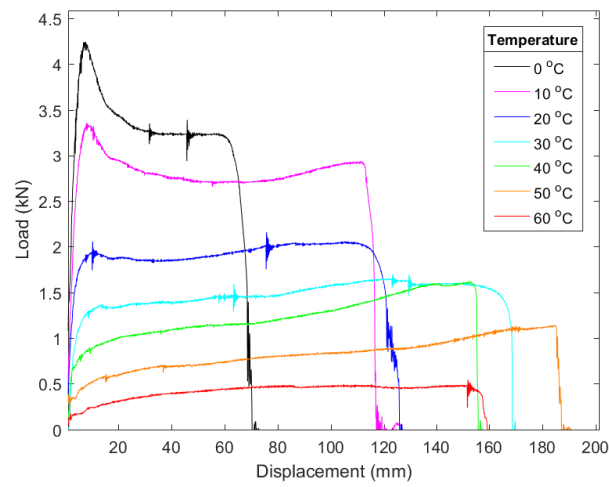


Figure B.15: Load against displacement at 2.28 mm interlayer thickness for randomly-crack tests – repeat 2.

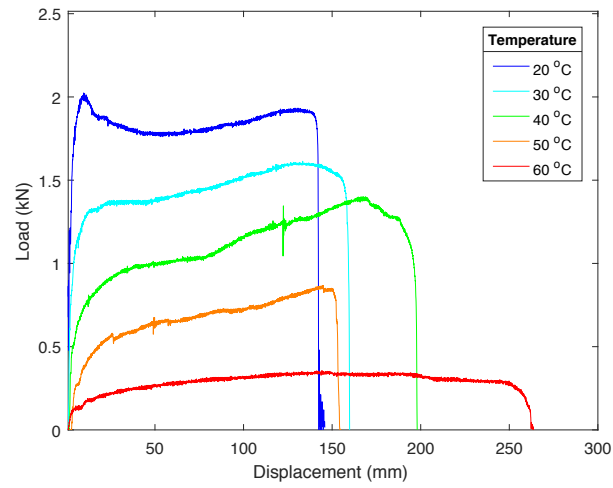


Figure B.16: Load against displacement at 2.28 mm interlayer thickness for randomly-crack tests – repeat 3.

