

**Impact performance and shear strength
of composite materials for aerospace
applications**

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Declaration of Originality

The work presented in this thesis was produced by me during the course of my PhD studies. Where contributions by others are used, they have been appropriately referenced in the text and in the bibliography.

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Abstract

This PhD research involved both shear and impact evaluations of composite materials for aerospace applications. Firstly, to fully understand the shear strength of composite materials for aerospace applications, the Iosipescu shear test was performed and the different specimen geometries were compared to obtain accurate out-of-plane shear properties. A multi-scale Finite Element Model (FEM) was devised in collaboration with AVIC The First Aircraft Institute (FAI) with suitable failure criteria to simulate the failure of thick laminate Carbon Fibre Reinforced Plastics (CFRP) under shear loading. The thick-sectioned laminates are modelled at a sub-laminate level whilst the structural failure is predicted at a ply level which incorporates shear nonlinearity for the thick composite structure. Based on this multi-scale approach, a user-defined FORTRAN subroutine (VUMAT) has been written for ABAQUS/EXPLICIT solver and is used to model the shear nonlinearity and intra-laminar failure. In addition, a cohesive zone model is used to predict the inter-laminar delamination. The modelling has been employed to predict the failure processes for Iosipescu shear test specimens with different fibre orientations.

The results show that both the failure mode and the load-displacement trace for finite element simulations agree closely with the experimental findings. This demonstrates the validity of this multi-scale, nonlinear, three-dimensional model for thick laminates. In particular, for the Iosipescu shear test, the effect of the fibres being aligned along the length of the specimen or out-of-plane is investigated as well as different dimensions of the specimen. This research has recently been published in Reference [1].

Secondly, for the impact evaluations of composites, low and high velocity impact performance of composite sandwich structures with foam cores were investigated. In particular, the idea of multi-layering the core with foam layers of different density and its effect on the energy absorption under low and high velocity impact is of interest. In this thesis, composite sandwich panels made of Glass Fibre Reinforced Polymers (GFRP) for skins and polyvinyl chloride (PVC) foam for the cores are used. Low and high velocity impact of the sandwich composite structure samples, using a drop weight tower and gas gun facility were performed.

By stepwise grading the foam core with different densities, it was found that there is a better energy absorption if you have a lower density on the impact side, and higher density at back surface. Rigid and compliant impact by using steel and high-density polyethylene (HDPE) was performed in the experiments. The sharp edge of a projectile cuts the composite face skin to produce more damage on the back face skin, but the graded foam core of lower density on the front can help to absorb more impact energy by spreading the load. Mechanisms of failure have been established such as core crushing, skin/core cracking, delamination and fibre breakage, and suggestions for modelling these failure processes is proposed. A paper is in draft on this research in collaboration with researchers at Royal Melbourne Institute of Technology (RMIT) [2].

Both of the shear and impact experiments were validated by using Digital Image Correlation (DIC) system. By introducing this method, the full field strain on a specimen during the experiments was recorded to determine the full structural response under static and dynamic loading.

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Nomenclature

E_1	Longitudinal modulus
E_2	Transverse modulus
G_{xy}	Shear modulus
τ_{avg}	Shear stress
ν_{12}	Poisson's ratio
Y_T	Tensile strength in the matrix direction
Y_C	Compressive strength in the matrix direction
S_L	Longitudinal shear strengths
S_T	Transverse shear strengths
$[\bar{\sigma}^*]$	Global equivalent stresses
$[\bar{\varepsilon}^*]$	Global equivalent strain
N	Number of plies
V^k	Ratio of the original thickness
$\bar{\sigma}_i^k$	Stress components for k _{th} ply
$\bar{\varepsilon}_i^k$	Strain components for the k _{th} ply
G_{13}^0	Initial shear modulus,
S_{13}	Shear strength
X_T	Axial tensile strength
X_C	Axial compressive strength
σ_n	Normal traction
τ_L	Shear tractions
ϕ	Fracture angle
μ_L	Longitudinal friction coefficients
μ_T	Transverse friction coefficients

d_f	Damage variable
G_{IC}^t	Intra-laminar fracture toughness in fibre tension
G_{IC}^c	Intra-laminar fracture toughness in fibre compression
V	Volume of an element and
T	Dimension in the thickness direction
C_{ij}^0	Undamaged stiffness coefficient
R_{ij}	Stiffness ratio
$\bar{C}_{ij}^{0,*}$	Equivalent undamaged stiffness coefficient
W	Out of plane thickness
H	Specimen height
ε_y	Major strain on y direction
U_z	Out of plane displacement
I	Moment of inertia
$v_{residual}$	Residual velocity of projectile and
Ω	Rotational velocity
E_{absorb}	Energy absorbsion

Acronyms and abbreviations

ASTM	American Society for Testing and Materials
AVIC	Aviation Industry Corporation of China
BSI	British Standards Institution
CLS	Combined Loading Shear
CFRP	Carbon Fibre Reinforced Plastics
DIC	Digital Image Correlation
DLS	Double Lap Shear
EN	European Norm
FAI	First Aircraft Institute
FEM	Finite Element Method
FGM	Functionally Graded Material
GFRP	Glass Fibre Reinforced Plastics
HDPE	High - Density Polyethylene
ISD	Interlaminar Shear Device
ILSS	Interlaminar Shear Strength
IR	Infra-Red
ISO	International Organization for Standardization
PVC	Polyvinyl Chloride
PUR	Polyurethane
PS	Polystyrene
PU	Polyurethane
PMI	Polymethacrylimide
PET	Polyethylene Terephthalate
RIFT	Resin Infusion under the Flexible Tooling
SAN	Styreneacrylonitrile
SBS	Short Beam Shear
UD	Unidirectional

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1. Introduction

This chapter describes the importance of strength analysis of composite structures for aerospace applications. There are two aspects to this research. The first aspect focusses on the out of plane shear properties of thick laminate carbon fibre reinforced polymer (CFRP) composite, and the second aspect focusses on the dynamic properties of composite sandwich structures subject to impact at various velocities. The two applications and problems of the different composite structures are introduced in this chapter, and the aim and objectives are outlined. The thesis structure and content of each chapter is also briefly outlined.

1.1. Thick laminate CFRP composite material

Composite materials serve a variety of different types of industries, from aerospace to sports. The use of composite materials is increasing rapid, particularly in commercial aerospace applications, as shown in Figure 1-1, in terms of light-weighting and high strength. Their use has also increased in the Airbus A350-900 XWB model, where the percentage of composite materials has reached 52%. Among these composite materials, thick laminate CFRP composite materials were used for the main loading structures such as the wing, empennage and fuselage. These thick laminate composite structures are facing the threat of possible shear loading failure in extreme conditions, which would cause a critical failure of a primary aerospace structure. For this reason, understanding of out of plane shear mechanics is very important for thick laminate composite structural designers and aviation engineers.

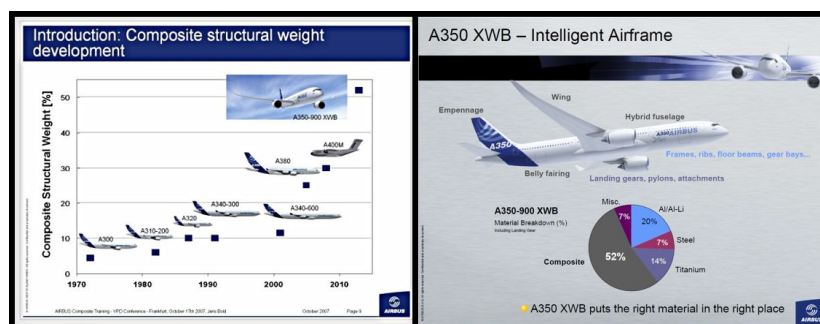


Figure 1-1. Composite used in the airplane application [3].

One of the main challenges in composite design is to predict the damage state of a composite structure under different loading condition up and until final fracture [4]. For example, when evaluating the Airbus A350 XWB, the European aircraft manufacturer put the wing under extreme stress tests. The Airbus test showed that the wings bend considerably when extreme loading is applied to them using hydraulic rams. This is shown in Figure 1-2. The results show the displacements up to 5 meters. This should make nervous flyers feel better about turbulence, but this also introduces a large amount of shear stress at the wing root, which could potentially (albeit it is unlikely with careful design) cause failure of the whole wing structure because of delamination or shear failure. However, it should be emphasized that these structure are carefully designed to withstand this extreme shear loading condition.

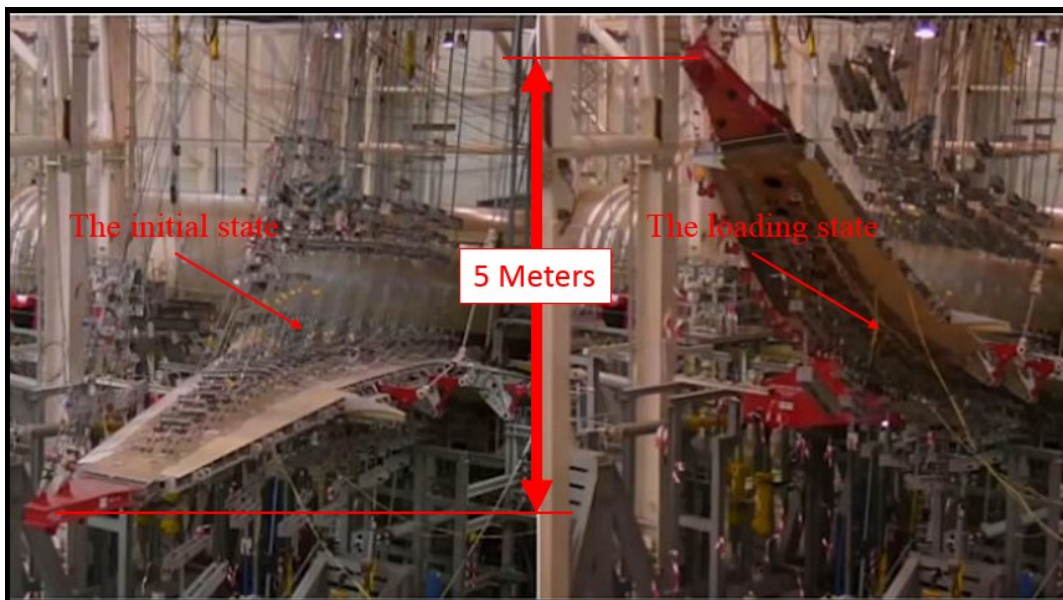


Figure 1-2. Loading condition for wing structures [5].

The correct measurement of the out-of-plane shear strength of composites is a known research problem, and the suitable simulation method and failure criteria are still much discussed. Just as in the Worldwide Failure Exercise (WWFE I & II), many researchers have investigated modelling methods and failure criteria [6, 7, 8]. This thesis presents an improvement to the above multi-scale approach. Chou's equivalent theory [9] is employed to calculate the equivalent elastic constants of the sub-laminates and the self-

correcting secant stiffness is used to describe the shear non-linearity. A user-defined material subroutine VUMAT is written for the ABAQUS/EXPLICIT solver. To integrate efficiently with in the VUMAT subroutine and to improve the efficiency of reading the material and layup information, both were saved in individual FORTRAN parameter code files instead of in a local text file. The parameter code is called with the “include” function, rather than with the “read” function. In this way, the material and layup information can be obtained directly from computer memory, which is much more efficient than reading data from a hard disk. Moreover, this method could be used in parallel computing without introducing any errors. With these improvements to the multi-scale approach, problems associated with large-scale complex, thick-sectioned laminated structures can be solved efficiently as described in this thesis.

1.2. Composite sandwich structure used for aircrafts

The structural sandwich concept involves combining two thin and stiff faces with a thick and relatively weak core. A structure of superior bending stiffness and low weight is obtained by sandwiching the core between two faces and integrally bonding the two faces together. For the composite sandwich structure, the two thin faces are made by the composite material to provide high stiffness and low weight. This structure is always used to provide good energy absorbing abilities. In Figure 1-3, the composite sandwich structure is used for the radome, landing gear doors, flap track fairings, engine environment structures (like fan case panels), and nacelle structures interiors.

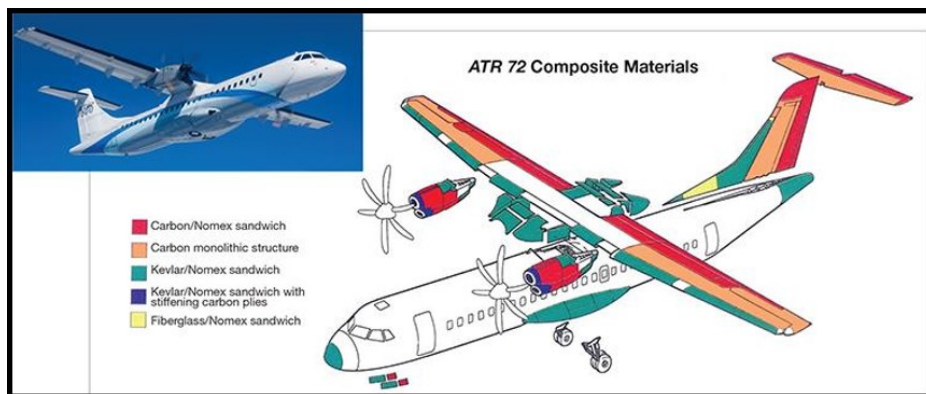


Figure 1-3. Composite sandwich structures used in ATR's ATR72 are illustrated here. They include the use of glass, aramid and carbon fibre reinforcement [10].

These structures on aerospace applications are constantly threatened by impact damage under low and high velocity. Threats include tool drops on the floor, bird strikes on the radome, hailstone hits on the wing structures, and even the “explosion” of the engine. As a result, these structures should have good energy absorption levels for low and high velocity impact, and the sharp and compliant impact should be taken into account as well. Figure 1-4 shows the damage on the radome and the damage on the engine structure caused by bird strike and hail. These serious damages on the structures can lead to a major problem for the aircraft.



Figure 1-4. Illustration of damage on an aircraft’s composite sandwich structures. Left: Emirates airbus rolls royce engine cowling went to pieces happened on 18 Oct 2006 [11]; Right: The nose cone was flattened by a sever bird strike as the plane landed at Nevşehir in Turkey [12].

Bearing in mind the above, investigations on the impact performance of composite sandwich structures are extremely important for aerospace applications. As a matter of fact, many airplane manufacturers have attempted to develop different composite structures to reduce the risk of impact damage. A great deal of research has been conducted on the impact performance of different composite skins as well as the different foam cores. The modification of the construction of the composite sandwich structure is also a crucial way to improve the impact performance of composite sandwich structures. In this research, the experimental study of the graded foam core composite sandwich structure was tested under different velocities. The thesis also assessed the impact of different types of impactors. The wide range of impact velocities and the different impactors can help to establish a better understanding of the impact performance of composite sandwich structures.

1.3. Aims and thesis outline

This research focusses on the static and dynamic properties of the composite structures used in aerospace applications.

For the thick laminate composite structures, the out of plane shear property is the most important property for the design of the main loading structures. Therefore, the accuracy of the thick laminate composite needs to be verified and models on the shear failure need to be developed. Out-of-plane shear property is difficult to model because of our limited understanding of delamination processes between layers and the failure mode for the different lay ups and configurations of the thick laminate composites. For thick-sectioned composite laminates, the ratio of span over thickness is relatively small and out-of-plane effects cannot be ignored. As a result, classical sheet or shell theory is inapplicable. Therefore, three-dimensional modelling and three-dimensional failure criteria should be employed.

To fully understand out-of-plane shear property response, different methods of shear testing were compared to the thick laminate composite conditions and the most suitable method to obtain the shear property was chosen. Secondly, the multi-scale modelling approach was developed in combination with the failure criteria and the damage evolution. Thirdly, the model was verified with the shear experiment result with the full field strain technique to make sure the model could predict the shear failure mechanism in the thick laminate composite.

For the impact study of composite sandwich structures, this PhD thesis measured the impact resistance of different constructions of composite sandwich structures under low and high velocities. The graded foam core was employed in the composite sandwich structures to obtain a better impact energy absorption level.

For both low and high velocity impact, the compliant and rigid impact levels were also taken into account. To fully understand the mechanical performance of different composite sandwich structures used in the aerospace applications, the study was divided into separate sections for each of the different configurations of graded foam core composite sandwich structures.

2. Literature review

In this chapter, the composite laminates and composite sandwich structures used for the aerospace applications are reviewed. The mechanical testing methods and the numerical modelling method for the thick laminate composite are also assessed. This essential knowledge helps researchers to acquire a better understanding of the problems of the composite structures.

2.1. Composite laminates

2.1.1. Unidirectional prepregs composites

Unidirectional (UD) prepregs composites are the most commonly used FRPs in commercial aerospace due to their exceptional specific properties. The in-plane performance of UD composites is generally superior to any other class of FRP. The reason for this is largely attributable to the way fibres are arranged within these materials. A lamina is the building block of carbon fibre composite laminated structures, and each lamina may have more than one type of fibre. These fibres may be oriented in different directions. Each lamina may have a different thickness, fibre orientation angle, fibre material and matrix material. Figure 2-1 illustrates how fibres in a UD prepreg laminate might be arranged. UD prepregs have highly aligned fibres with a regular packing arrangement that allows for a high Fibre Volume Fraction (V_f). These properties give UD prepregs their exceptional in-plane properties.

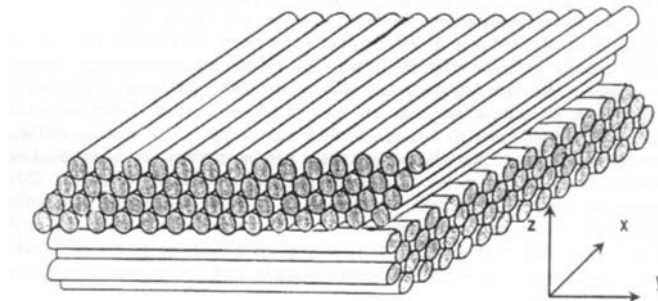


Figure 2-1. Illustration of fibre alignment in two layers of a cross-ply UD prepreg laminate [13].

A further benefit of UD preregs is the ability to optimise the layup for a specific load scenario. As each ply is discrete from the others, the plies can be set in any orientation or order. Whilst the other classes of materials can be manufactured with a variety of ply orientations, there are a limited number of ply angles commercially available. UD prepeg laminates, on the other hand, have an effectively infinite arrangement of possibilities. In addition, UD preregs are well disposed to automated placement. Automated CNC fibre/tape placement equipment is commercially available and can lay down prepeg material in specific orientations, locations and thicknesses in order to optimise the component for its design load.

2.1.2. Thick laminated composite

The thick laminated composite is a composite laminate with a small aspect ratio. As stated in the introduction, this structure is always used on the main loading structure. However, the structure is only accurate for composite laminates with a very large aspect ratio, such as those of thin plates. In order to calculate the correct stresses in thick composite plates, transverse shear deformation should be considered. In addition to the laminate thickness, there is another reason to account for the shear deformation in the composite analysis. Due to the low shear modulus of polymer matrices, the transverse shear deformation of composite materials is more pronounced than that of conventional metals [14].

Although the composite materials have high in-plane strength, they are very vulnerable in the thickness direction. Due to the weak bonding between the composite layers, delamination can easily occur on the composite interface. Two types of delamination, namely edge delamination [15] and central delamination [16], have been widely investigated. Both can be viewed as a result of interlaminar stress concentration caused by material property mismatch in the thickness direction. It has also been verified by many investigators that delamination has significant effects on structural integrity. For example, the compressive strength of a composite laminate can be reduced considerably if there is delamination. Consequently, the strength in the thickness direction is as important as that in the in-plane direction. The study of interlaminar stresses has thus become an important issue in the composite analysis.

2.2. Composite sandwich structures

Solid flexible cores refer to cores typically made of foam and are compressible so that through-thickness deformations are possible. Some of the earliest applications of sandwich panels in the 20th century have been in the aircraft industry [17]. This was followed by an expansion of applications into the aerospace, automotive and marine industries, especially after introducing FRP composite materials.

Sandwich structures with composite faces and a foam core are known for their good low weight with high stiffness and strength properties and have therefore been used in aircraft structures for many decades. Examples are aerodynamic fairings (belly fairing, leading, and trailing edge fairings) and control surfaces (rudders, ailerons) [18].

Since weight reduction is a main motivation in aircraft design, the application of sandwich structures is increasing rapidly. Even concepts for the utilisation of sandwich structures in the primary structure, such as aircraft fuselage, have been developed for large airliners [19, 20, 21] and have already been implemented in smaller business jets [22-25]. One main issue is to identify an adequate sandwich core material for these purposes. Therefore, the different possibilities will be introduced below for comparison.

2.2.1. Core materials

2.2.1.1. Polymer foam cores

Polystyrene (PS) foams were first made in 1931. However, polyurethane (PU) was developed by Dr. Otto Bayer at the beginning of the Second World War. It was used to replace the rubber and also as a coating to protect other common materials, such as wood and metals.

A few years after the war, flexible PU foam was invented and used for cushioning in furniture and in the automotive industry. Nowadays, common polymer foams include polymethacrylimide (PMI), polyethylene terephthalate (PET), styreneacrylonitrile (SAN), and polyvinyl chloride (PVC) foams.

Over the last few years, rigid polymer foams have gained interest as core materials for sandwich structures in aerospace, marine, automobiles and other commercial applications due to their higher energy absorption capabilities, especially in the event of impact loading [26, 27]. Among polymer foams, polyvinyl chloride (PVC) and polyurethane (PUR) foams are the most widely used core materials in sandwich constructions. The foam core plays a very important role in increasing the stiffness and also in controlling the failure mechanisms, especially in absorbing impact energy. The structural response of a polymer foam strongly depends on the foam density and solid polymer properties [28].

2.2.1.2. Honeycomb cores

As shown in Figure 2-2 (a). Honeycombs are based on series of cells, nested together to form structures similar in appearance to the cross-sectional slice of a beehive. In its expanded form, a honeycomb is between 90-99% open space structure. Honeycomb structures are produced by using a variety of different materials, depending on their end application. For example, honeycomb cores made from polypropylene are used for low strength and stiffness load applications, while aluminium honeycomb cores with high strength and stiffness are used in high-performance applications. In addition, aluminium honeycombs are fire-retardant and have a good impact resistance. They offer the best strength-to-weight ratio of the core materials used in aircraft industries. However, aluminium honeycomb is more expensive and difficult to use in complex shapes. In general, the mechanical properties of honeycombs are highly anisotropic. In sandwich construction with foam or honeycomb as a core, the primary loads are carried by the facesheets. However, in textiles or truss-cored structures, a portion of the primary load is carried by the core.

2.2.1.3. Corrugated cores

Corrugated cores (shown in Figure 2-2 (b)), especially cardboard, are well known for their low costs. In 1856 the corrugated paper was patented in England, and was used as a

liner for tall hats. The single-sided corrugated board patent was awarded to Albert Jones [29] in 1871. Jones used the corrugated board for wrapping bottles and glass lantern chimneys. In 1874, the first machine for producing large quantities of corrugated board was built by G. Smyth. In the same year, Oliver Long [30] improved a design proposed by Jones and invented a sandwich structure with liner sheets on both sides and corrugated board in between.

During the past decade, many attempts have been made to study the effects of dynamic loading on corrugated core sandwich structures. Kazemahvazi et. al. [31] reported that when corrugated structures are under the quasi-static loading condition, they typically fail through plastic buckling with a sharp drop in load after the initial peak. However, it was later discovered that the buckling response of the core decreased significantly with increasing loading rates, resulting in an increased peak load in the structures. Fleck and Deshpande [32] found that corrugated cores provide a higher blast resistance than pyramidal lattice cores. In addition, the dynamic behaviour is considerably different from their response under quasi-static conditions for the corrugated sandwich structures.

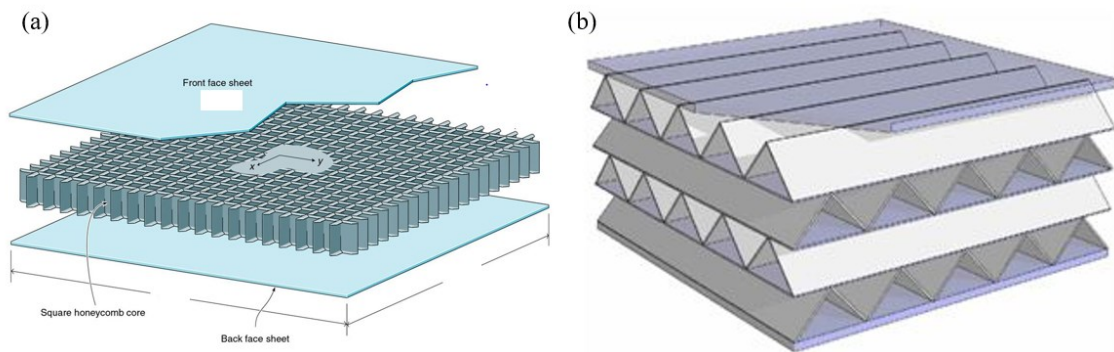


Figure 2-2. Schematic of honeycomb core (a) and schematic of corrugated cores (b) [33].

2.2.2. Graded foam core

The graded foam core composite structure interested researchers owing to its potential to improve the ability of impact energy absorption. As shown in Figure 2-3, the graded foam core composite sandwich structures can have multilayers instead of a single layer of foam between the two faceskins.

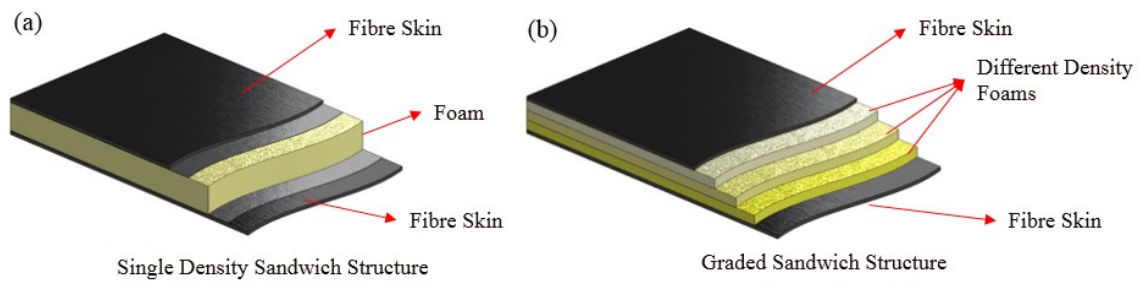


Figure 2-3. Single density foam core (a) and graded foam core (b) composite sandwich structure.

The applicability of sandwich structures could be improved if they contained a graded core, which might help to distribute the stresses due to bending or in the progressive absorption of energy under impact loading [34]. It is necessary to study the behaviour of composite sandwich structures under different types of failures with a graded foam material as a core, in order to explore their potential application to reduce bird strike damage and crash worthiness. Graded foam cores represent a new class of materials where property is a function of geometry (such as thickness, density and length) [35]. The composition and microstructure of these materials are not uniform in space, it can have varieties of different materials but gradually vary following a predetermined law [36-39]. The concept of graded foam core structure was proposed as early as 1984 by material scientists as a means of preparing thermal barrier materials [40]. Laminated composites are most similar to graded material with variation in laminate properties. However, they possess distinct interfaces across which properties change abruptly [41]. For example, a rocket motor casing can be made with a material system such that the inside is made of a refractory material, the outside is made of a strong metal, and the transition from the refractory material to the metal is gradual through the thickness [42]. Composite sandwich structure with graded foam core material has a number of advantages that make them attractive in many applications, including a potential reduction of in-plane and transverse through-the-thickness stresses, an improved residual stress distribution, enhanced thermal properties, higher fracture toughness, and reduced stress intensity factors. It is worth mentioning that the distribution of the material in functionally graded structures may be designed to accommodate various spatial specifications. Currently, advanced processing methods to introduce compositional gradients into various material systems are being developed by materials scientists [43]. A typical particulate composite

with prescribed variation in the distribution of constituent phases could be a representation of graded material structures. The graded material structures concept could be borrowed to make sandwiches with a graded core that exhibit resistance (stiffness) proportional to the applied load. These could serve some applications better than regular sandwiches, like a spring with varying stiffness. Such a sandwich could be realized by using a particulate composite with a varying volume fraction of constituents.

2.3. Mechanical testing of composite structures

2.3.1. Shear test for the thick laminated composite

The shear testing of composite materials has proven to be one of the most difficult areas of mechanical property testing, which in turn can lead to unreliable results. There are several test methods available for determining the shear properties of composite materials, such as the $\pm 45^\circ$ tensile test (ASTM D3518 [44]), the Iosipescu shear test (ASTM D5379 [45]), the V-notched rail shear method (ASTM D7078 [46], Figure 2-4 (a)), and the short beam shear (SBS) test (Figure 2-4 (b)). Different authors have shown that these two shear strength and shear modulus results show that each of these methods has drawbacks and inaccurate results for the problem. There is no way to respond to all the conditions for proper shear performance. However, the Iosipescu shear test method does show some particularly favorable characteristics [47]. Van Leeuwen [48] indicated that the measured shear modulus values are nearly the same for different test methods. The ultimate shear strength values show more pronounced differences. Some non-typical shear test methods were also developed and compared with the short beam shear (SBS) test, such as the interlaminar shear device (ISD), which loads the edges of a block specimen [49]. The test revealed that the ISD method gives a simpler and better defined shear stress distribution than the SBS test. Another non-typical method, the double-lap-shear (DLS) test, is described in Ref 50. The DLS test is not well suited to predict the shear properties if standardized analysis is employed. Pahr, D H also fully demonstrate that the real ILSS cannot be obtained from the SBS test without the extended analysis. However, it is possible to determine the boundaries of the ILSS from the results of the SBS as true.

Recently, a combined loading shear (CLS) test method was developed by International Organization for Standardization [51]. The methods combine the edge and face loadings into a new test fixture design. The SBS method is the most common test used to obtain the interlayer shear properties. Several standards, including ISO 14130 [52], EN 2563 [53], ASTM D2344 [54], and SACMA SRM 8R-94 [55], recommend this approach to attempt to quantify the apparent value of the shear properties of parallel fibre-reinforced composites. The specimen is short, relatively deep, and flattened into a three-point bending structure with a narrow support span. The narrow range of intent is to simultaneously maximize the in-plane shear stress to reduce the bending stress. According to the Euler-Bernoulli beam theory, the distribution of the shear stress parabola across the specimen has a maximum at the centre of the face. However, the beam hypothesis assumes a large proportion of the depth h of the support of the span L , which is not suitable in this case, and therefore, theoretically cannot be applied to the short beam test. Therefore, several problems occur [56]. Edge shear, shear stress, non-linear behaviour, imperfect stress distribution, and the presence of normal stress, so the determination of shear strength is questionable. The contact stress caused by the load point greatly distorts the stress distribution, often resulting in fault loading under the nose. The resulting failure is only a pure shear failure. In contrast, the failure results from the presence of complex stress states in the samples are shown and others. Analysis by Adams and Lewis [57] reported that a width-to-thickness ratio greater than 2 can lead to significant shear stress variations in the width direction. Further difficulties with tough matrix resin composites are encountered in the classification of thermoplastics.

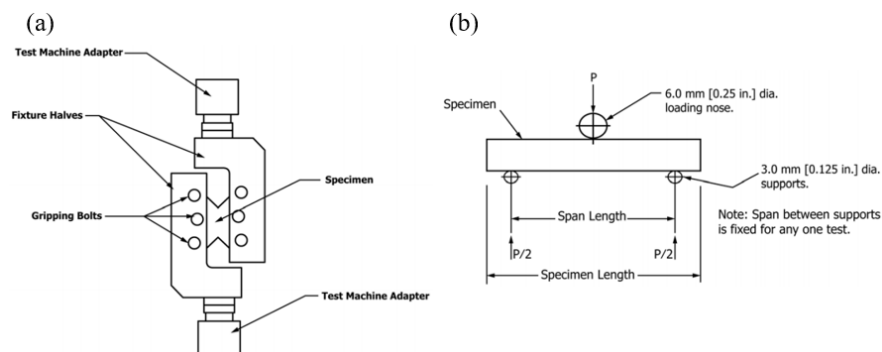


Figure 2-4. (a). Schematic of V-notched rail shear method [46]; (b). Schematic of short beam shear test [54].

2.3.2. Dynamic test for the composite sandwich structures

An increasing amount of research has focused on sandwich structures and aspects of the problems involved in those structures have been addressed. Responses of sandwich beams and plates subjected to quasi-static loading and dynamic/blast loading have been extensively studied and documented in detail in the literature [58-61]. The principal mechanisms have been clarified by numerical simulations [57,61] and analytical models have been developed to predict the blast response of sandwich beams and panels [63-66]. However, most of the experimental studies on the response of sandwich construction to blast loading have focused on flat panels or beams; work on curved sandwich panels is very limited [67-71].

2.3.2.1. Low-velocity impact

Low molecular weight composite sandwich panel impact of foreign body covering the sub - critical damage to the whole perforation reaction. At low impact energy, there may be skin lesions and skin core stripping. The point of interest here is the relation between the peak impact load and the area of impact damage [72]. A related problem is the investigation of contact conditions and material properties of these conditions [73,74]. It is also important to impinge [75] after the residual in-plane strength of the panel. As the impact energy increases, the skin starts to perforate and large-scale core damage occurs. Energy is absorbed as the skins and core become damaged. Many papers have discussed the perforation energy absorption mechanism [76-78]. The mode of energy absorption depends on the material of the panel, the geometry of the part of the panel, and the impact conditions. Mines [77]. analysed the use of sandwich panels in marine sandwich structures and has determined that the shear of the skin is absorbed as much of the energy. Wen et al. [78] also analysed the oceanic sandwich structure, which identified the main energy absorption modes of the APT and the global panel beneath the deformation fragmentation. Finally, the authors of the reference 79 analysed metal honeycomb core or polymer composite skins. They found that the energy absorption mode is the core of the crushing; bending and stretching of the top surface; and wrinkling and punching through the top; and the structural bending of the whole.

The next step is a variety of components that quantify energy absorption during perforation. This method has been discussed for the dynamic collapse of sandwich beams [80,81] and has been shown to affect the global energy absorption behaviour of the beam and the squeezing behaviour of the core and the tensile failure stress of the underlying skin. As for the panel, Wen et al. A perforation model based on metal-plastic theory was developed [78]. In their models, such as skin strain characteristics of the material is important. Global geometry effects are also included. Zee and Xie [79] have developed in-depth analysis of perforations of laminates due to ballistic impact. They quantify the contribution of energy absorbing fibres, matrix, delamination and friction. Wierzbicki et al. [72] extended the work of Goldsmith and Sackmann [75] by developing an analytical model to quantify the effect of the local energy absorption mechanism on the thickness of the panel. Impact punch and panel conditions discussed herein involve relatively large impacts (50 mm in diameter) and relatively low impact speeds (up to 8 m s^{-1}). These conditions produce large local deformations in the panel [80]. Thus, the energy absorption mechanism is different from those described above. It will be shown that the density of the nucleus affects the perforation mechanism. (Density of 84 kg m^{-3}) and Coremat (density: 640 kg m^{-3}) are the two core materials considered in this paper, aluminium honeycomb. The latter is a proprietary material made from non-woven polyester, with 50 % of the microspheres being resin impregnated mats [82]. The trade name for honeycomb panels is Aerolam [83]. It is noteworthy that, here, through the work described by Wen et al. [78]. Consider the plate having a density of three cores, i.e. 100, 400 and 475 kg m^{-3} .

2.3.2.2. High-velocity impact

Mezzanine structure as a part of the body of the aircraft may face the impact of projectiles, such as bird strike, hail, debris flow runway, and loading ballistic missile threats. This will result in damage to the structure and core fragmentation, surface sheet indentation, intact internal peel or peeling, and visible light penetration or perforation. All of these types of damage can lead to strength and stiffness reduction in structures [84]. Then the need for high impact velocity sandwich panel penetration and perforation resistance to define different skin materials (polymer, aluminium, fibre reinforced) and various core materials, including trusses, honeycombs, corrugations and foams in different materials

The panel, with open or closed cells, provides high specific strength and energy absorption capabilities.

In recent years, some experimental studies have been carried out to characterize impact damage in sandwich panels. Some studies have investigated the high-speed collision behavior of sandwich structures [85-91]. Most studies have focused on failure loads, ballistic limit velocities, perforation energies, and damage induced in composite sandwich panels subjected to projectile impact, and also improve the damage resistance properties of composites [92]. Of the composite sandwich structure undergoes significant damage such as fiber breakage, matrix cracking and delamination followed by impact penetration into the core [93]. The face sheet defined in Vinson [94], as the primary load-bearing element in the composite sandwich structure, found that the optimum design of this structure must take into account that the topsheet responds to both static and dynamic loads. Weihong Hou et al [95] studied the ballistic performance of quasi-static and aluminum foam core metal sandwich structures affected by perforation. They discuss the impact of several key parameters on the trajectory limit and energy absorption in perforated plates. These effects include impact speed, skin thickness, thickness and density of the foam core, and the shape of the pellets. Villanueva and Cantwell [96] studied the stratification and sandwich structure of the fabric and the unidirectional glass fiber composites skinned longitudinally with the core of the fiber-based / polypropylene-based foam aluminum. In contrast, a woven glass fiber / polypropylene type sandwich structure exhibits a small amount of peeling after the high-speed impact test. They report that the foam aluminum in both systems exhibits localized indentation failure after collapsing at higher impact energy [97]. As in the case of functionally graded sandwich structures, the failure mechanism also changes as a function of density distribution. Goldsmith et al. [96] Experimental studies of different types of honeycomb core perforation band metal / plastic cover a wide range of amazing speed. He discusses the concept of ballistic limits for composites and sandwich structures, and examines the behavior of perforation after velocity versus initial velocity. V. Skvortsov et al. [99] Consider the elastic and dynamic response of FRP laminates and foam core sandwich plates during impact. They studied matrix cracking, core fragmentation, delamination, delamination, fibre failure, and energy absorption. Velmurugan et al. [96] studied the

response of three sandwich panels (checkerboard / epoxy / foam; chopped mat / epoxy / foam; and checker mat / chopped mat / epoxy / foam) The impact velocity in the range of 30 to 100 m⁻¹. They report ballistic limits, residual velocities, and changes in energy absorption for different types of cores. Kepler [101] looked at sandwich panel penetration and failure mode and showed the most significant contribution from the core material and the impact of the membrane between the tensile fibres, core compression and friction. He also asserted that small contributions were derived from delamination, core fracturing, and stripping between the core and the back sheets. He uses a simple physical model to estimate the contribution of energy absorption. Goldsmith and Sackman [102] investigated the effects of several parameters on energy dissipation during the perforation. These include impact velocity, boundary conditions, and bond strength between the honeycomb core and the facing aluminium. Hoo Fatt and others. [102-104] The analytical solution developed was subjected to normal shocks with different shapes by the projectile, and wherein the overall bending and the drawing of the plate were considered as the ballistic limit of a honeycomb core sandwich plate. Although extensive research has been published on the different aspects of foam-filled sandwich structures, in particular the optimization of foam density, high velocity shock response is one area that has received less attention.

2.4. Modelling of laminate composites

2.4.1. Finite element model method

Although most commercial finite element software has already integrated analysis modules for composite structures, many of these are based on the classical two-dimensional laminate theory or three-dimensional ply-by-ply modelling [105-108]. For composite laminates with hundreds of layers, a three-dimensional ply-by-ply FEA model would be too time-consuming for both pre-processing and calculation.

Numerous researchers have investigated progressive failure analyses using linear, orthotropic material properties. By using newly developed damage evolution criteria, Lapczyk and Hurtado [109] use the progressive damage features within ABAQUS to model open-hole tension tests of aluminium/composite sandwich structures. Zhang et al. [110] used a modified Hashin criterion to build an FE model with grid stiffened plates subjected to damage. The delamination and progressive damage of tension, interlaminar shear and compression are investigated on open-hole sample. Spottswood and Palazotto [111] looked at the progressive failure of complicated curved composite shells up to and through their snapping point by using use simplified large displacement and rotation theory. And by the author Huhne et al. researched the effects of two material degradation models on the response of composite bolted joints [112]. They compared the experimental result with the model result, and found that the constant and continuous degradation approach can be incorporated into an ABAQUS subroutine. Whilst Xie and Biggers [113] use a progressive damage model to discounts the plies of composite after the failure appear. And the difference of width-to-hole-diameter ratio on open-hole tensile was compared by using this method. For Labeas et al [114], they use ANSYS to compared the experimental results and model data to find the best combination for two different failure theories and two degradation models, so the progressive damage and buckling of an open hole compression specimen can be simulated. Tserpes et al. [115, 116] created a user material for ANSYS that uses the Hashin criteria and the ply discount method to model progressive failure.

Nonlinear stress-strain response, especially in the transverse and through-thickness directions, is an important aspect of composites. There are a number of scientific papers that incorporate progressive failure with nonlinear response. Schuecker and Pettermann [117] create a continuum damage model that they compare to results from the World Wide Failure Exercise (WWFE). Hochard et al. [118] develop an ABAQUS user material using shear damage and inelastic strain evolution laws. Both static and fatigue loads are applied to open-hole samples, and strain fields are compared to data obtained from digital image correlation. The failure of tapered composites and the post-buckling reaction, especially those with a stratified decrease, are progressed by Ganesan and Liu [119]. Using the non-linear layer characteristics and the maximum stress failure criterion, multiple layups were opposed to each other. KILIC and Haji-Ali [120] developed ABAQUS user materials that utilize the nonlinear behaviour of Ramberg-Osgood and the Tsai-Wu failure criterion to simulate changes in single-bolt and naked-eye tension tests. Open-pore and double-gap compressed samples were modelled by Basu et al. [121] employs a layered shell element, and the ABAQUS user material. The nonlinear material properties are modelled using the Schapery theory and rotational axis compression of the fibre. Antoniou and so on. [122] According to the simulation of compression, tension, torsion and loading of thick cylindrical shell composite materials in ANSYS. The linear, elastic fibres of the directional response and nonlinearity of the transverse and shear response of the user material were used to create the first and last layers of the failure of the envelope for comparison with the experimental data. Huang [123] developed the ABAQUS user shell section that degrades nonlinear fibre and matrix performance when a bridge mode failure occurs. Mesh size, element type, and load application effects were studied and compared experimental results. A unique aspect of this failure analysis is that if the ply associated with the element fails, the ply in the entire structure is discounted. Although there are several examples of linear and nonlinear properties coupled with progressive failure analysis, none of these papers have the ability to model effective properties by homogenizing a material into a material. An important part of modelling is the computational efficiency of the code so that designers can quickly analyse many design iterations.

2.4.2. Failure criteria

For three-dimensional failure criteria, several solutions based on meso-mechanics [124-125] and macro-mechanics [126-130] were proposed in the Second Worldwide Failure Exercise (WWFE-II). In these analyses, most employ a ply-by-ply approach except for Bogetti's multi-scale theory [130]. Bogetti's multi-scale theory is based on sub-laminate homogenization and decomposition of sub-laminate stress and strain. [131] With this method, the number of elements in the thickness direction of a laminate could be greatly reduced, and it is quite suitable for modelling thick composite laminates. Based on this multi-scale approach, several kinds of user subroutines were developed to solve thick laminate problems. [132] In these subroutines, the tangent stiffness is used to calculate the nonlinear shear stresses. In this approach, a very small load increment is required to calculate the stresses, but the tangent stiffness method is not self-correcting and numerical errors can be accumulated. Furthermore, when using the above subroutines, a database file that contains material and layup information will repeatedly be read from the local hard disk of a computer or server. [133, 134] Reading data in this way is inefficient and would cause reading errors when multiple processors are used for parallel computing.

2.4.2.1. The first worldwide failure exercise

The first Worldwide Failure Exercise (WWFE-I) was published in a special issue of Composites, Science and Technology in 1998 [135]. The reason why the exercise need to be set up is because there are no standard failure theories for composite, and the WWFE-I was made after a meeting of experts in 1991. A key question was that is there any of the failure theories for composite work [136]. It was clear that there were certain issues to resolve. Firstly, whether the failure theories worked or not seemed irrelevant, as the industry lacked faith in the failure theories at that time. This lack of study was very important by the lack of a definition of composite failure. For example, the sporting equipment may be considered a failure if its stiffness reduces by a certain percentage. But for a pipe designer, he may consider that if liquid escapes through the walls of a pipe, the composite would be failure. These are clearly not a single definition of failure for composite.

In the first exercise, the proposed question of the WWFE-I may seem like a simple question to answer due to the amount of literature available on the subject. However, an assessment of the literature and a review of theories available to the authors of the exercise were selective in the cases chosen as demonstrated by the references in the first exercise [136-138]. Hinton, Soden and Kaddour conducted WWFE-I to solve the problems and helped to find a clear answer to the question of whether the failure theory worked for FRP. During the planning phase of the first exercise, the list of eight-point instructions the organizers have finalized should be followed in order to carry out a fair examination of the fault-providing theory [135].

WWFE-I was using such a rigorous instruction for the first exercise, which was ultimately leading to its success, which required this precise profile.

The first exercise focused on the only two-axis test case. This is considered to be a suitable starting point for evaluating the state of the FRP failure theory. It is also important that the size of the task is not overbearing in the future. The exercise was originally divided into two parts. Part A is presented by the participants of their blind forecasting with the organizer [140] for each given case, with different theories comparing the whole of the papers presented. Part B compares the theoretical experimental data and gives participants an opportunity to comment on their theoretical and experimental data and give them an opportunity to adjust their results [141,142].

The organizers succeeded in assembling 11 participants in parts A and B, including from Aberdeen, Rotem, Parker and Zinoviev [142-153]. Part B of the review, it is clear that a number of theories stand out from the rest. These are proposed by the theory of Puck, Zinoviev and Aberdeen [144,146,150]. The organizers commented that the three theories predict all cases and are among the best predictions of initial and final failure. They also note the success of predicting other parameters, such as pattern and failure sequences. It should be noted that other theories, such as those proposed by McCartney, have never been used to predict the results for all cases in WWFE-I. These contributors should applaud what he tried to exercise.

Following part A and part B, the organizer conducted part C of the first exercise [155]. In this new theory appeared in the literature and invited to participate in the light received

by the four new participants [156-158]. The same processes on parts A and B were subsequently carried out by participants, as well as from four new theories which Bogetti and Cuntze commended with the three leading theories of A and B [155,156]. WWFE-I is, and still is, a prominent work in the field of composites. Part A published more than ten years later, the first exercise is still regarded as the most comprehensive, best quality work FRP for evaluating the state of failure prediction [160].

2.4.2.2. The second and third world wide failure exercises

As a result of the success of the first exercise, the organizers announced the understanding of the ability of two more exercise plans to further the various fault theories. The second exercise was developed to evaluate the current theory, and they analysed the effectiveness of the triaxial loading case.

The third exercise, on the other hand, is designed to evaluate the current theory of the ability to predict damage to composites [161]. Of these two new exercises, WWFE-II is most closely related to WWFE-I and provides the next big step in the FRP failure theory of the country. This is slightly earlier than the WWFE-III launch and scheduled for release in the summer of 2011.

The main difference between the WWFE-I and the WWFE-II is that the first exercise is only concerned with the in-plane loading and response while the second exercise is concerned with triaxial loading and 3-D stress reactions. During the first exercise, many participants proposed a simplified three-dimensional theory so that the starting point for WWFE-II was to invite the participants of the first exercise to participate in the second. The six receptions (or nominees standing on their) of the participants, along with the six, the other six participants were also confirmed to participate with new theoretical or model techniques. The final list of participants can be viewed in 162 to view these participants from a number of countries and institutions, and therefore should provide failure theory for an open view of the state of the FRP.

The purpose of the third exercise is to verify that the contributors claim to be able to predict damage under load, such as transverse crack stratification, matrix crack initiation and propagation of deformation until fracture failure theory. As with other exercises, a

series of test cases to a comprehensive test theory have been selected on a series of laminates. Similarly, a large number of participants were invited to come from different countries and professions, covering academics, industry and software companies. In total, seventeen participants will participate [163].

The eight-point instruction list set out for the first exercise is being followed for the second and third exercises. Of interest to the author was Instruction 6:

High quality experimental data should exist for all the cases being solved theoretically. This will enable theories to be benchmarked against test data rather than against other theories.

The WWFE was selected by the organizers as WWFE-II and WWFE-III, but they did not have the required experimental data in the twelfth and final case of WWFE-II. The proposed test case is based on the cross-layer thickness compression test, $[0/90]_s$ laminate.

2.5. Summary

There has been significant research on the out of plane shear strength of thick laminates by both experimental and modelling studies. Firstly, this PhD study focusses on application of a model to the Iosipescu shear test. Secondly, the topic addressed is the impact performance of composite sandwich structures. The focus of this impact study is graded core sandwich panels. These two topics are particularly important for aircraft structures.

The experimental evaluation and the modelling of the shear performance for the thick laminate composite needs development for determining shear strength properties and predicting shear failure. Furthermore, the traditional modelling is too time consuming for the engineering application. Therefore, in this PhD thesis, the Iosipescu shear test with the DIC method was investigated to obtain the accurate shear property for thick laminate composite, and the ply-by-ply modelling approach with the combined failure criteria is developed for predicting the shear failure and delamination between layers. This research has been published recently in the Journal - Composites Part B [1].

For the impact study of composite sandwich structures, the focus is on the localized impact with low and high velocity on the graded foam core composite sandwich structures. This is with the aim of improving the impact energy absorption levels of the structure with a graded foam core. This thesis investigates the localized low and high velocity with rigid and compliant impactors. With high-speed DIC full-field strain tracking method, a good understanding of the impact performance of the graded foam core composite sandwich structure can help us to improve the impact energy absorption ability.

3. Materials and experiment procedure

In this chapter, the material and the experiment methods used in this thesis are introduced. The materials used are the thick laminate CFRP, the composite sandwich structures with polyvinyl chloride (PVC) foam cores, and glass fibre skins. The experimental methods in this research are the Iosipescu shear test for the quasi-static test, and the low and high velocity impact tests by using the drop tower and gas gun facility.

3.1. Composite structures

3.1.1. Laminate composite

The material used in the thick laminate out-of-plane shear performance research is the CFRP laminate composite. The laminate composite panel is provided by AVIC and made from the prepreg material in autoclave. The CFRP panel is made by the CCF300/5228A material system, whilst CCF300 is the fibre and 5228A is the resin. The material has 160 plies and the total thickness is 20 mm.

The panel is cut by the water jet shown in Figure 3-1, and the V-notch is cut on the specimen by the right demensions. The water jet cut can keep the fibers and resin from becoming damaged and can control the quality of the specimen. These specimen are used in the Iosipescu shear test for the investigation of the shear project.

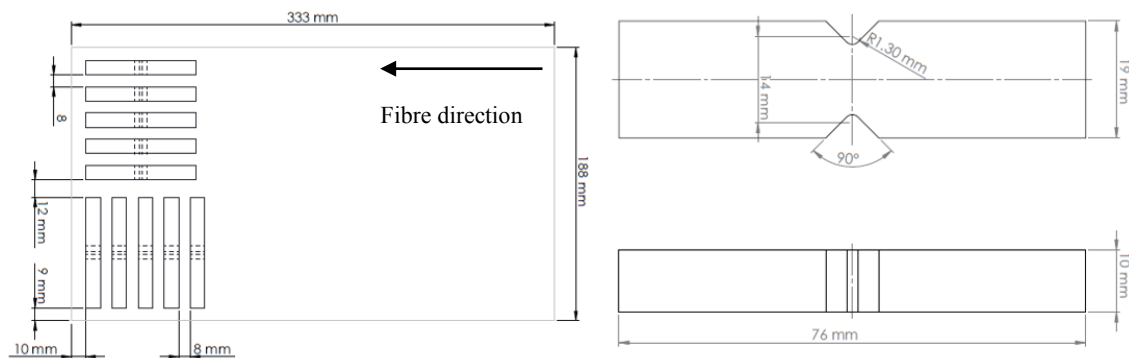


Figure 3-1. Water jet cut panel and specimen geometry.

3.1.2. Sandwich Structure

As shown in Figure 3-2, the composite sandwich structure is made by the glass fibre for the skin and the PVC foam cores in between. The composite sandwich structure skin is made by the glass fibre weave and the PVC foam for the composite sandwich structure core material. The E-glass fibres of skin materials were placed in a quasi-isotropic layout [0/90/45/-45/90/0]s. The resin for the composite sandwich structure is Priem 20LV with slow hardener. After curing the sample, the composite sandwich structure panel is cut into the required dimensions.

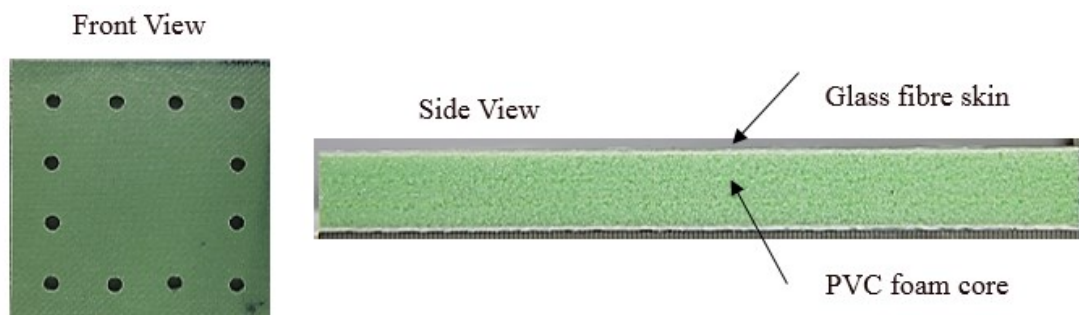


Figure 3-2. Front and side view of composite sandwich panel.

3.2. Manufacturing Process

3.2.1. Autoclave

The autoclave is used for manufacturing the prepreg composite material. In the production of composite aerospace and aircraft components, autoclave curing has traditionally been used to achieve the desired fibre content (resin-to-fibre ratio) and the absence of resin voids to produce light-weight and strong components. Autoclave curing achieves this by placing the part under vacuum in an autoclave and then pressurizing the autoclave during the heated cure cycle. The high pressure on the part (within the pressurized autoclave) helps to minimize resin voids and to achieve the desired resin/fibre ratio.

3.2.2. Resin Infusion under the Flexible Tooling (RIFT) Manufacturing Method for Composite Sandwich Material

The composite sandwich structures are made by the RIFT method shown in Figure 3-4. This method can help to make the GFRP composite skin and closed cell PVC foam cores cure together. The RIFT process used to manufacture the panels was developed to speed up production as well as to achieve a repeatable specimen quality. The process involved is as follows:

- 1) Cut the necessary glass fibre sheets for the manufacturing process.
- 2) Cut the foams to size.
- 3) Cut the flow media, peel ply and vacuum bagging to size.
- 4) Lay down the flow media and peel ply as the bottom layers on the glass base plate. Place the foam in the centre and place channels into position.
- 5) Line the perimeter with sealant tape.
- 6) Lay up the fibre mats and foam as required, taking care to remove any foreign objects.
- 7) Place the upper peel ply and flow media into position.
- 8) Make the omega channels and place them into position.
- 9) When satisfied with the layup, seal it with the vacuum bag. Ensure a tight seal using the sealant tape.
- 10) Place the setup into position in the oven. Insert inlet and outlet tubes into the channels. This requires breaking the seal, but sealant tape can be used to re-form it.
- 11) Test the vacuum of the setup. First, seal off the inlet temporarily. Once the vacuum pump has been turned on, the gauge should drop to the suction rate by the pump. Turning off the pump should result in a drop of less than 10% in 20 minutes.
- 12) When satisfied with the vacuum, the adhesive can be prepared. Estimate the adhesive required by calculating the volume of glass and tubing. Leave some excess for the adhesive layer between the glass, the foam and the volume to be taken by the flow media, peel ply and the channels. Between 30-50% excess is usually practical. Prime 20LV should be the epoxy infusion system used, with a slow hardener. The mixing ratio should be 100:26 resin to hardener, by weight. Try to use a large diameter mixing bowl to reduce the likelihood of an exothermic reaction.

- 13) Mix this for at least two minutes, ensuring it is thoroughly mixed around the edges.
- 14) Degas the mixture for approximately 15 minutes in an airtight vacuum chamber.
- 15) Start oven where the panel is located. It should be programmed to rise to 20 °C at a rate of 1 °C min⁻¹. It should remain at this temperature for 510 minutes.
- 16) To post cure the specimen, the oven should rise to 90 °C again at a rate of 1 °C min⁻¹. It should remain at this level for 4-6 hours. This controlled temperature rise is needed to ensure the polymer crosslinks are not destroyed.
- 17) Once this is complete, the oven should be left to cool at a natural rate. When cooled, the parts can be de-moulded.

Ideally, the temperature would be measured in one of the sandwich skins using a thermocouple, thus ensuring that an exothermic reaction is not occurring. However, it is difficult to acquire a large enough oven that would allow the thermostat control of the oven power. Test runs have shown that using the oven temperature control is sufficient, with a small possibility of an exothermic reaction occurring in the resin capture pot. Additionally, there may be problems with an incomplete infusion occurring in the glass fibre, especially for the upper skin. To counter this, it is recommended to slow the infusion process down. For this particular experiment, this involved taking two measures. The first was to clamp down the inlet from the resin pot to reduce the flow rate into the vacuum. Secondly, the upper flow media was purposely cut away so that it was shorter than the bottom flow media. This resulted in a plug forming in the system. To flow into the outlet, the adhesive must fill up remainder of the mat, thus forcing full impregnation of the fibre mats. This optimal setup is shown in Figure 3-3.

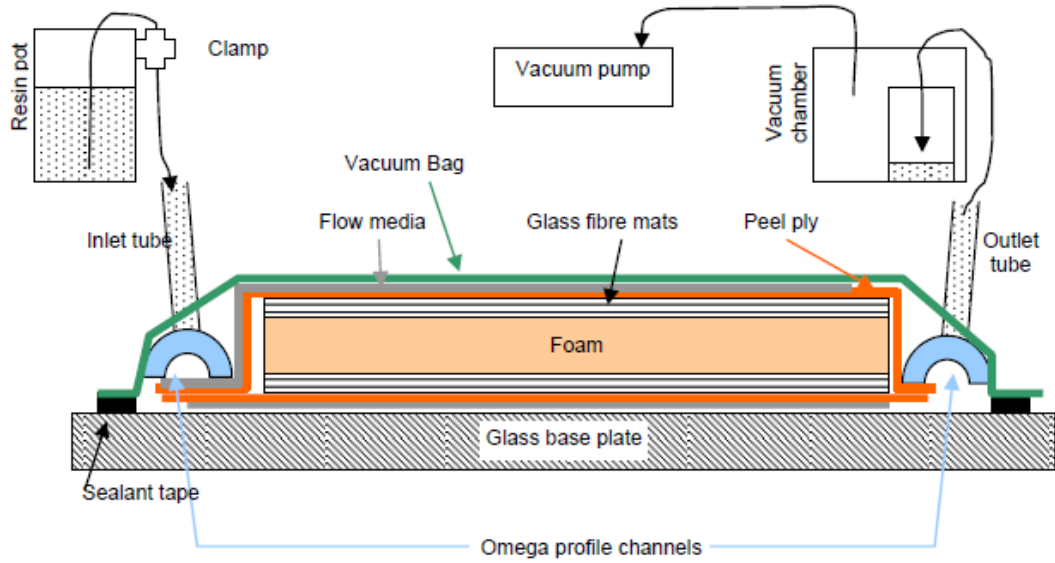


Figure 3-3. RIFT setup.

3.3. Iosipescu Shear Test

In this research, the Iosipescu shear test was chosen after comparing the different methods of the shear test in the literature review. This comparison was made because of the specimen thickness range of the Iosipescu test fixture and the ease of manufacturing the test specimen. The fixture is shown in Figure 3.6. It is the standard fixture from ASTM D 5379. The method allows us to test the maximum 20 mm thick specimen and a good stress concentration on the test area. In the Iosipescu shear test fixture, the left and right adjustable jaws can be tightened by thumbscrews to fix the specimen and to establish good alignment with the fixture. The specimen alignment pin can help the specimen in the centre of the fixture.

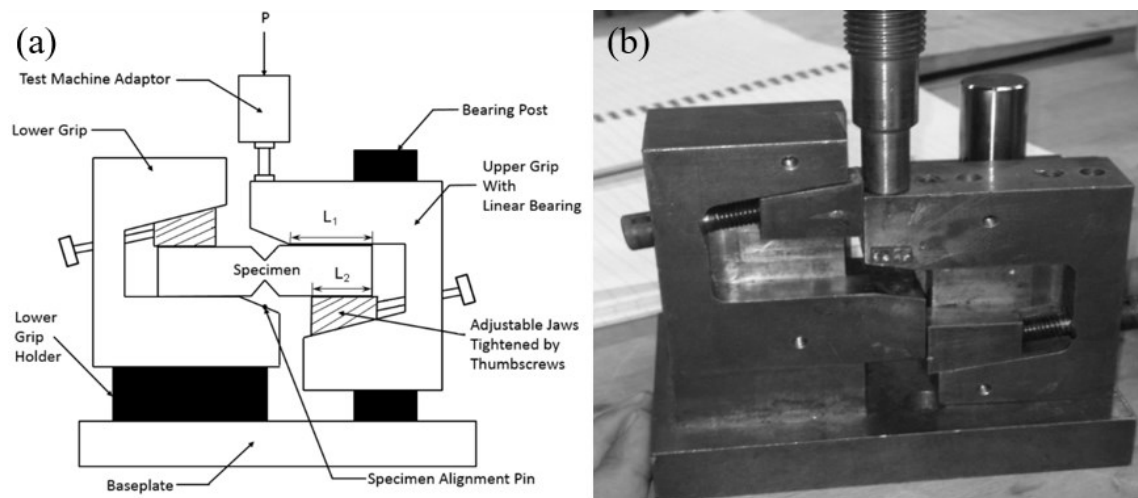


Figure 3-4. Schematic of Iosipescu shear test (a) and Iosipescu shear test fixture (b).

3.4. Impact Test

3.4.1. Low Velocity Impact

The low velocity impact is generated by the drop weight tower. As shown in Figure 3-5, the drop-weight tower generates the freefall weight. The drop weight tower can provide up to 300 joules in impact energy. The weight can be obtained by changing the number of the slices. Each slice is 1 kilogram, making it easy for those operating the process to add to the drop weight tower. The operation box can program the electromotor to lift the weight up to the height required. By triggering the button, the weight with the loading cell and the impactor at the end will hit the specimen on the base. The basement is solid and reinforced to provide the shaking and rebound of the impact force. The oscilloscope can receive the signal from the load cell, and the impact force can be obtained by using the sensitive parameter of the load cell, converting it from voltage to force. The transparent shield prevents those operating the process from being hit by the damaged specimen.

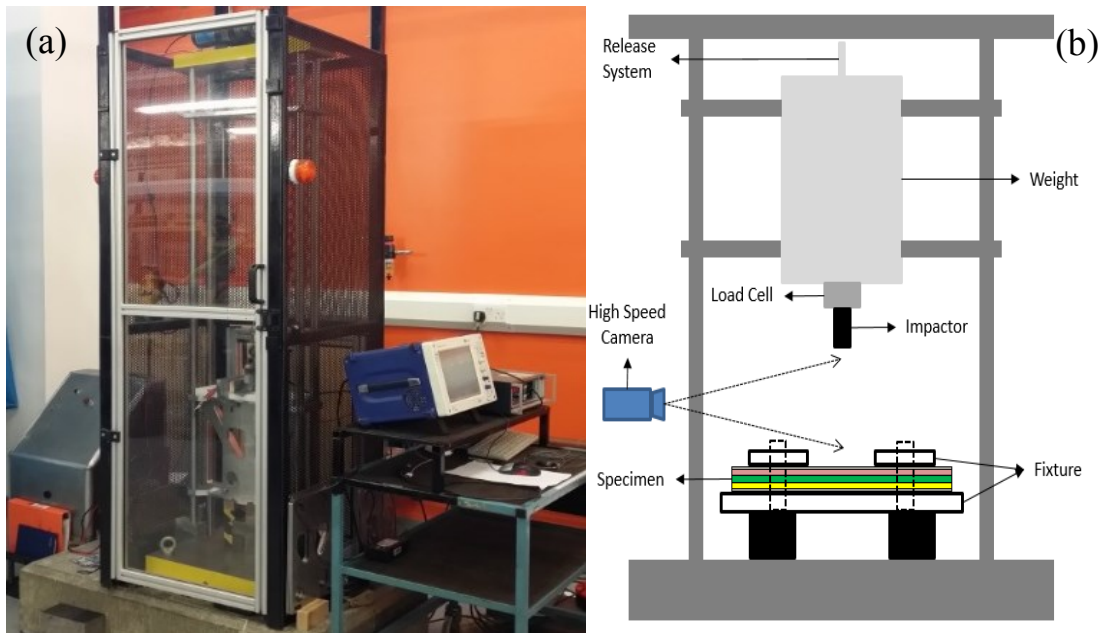


Figure 3-5. Drop weight tower (a) and schematic of drop weight tower (b) for low velocity impact test.

3.4.2. High Velocity Impact

The high velocity impact is generated by the gas gun facility funded by AVIC. This is shown in Figure 3-6. The gas gun facility is driven by compressed air, and the compressed air is held in the tank at the end of the barrel. By rapidly releasing the compressed air, the air pressure will push the projectile to the end of the barrel, and the projectile will go through the barrel for increasing the speed. There are two laser gates at the end of the barrel in order to measure the speed of the projectile. The oscilloscope will receive the two signals from both laser gates. The distance between the two laser gates is 50 mm, and the time gap between the two signals is the time for the projectile to pass the 50 mm length. The fixture will hold the specimen at the other end of the barrel, and the chamber protects the operator from being hit by the damaged specimen. Lastly, the reinforced back will stop the projectiles.

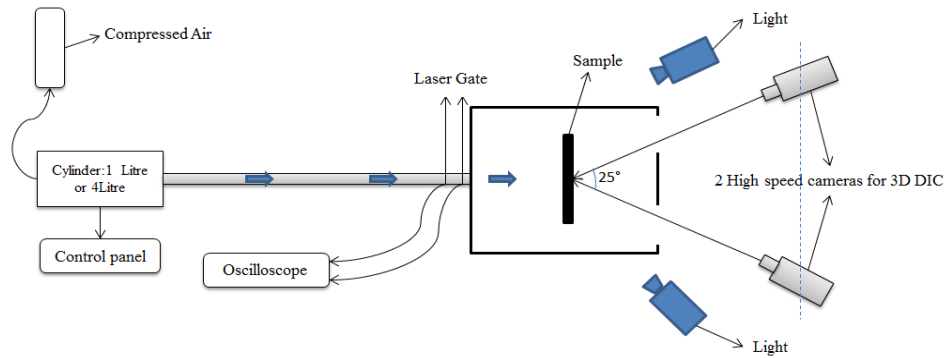


Figure 3-6. Schematic of gas gun facility.

3.5. Compliant and rigid impact

3.5.1. Compliant impact projectile

For the compliant impact experiments on the composite sandwich structures, high-density polyethylene (HDPE) was used for the compliant projectiles. The properties of HDPE are shown in Table 3-1, which was provided by the material manufacturer.

Table 3-1. Material properties of HDPE

	Density	Elasticity modulus	Tensile strength	Elongation at break	Indentation hardness	Shore hardness
Unit	g cm^{-3}	N mm^{-2}	N mm^{-2}	%	N mm^{-2}	
Test method	ISO 1183	ISO 527	ISO 527	ISO R 527	ISO 2039	ISO 868
Value	0.954	850	22	300	36 – 46	61

The soft edge of the HDPE projectile can disperse the impact load on the composite sandwich structures, and the large strength to density ratio can enable the light weight and the high strength to have a higher velocity and impact energy levels. From Figure 3-7, we can see the HDPE projectile before and after the impact. The projectile has a deformed edge and damage on the reverse side because the low elasticity modulus and hit the barrel.

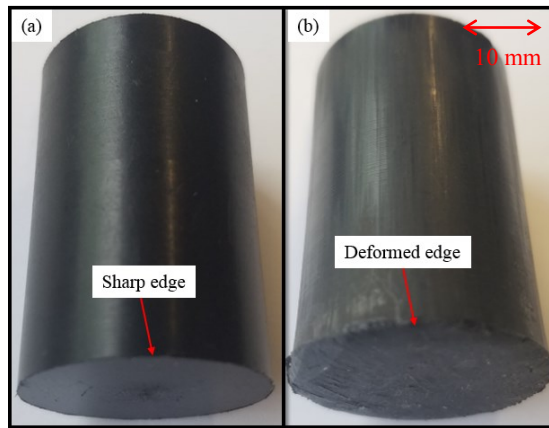


Figure 3-7. Compliant projectile before impact (a) & after impact (b).

3.5.2. Rigid impact projectile

In order to understand the effect of the sharp edge impact on the composite sandwich structures, the Aluminium 6 series was chosen for the rigid impact projectiles. This series was selected because the low density of aluminium can help to reach the same weight as the compliant HDPE projectiles. The density of Al is higher than HDPE. However, by drilling a hole inside the projectile, the total mass of the Al projectile becomes the same as the HDPE projectile (shown in Figure 3-8 (a)). Figure 3-8 (b) & (c) show the shape before and after the impact. The edge of the rigid projectile did not deform too much as for the HDPE projectile.

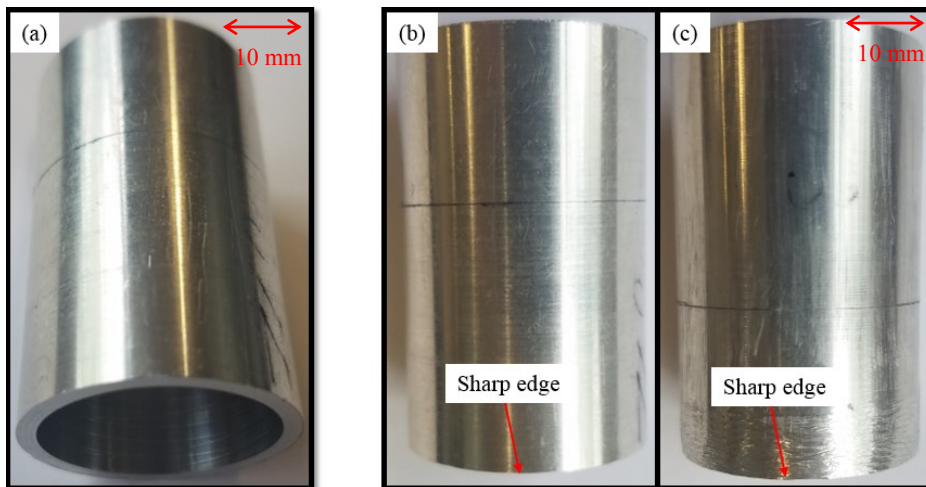


Figure 3-8. Rigid projectile: (a) inner shape; (b) before impact and (c) after impact.

3.5.3. Typical damage for compliant and rigid impact on composite sandwich structure

The typical damage for both compliant and rigid impact under same velocity were shown in Figure 3-9. It shows that the rigid impact has a sharp edge cutting on the composite fibres and the large delamination around the cutting area for the front skin, whilst the compliant impact on the composite sandwich structure produced less fibre break on the front skin. Moreover, the damage on the back skin for the rigid impact was peel off the composite skin, whilst the compliant impact produced the delamination at the edge of the boundary condition. This phenomenon will be explained in the impact experiment chapter.

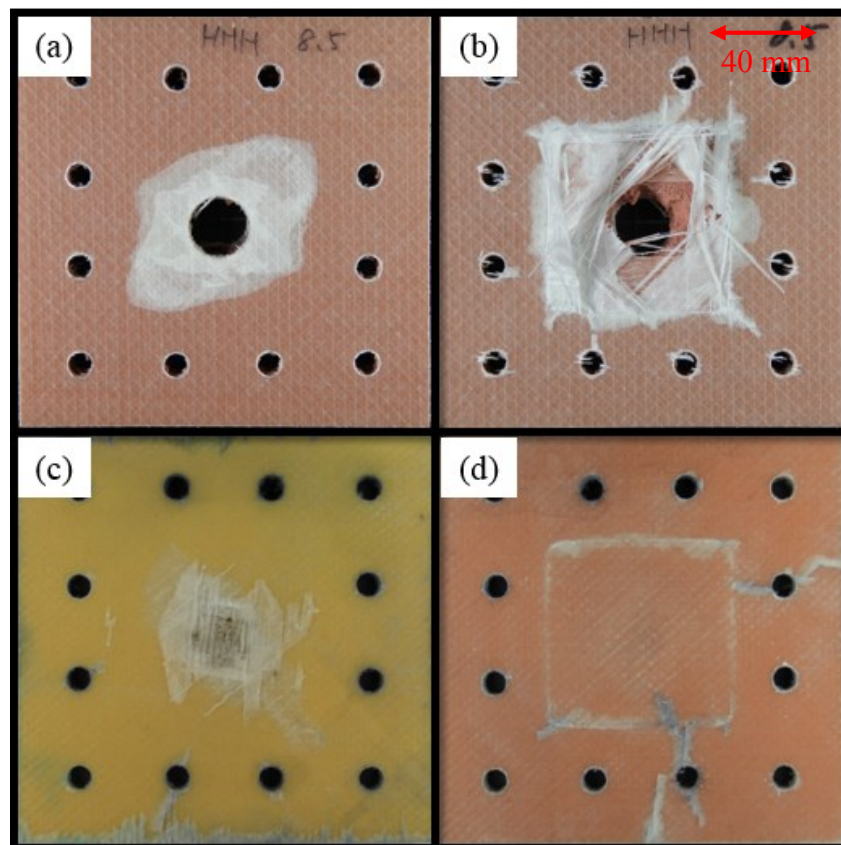


Figure 3-9. Rigid impact on composite sandwich structure (a) & (b): (a), the front skin view on the impact side for composite sandwich structure under the rigid impact; (b), the back skin view for the composite sandwich structure under the rigid impact. Compliant impact on composite sandwich structure (c) & (d): (c), the front skin view on the impact side for composite sandwich structure under the compliant impact; (d) the back skin view on the impact side for composite sandwich structure under the compliant impact.

3.6. Instrumentations

3.6.1. Digital Image Correlation (DIC)

In many engineering and scientific fields, the use of non-destructive testing with precision optical components has become increasingly popular. The reason for doing this is to consider the wing structure, which is a large and expensive part when it is easy to understand. Instead of estimating the invasion by way of lifetime damage to the integrity of its structure, it makes more sense to scan the area rather than gaining point information.

One of the optical methods is known as digital image correlation (DIC), also known as white light spot technology. The DIC can be used to generate a full-field surface displacement and compare the strain graphs by comparing a series of distorted images. Tracking the characteristics of the specimen, a random coating pattern is usually applied to the surface of the sample. The technique can be applied to whether or not the unused paint pattern has significant features on the opposite surface. However, it has been considered more reliable to use a speckle pattern [163]. The working principle of this process is to use a digital camera, which is regarded as a baseline capture initial image for comparison of all subsequent images. These subsequent images will be displayed with some variations of the deformation [164] of the results of the surface coating pattern. In order to achieve higher accuracy, the correlation is based on pixels, called facets, instead of using individual pixels to track the quadrilaterals. These aspects have a pattern in the array of corresponding grey values, just like a bar code, which allows the tracking of the image through subsequent stages [165]. This process is illustrated in Figure 3-10.

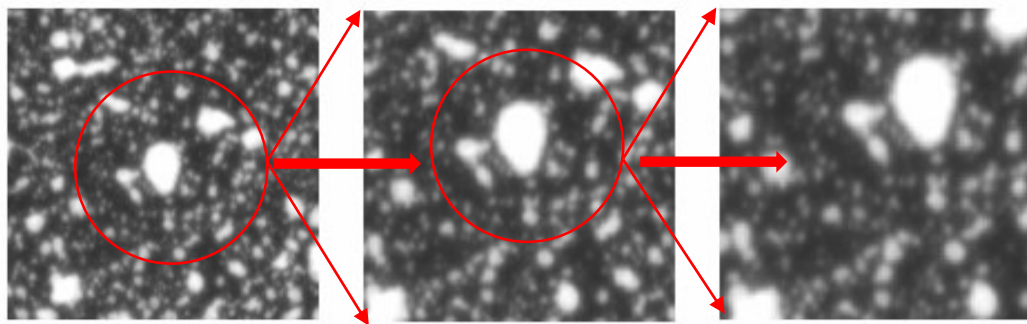


Figure 3-10. Example of DIC method.

This is a common overlap surface to increase the associated accuracy and reduce the risk of losing data in the evaluation. The size of the facets and the level of overlap play an important role in determining correlation. For example, strains can be measured in the range between 0.1% and under ideal conditions [163] with an accuracy level of more than 100% at 0.05% (500 micro strain). However, the resolution is related to the available facet size, which in turn will determine the level of strain that can be obtained. For example, if there is a function in the sample, selecting a mask that results in a low strain resolution and a facet size associated with the feature that may not be a discernable strain pattern. However, the use of larger facets allows more data to be analysed and the accuracy of the measured displacement vector associated with the facet increases.

The result of the processing required by the device is a digital camera with a suitable lens, a tripod (so that the image plane is constant), illumination to illuminate the sample, and a computer to load the processing software. In addition to the software, the other necessary equipment is standard goods readily available. There are many software packages available on the market, including ARAMIS by GOM, [166] Vic-2D / 3D by related solutions, [167] and Strain Master by LAVision [168].

Into more detail about the general method, it is a specialized facet record where the light intensity is tracked, where each facet should have a unique array of grey values associated with it (8 bits) The pixel random paint pattern is captured as a value assigned to a grey scale [169]. For such accurate work, the light intensity between the image phases must not change, which is unlikely to satisfy the condition that the change in the surface reflectance can easily be induced in consideration of the slight displacement. Therefore, this is the wrong common source. So, why use a face instead of a single pixel is obvious. In order to track facets between images, correlation is performed, typically using an array of light intensities as matching parameters. Briefly, the idea is to compare the selected facets on the initial image of a group of pixels in a subsequent image phase. The difference in intensity between the two arrays is then calculated and the relevant parameters obtained from this calculation. This process is repeated for a number of different groups of pixels on subsequent images, and the matching set is selected as one, where the correlation is the best [158]. An operator may typically set acceptable correlation parameters.

Once the subsequent image is mapped to a starting surface, the displacement can be machined for each facet. This is simply the new distance (in pixels) minus the original distance, calculated in all working directions. For example, this would be 2D in both directions, and three-dimensional calculations in three directions. Strain can be calculated as the differentiation of this array, and is called the engineering strain tensor. The calculation of the strain tensor is formulated along the image array point and can allow for variations along the image strain due to some noticeable features. There are several ways to do this, including a linear gradient method, a second-order gradient method, or even a spline-based method [164]. In general, the corresponding strain tensor for each point is estimated from the displacement map, and a new displacement field is generated. To further improve the accuracy, the deformation map uses the estimated strain, called a synthetic map making. This is then compared to the actual image and the vector difference between the two is calculated. Again, the strain tensor is calculated such that the difference is reduced via the statistical correlation parameter [169].

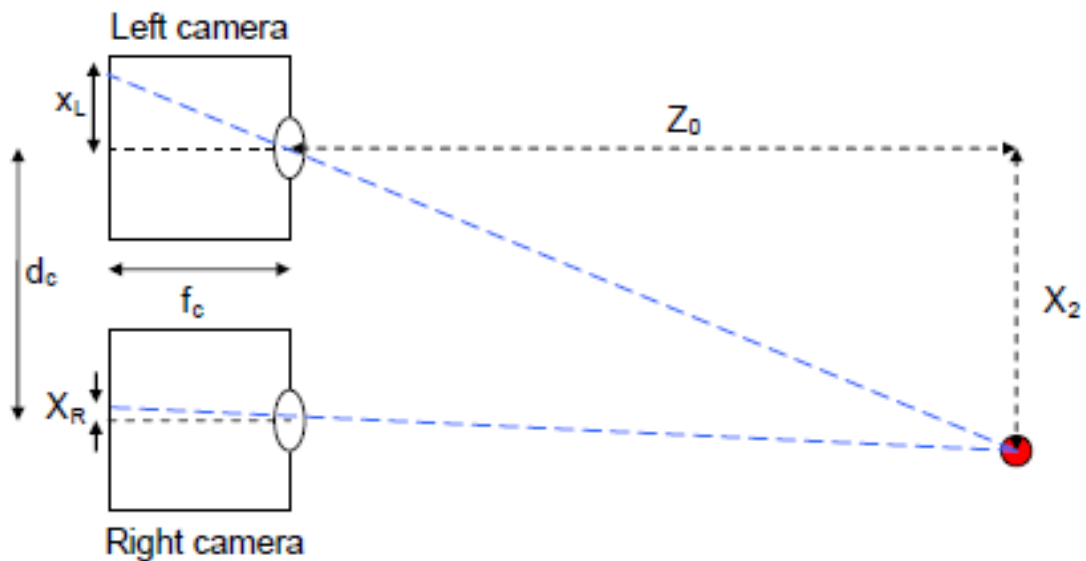


Figure 3-11. Trigonometric principle of stereoscopic imaging.

Using a 3D DIC is considered more accurate because it can track any deformation beyond the plane. The 2D system will erroneously record this motion as an in-plane deformation. The greatest benefit of using 3D DICs is the ability to measure deformations of non-planar surfaces, such as tubes. The determination of the deformation in the outer plane is

based on a simple trigonometric principle, as shown in Figure 3-11. Using similar triangles, the out-of-plane displacement can be determined using Equation (3-1). The greatest error of this type of analysis arises when the distance Z_0 is very great, leading to very small variations in x_L and x_R . Additionally, an accurate calibration is required to determine the values of d and the angle between the cameras. Without an accurate calibration, the values of x_L and x_R would be estimated incorrectly. Hence, there may be some situations where the use of a single camera for 2D work is a better choice.

$$Z_0 = \frac{d * f}{x_L - x_R} \quad (3-1)$$

Where 1) Z is the out of plane distance; 2) d is the distance between camera centreline; 3) f is the focal length; and 4) x_L and x_R are the distances between the image centreline to the point on the CCD where the light converges for the left and right cameras, respectively.

3.6.2. GOM Camera

Provided to the author is a DIC system, called ARAMIS, provided by GOM [166]. The software version used is 5.4.1. What is more important is the hardware version, which is ARAMIS 1.3m which uses 1.3 megapixel images (1280 x 1024 pixels) and is captured by up to two cameras at a maximum frequency of 1 Hz. The quoted strain range for the strain measurement is 0.05% to 0.02%, plus the quoted strain accuracy. The lens used with the system is 135 mm, 50 mm macro and one 12 mm, all of which are telecentric. For 3D DIC, two cameras are required to capture the stereo image. For this purpose, a pair of lenses 50mm and 12mm are available.

The imaging unit is a sensor capable of recording an 8-bit gray scale CCD chip in an image. When in use, it is directly connected to the PC and via software, which allows full control of either manual or automatic control (via a timer). The equipment required to link the load and trip data to the captured image is not available. Post-experiment links are required, although this does not have much difficulty in the speed of the tests used to complete. Overall, the researchers were satisfied with the system, but the low image resolution required for any of the smaller capture regions or the loss of detail. To reduce this effect, it is desirable to use a higher resolution SLR camera that can be made, although this is a nonstandard solution.

3.6.3. High Speed Camera

In order to get the details of the projectiles and specimen under low and high velocity impact, the high-speed camera was used in both experiments. The speed range from 10,000 to 50,000 fps depends on the resolutions required in the experiment.

3.6.4. Speckle Application

As mentioned earlier, the DIC technique works well when there is a random pattern applied with high contrast. For this study, a white pattern was applied to the sprayed surface using a black background paint. In order to effectively utilize different samples, it is necessary to adjust the spots of the application. For example, a pattern that is required to capture a smaller physical area is required to ensure a finer detail of the image. However, there is a risk that if a larger area is captured the fine pattern will not be discernable.

Develop a different pattern of roughness using different spray techniques. Sprayed with acetone mist, which is manually controlled by the application of the nozzle to get the coarsest pattern. This produces an average diameter spot of 0.6 ± 0.1 mm and is called "rough". The second technique utilizes relatively inexpensive spray gun technology with water-soluble matte paints. The paint is in a chamber and is forced through a small hole using a compressed air tank. The resulting spots had a mean diameter of 0.46 ± 0.02 mm, and were called "sperm" because it reduced the variability. A finer gun is also used to produce an average diameter of 0.16 ± 0.01 mm speckle spots, and is called "ultrafine".

4. Thick laminates composite shear test

4.1. Introduction

In this chapter, the Iosipescu shear test was performed for different V-notch depth and different specimen thickness. The objective is to get an accurate result for the out-of-plane shear property of thick sectioned composite. As introduced in the literature review, some of the shear test methods are not suitable for the thick laminate composite material because of the fixture limitation and stress concentration problem when the specimen is too thick. The ASTM standard D5379 [44] is one of the most suitable method for the thick section composite out of plane shear test, but the ASTM standard D 5379 did not illustrate the best sample geometry for the Iosipescu shear test. The specimen geometry can affect the accuracy of the result, so that verification of the test specimen geometry is the first topic before using the experimental results to compare with the FE Model results. By introducing the DIC system during the experiment process, the full field strain map on the specimen, strain distribution and the delamination failure on the specimen can be determined for comparison.

4.2. Experiment

4.2.1. Material and experiment arrangement

The material used in this experiment is UD prepreg CFRP as introduced in Chapter 3. The material for each specimen with 160 plies, the thickness d_l is 20 mm. Table 4-1 gives the mechanical properties of the composite laminates evaluated in this thesis as quoted by the prepreg manufacturer AVIC.

Table 4-1. Mechanical property of uni-axial laminates.

E_1 (MPa)	E_2 (MPa)	E_3 (MPa)	ν_{12}	ν_{13}	ν_{23}	G_{12} (MPa)	G_{13} (MPa)	G_{23} (MPa)
138000	9040	9040	0.307	0.307	0.35	4500	4500	3476
X_T (MPa)	X_C (MPa)	Y_T (MPa)	Y_C (MPa)	S_L (MPa)	S_T (MPa)	n	G_{12}^0 (MPa)	G_{13}^0 (MPa)
1696	1188	71.4	202	102	90	1.3	4500	4500

Six variants of Iosipescu shear specimens (see Figure 4-1) with different fibre orientation (G13 shear test and G23 shear test) and different out of plane thickness or specimen height, h , were designed according to the ASTM D5379 standard [44]. The cutting directions for the G13 and G23 shear test specimen are shown in Figure 4-1 (a) and the Iosipescu test fixture is shown in Figure 4-1 (b). For the G13 shear test specimen (see Figure 4-1 (c)), the fibres are along the longitudinal direction of the specimen, while for the G23 shear test, the fibres are along the transverse direction of the specimen (see Figure 4-1 (d)).

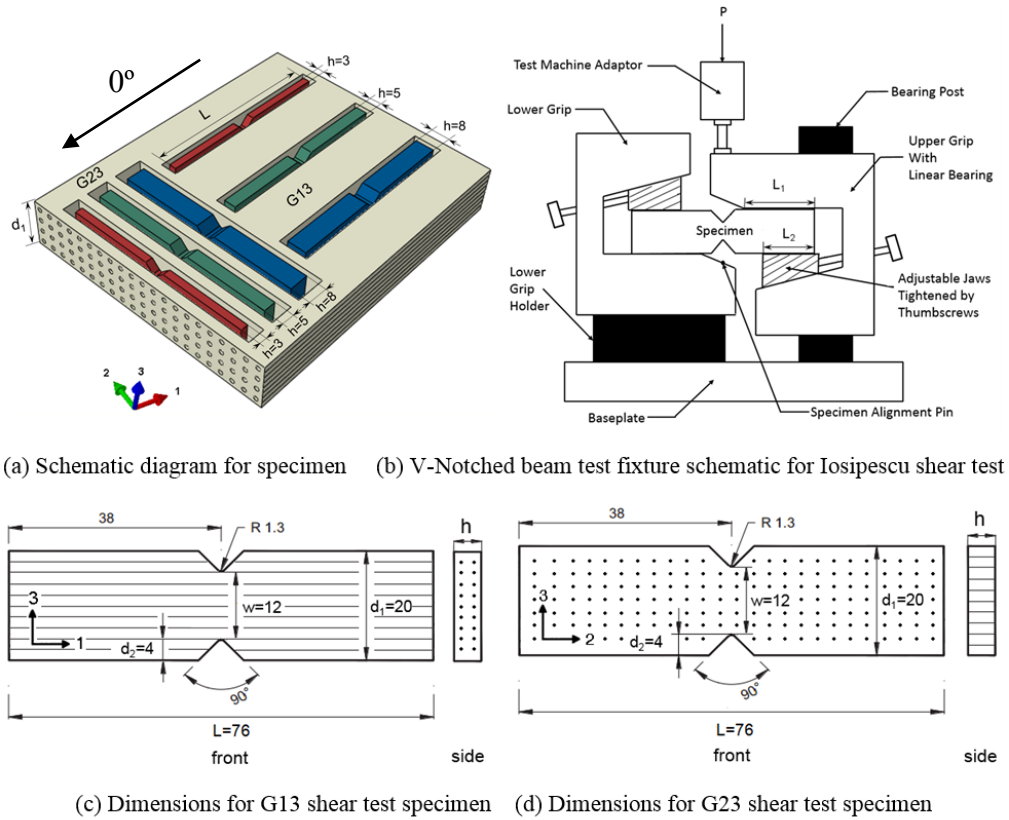


Figure 4-1. Specimen dimensions and test fixture schematic for Iosipescu shear test (dimensions in mm).

The Iosipescu shear test achieves a state of pure shear loading at specimen midlength by application of two counteracting moments produced by two force couples, as illustrated in Figure 4-2 Force Diagram. For a total force P , as measured by the external testing machine, the applied forces are, as shown, calculated from equilibrium requirements where a is the distance between forces of the innermost force couple. A state of constant

shear loading, as shown in the shear diagram of Figure Shear Diagram, is induced through the middle section of the test specimen, equal in magnitude to the total force P . The moment diagram of Figure Moment Diagram shows that induced moments exactly cancel at the mid length of the specimen, producing a pure-shear loading state.

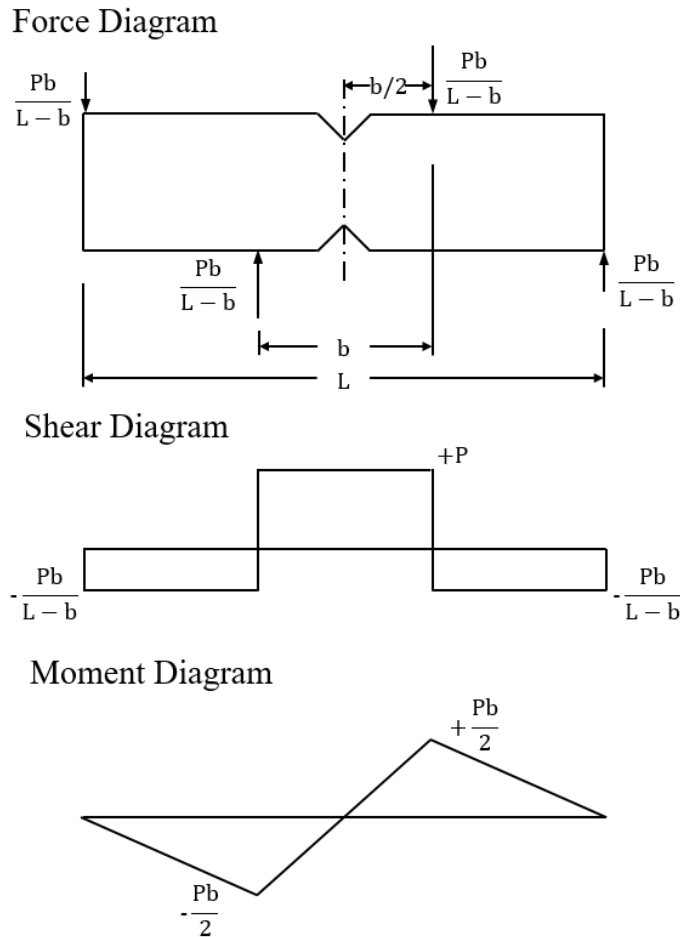


Figure 4-2. Force and moment equilibrium models of the Iosipescu specimen – one dimensional model.

It was originally assumed that the asymmetric loading of a beam, as depicted in Figure 4-2, results in a constant shear force between the inner load introduction points and a zero bending moment at the middle of the beam. It was assumed that the asymmetric load introduction arrangement would produce a state of uniform and pure shear between the notches so the shear stress (τ) could be obtained by dividing the shear force (P) by the specimen cross-sectional area (A) at the notch. At $x = 0$ in Figure 4-2 $\sigma_x = \sigma_y = 0$ and $\tau_{xy} =$

P/A. However, equilibrium requires that the resultant axial force (N) and bending moment (M) are zero, so that

$$N = 0 = \int \sigma_x dA; M = 0 = \int \sigma_x y dA \quad (4-1)$$

The resultant of the shear stress is the shear force (P)

$$P = \int \tau_{xy} dA \quad (4-2)$$

And the normal stress (σ_y) is related to the shear stress by

$$\sigma_y = -\frac{1}{y} \int \frac{\partial \tau_{xy}}{\partial x} dA \quad (4-3)$$

where t is the specimen thickness. Thus, at any given point along the line connecting the notch roots ($x = 0$), the individual normal stress components (σ_x , and σ_y) need not be zero, and the shear stress may be greater or less than the average value P/A.

4.2.2. Instrumentation

An Instron 5800 series screw-driven machine was used to load the thick laminate CFRP specimens in the Iosipescu test. The test specimen was connected through two clamps connected to the actuator and 10 kN load cell on the test machine. The test machine was driven by the screw rack, which imposes certain limits on the speed of 0.07 m s^{-1} specified by the mechanical design.

As shown in Figure 4-3 (a), the $+45^\circ$ and -45° strain gauges were employed on the shear test for the strain measurement. The maximum shear strain occurs on a line in the region between the roots of the notches. Ideally, this shear strain could be measured by monitoring the relative displacement of the two fixture halves and calculating the average shear strain. However, due to compressive deformations at the four loading points, a fixture displacement measurement is not suitable for calculating shear strains in the Iosipescu shear-test method. Strain gages oriented at $\pm 45^\circ$ to the longitudinal axis of the specimen, and centred between the notches, can also be used to measure the shear strains.

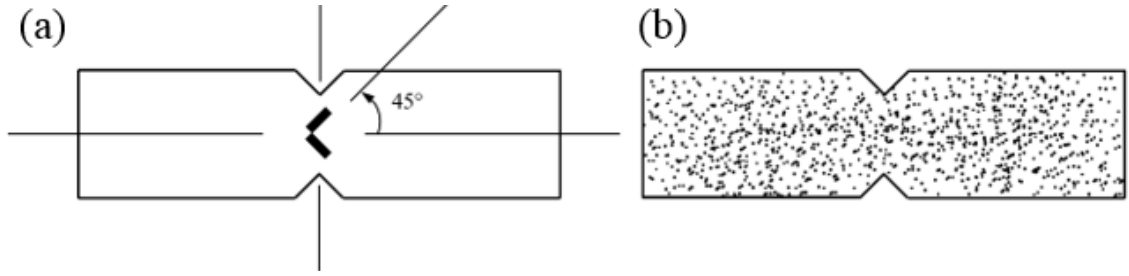


Figure 4-3. (a) the $\pm 45^\circ$ strain gauges on the specimen, (b) DIC speckle on the specimen.

The measurement of shear modulus, rather than strength, required determining the shear strain corresponding to the average shear stress across the test section, for a given applied load. Shear strains are usually measured with a strain gage rosette which averages the strains over a finite area in the test section. If two gages are arranged at $\pm 45^\circ$ to the longitudinal axis of the specimen, as shown in Figure 4-3 (a), the shear strain γ_{xy} , determined from the strain gages SG is:

$$\gamma_{xy}^{SG} = \epsilon_{+45} - \epsilon_{-45} \quad (4-4)$$

The strain in SG1 (ϵ_{+45}) is compressive, and that in SG2 (ϵ_{-45}) is tensile, so the shear strain is negative. The shear modulus G_{xy} is then calculated by dividing the average shear stress ($\tau_{avg} = P/A$) on the specimen cross-section by the shear strain from the strain gages.

$$G_{xy}^{SG} = \frac{\tau_{avg}}{\gamma_{xy}^{SG}} \quad (4-5)$$

Another method to get the strain on the specimen is the DIC system which was introduced in chapter. The speckle covered the whole specimen (Figure 4-3 (b)) to get the full field strain during the shear experiment.

The GOM camera setup with two cameras and computer (Figure 4-4). In Figure 4-4 (a) the two GOM cameras were positioned in front of the test fixture by the laser point in the centre of the specimen. The two LED light provide the uniform light on the specimen to get the perfect condition for each picture. In Figure 4-4 (b), the calibration process should

be done before the real test with the standard panel to keep the right angle and distance between the two cameras. Figure 4-4 (c) is the data processing with the GOM software.

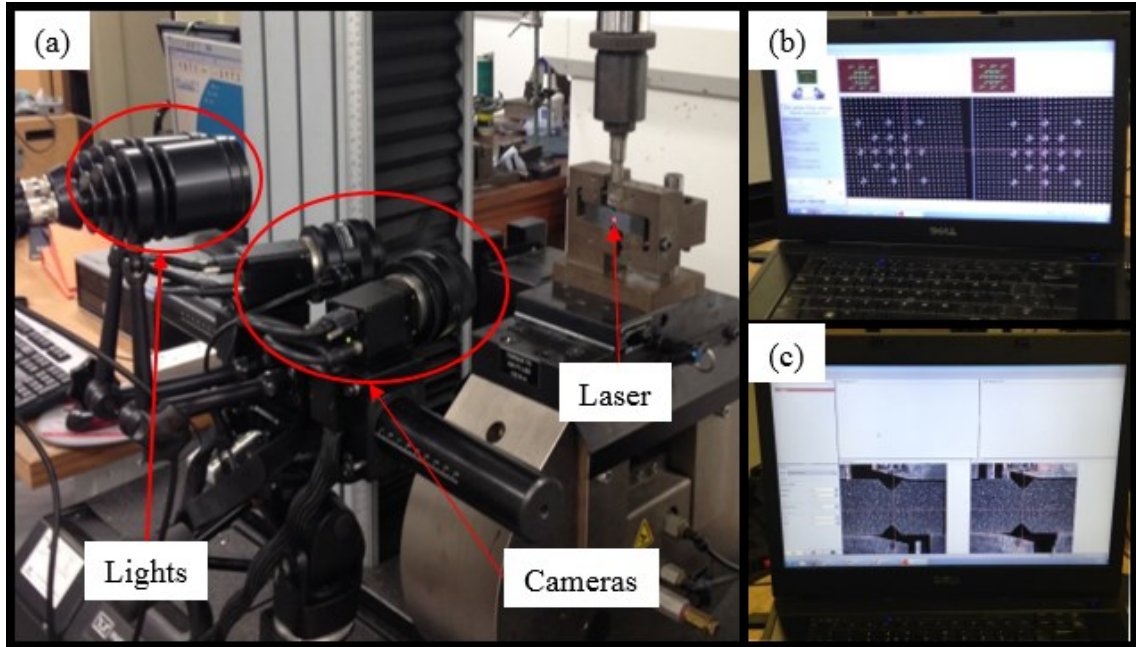


Figure 4-4. (a): The DIC set up for Iosipescu shear test; (b) the calibration of the DIC and (c) the data processing for the DIC.

4.3. Results and discussion

4.3.1. Validation of three dimensional DIC by strain gauges

Before the DIC results can be trusted for the strain measurement, the validation was made with both standard strain gauges and DIC method. The $\pm 45^\circ$ strain gauges were placed at the centre between the notches at the back face of test specimen, whilst the DIC system can get the strain at the same position on the front face of the specimen. So the two strain values can be compared. It can not only validate the accuracy of the strain on the specimen, but also help to confirm the specimen did not twist during the shear test.

The validations were made for both G13 (fibre along the specimen) property and G23 (fibre out of the specimen) property which shown in Figure 4-1 (c) & (d). The specimen geometries for both tests are the same with $w=12$ mm and $h=8$ mm, and the loading speed are the same with 0.1 mm min^{-1} for both shear test.

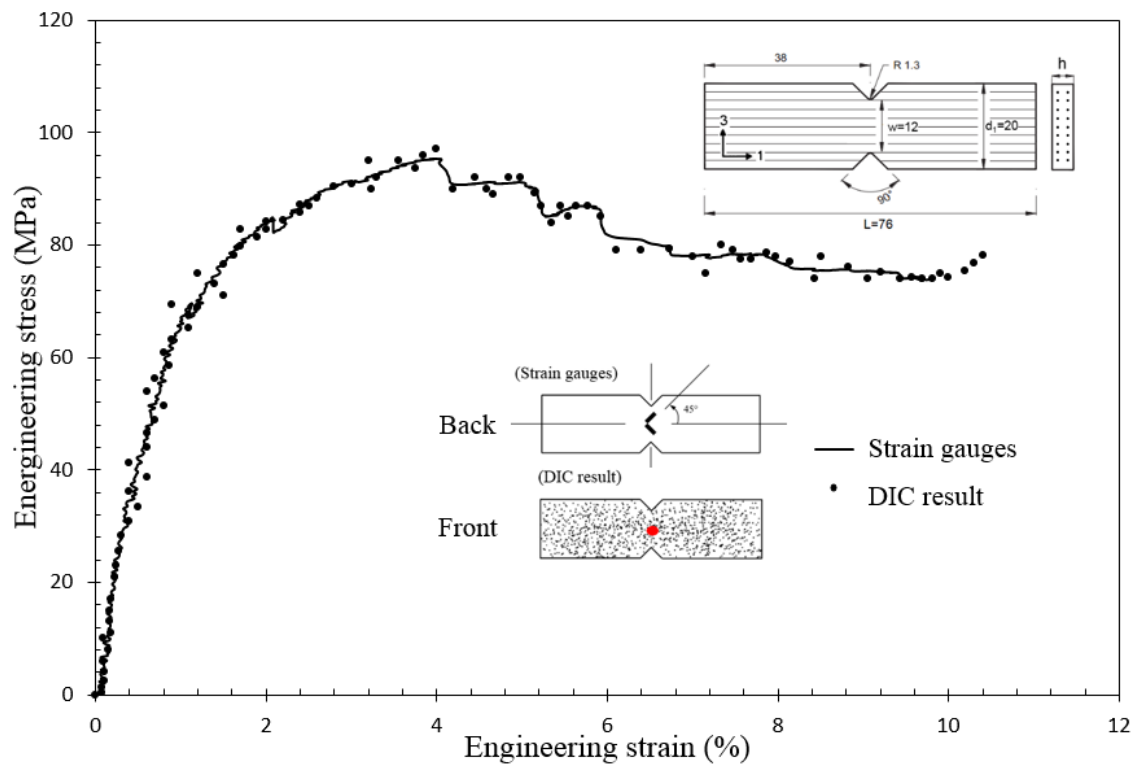


Figure 4-5. The engineering stress versus engineering strain of G13 shear property by both strain gauges and DIC method.

Figure 4-5 shows the engineering stress versus engineering strain for G13 property. the engineering strain can be got from the strain gauges and DIC. Furthermore, the value of strain from the strain gauges and DIC is the same. However, both of the methods can have the non-linear property which is the material characteristic of thick section CFRP G13 shear test.

Both the DIC method and strain gauges can also get the delamination between composite layers as the engineering stress dropped and vibrated after the peak value of engineering stress. The delamination between layers of the G13 shear test is one of the most important phenomenon to be compared with FE Model. So the DIC method can be used for the full field strain measurement on G13 shear test.

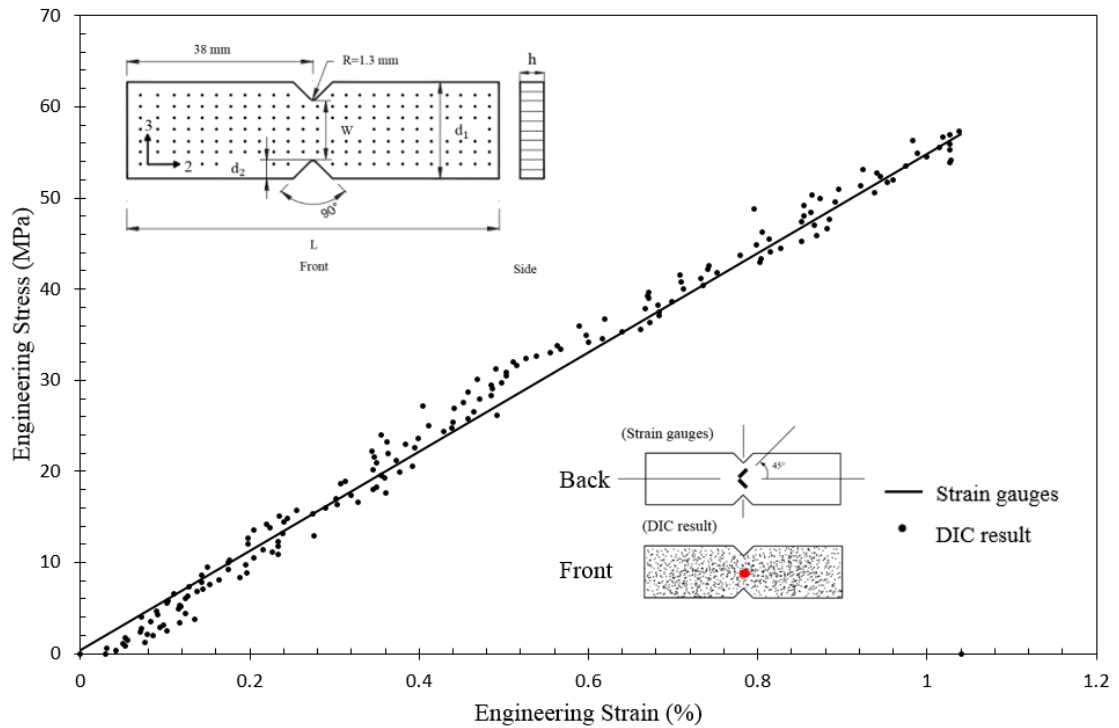


Figure 4-6. The engineering stress versus engineering strain of G23 shear property by both strain gauges and DIC method.

Figure 4-6 shows the engineering stress versus engineering strain for G23 shear property of thick sectioned composite. As the same as G13 shear test, both strain gauges and DIC results show the same strain level during the shear test until the final failure of the G23 shear test.

For both G13 and G 23 shear test. It can be found that the GOM system DIC technique can reflect the time based strain value of the centre point on the specimen, this gives the possibility of using DIC for all the shear test which would save lots of time on soldering strain gauges on the specimen.

4.3.2. Out of plane shear strength G13 & G23

The typical engineering strain versus engineering stress trace for G13 shear property is shown below (Figure 4-7). As shown in the DIC result at stage (B), the stress concentration started from the top and the bottom notches when the stress increase. And the trace begins to act as the non-linear property until stage (C), the two delamination start from top and bottom notches where the two red line appear on the top and bottom notch. After the stage (C), the engineering stress did not increase whilst the engineering strain at the centre point increase, as shown in stage (D), there are more delamination appear between the notch area. Moreover, when the engineering strain increased to stage (E), the engineering stress start to drop because the delamination extends longer to cover the test area.

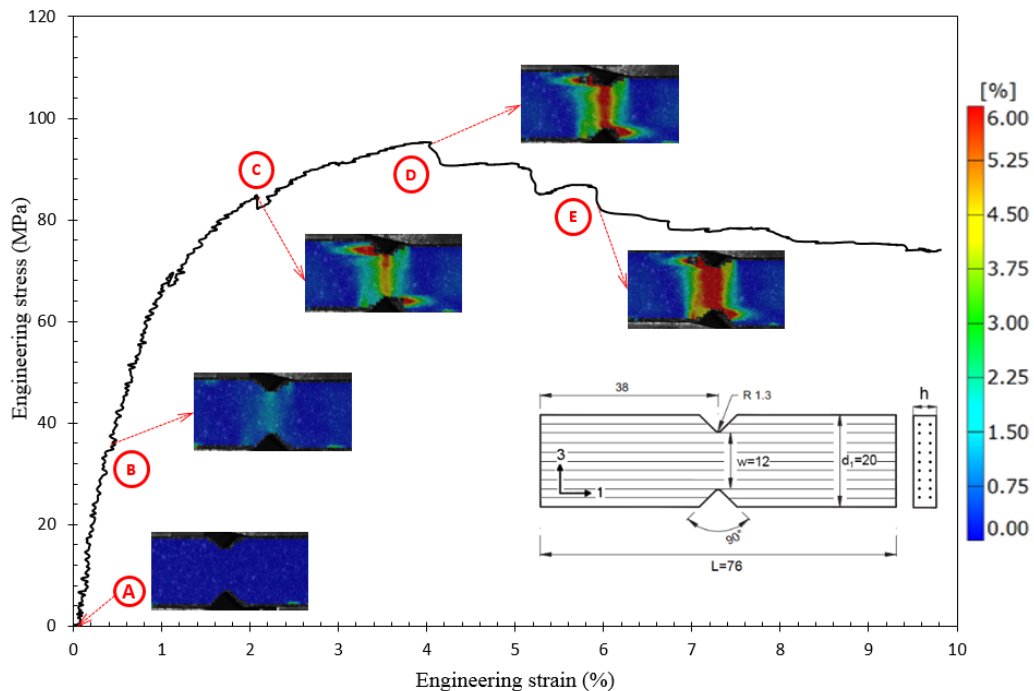


Figure 4-7. The engineering strain versus engineering stress for G13 shear experiment.

For the G23 shear test, the trace of engineering stress versus engineering strain performs the linear feature. As shown in stage ② the stress concentration starts from the test fixture area. When the engineering stress and engineering strain increase, the stress concentrated in the middle of the notch area as shown in stage ③. Before the final failure of the G23 shear test at stage ④, the stress concentration area between notches became bigger and cover the notch area. However, the final failure of the G23 shear test was shown in stage ⑤, and the final failure of the specimen is as the same shape as shown in the DIC result.

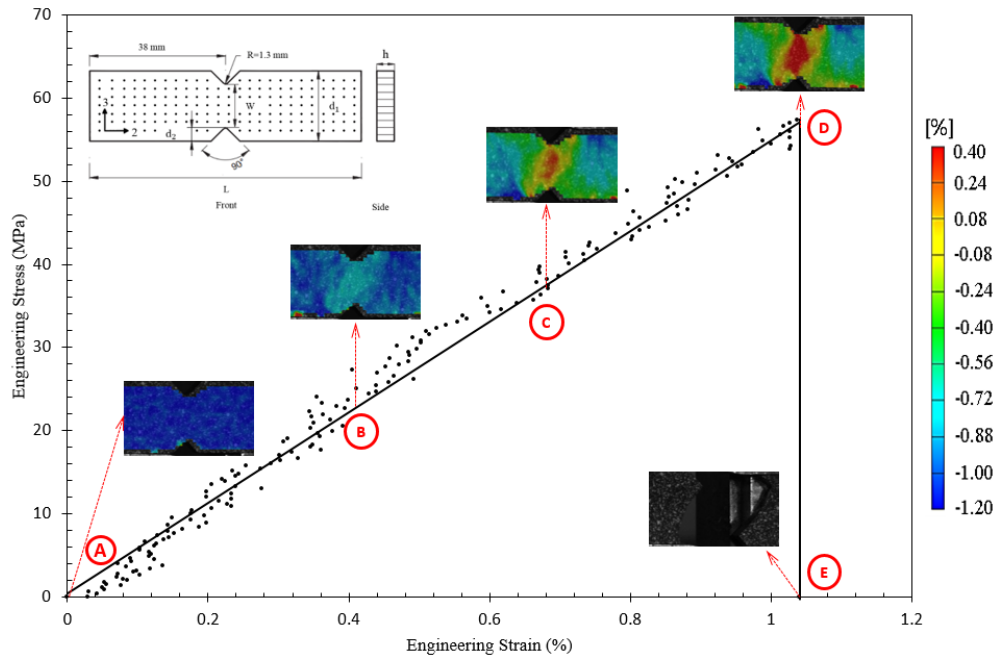


Figure 4-8. The engineering strain versus engineering stress for G23 shear experiment.

By using the DIC method, we can have a visualized full field strain map on the specimen, this can help to have a better understanding of the Iosipescu shear test at each stage. This can also help to have a good comparison between experiments and simulation as we can know how the damage initiate, how the delamination increase and how the specimen perform before the final failure. It not only gave us the values to compare, but also the reaction of the whole specimen.

4.3.3. Effect of V-notch depth

The effect of different V-notch depth was compared in this part. The V-notch depth would affect the damage on the specimen for G13 shear test. However, for the G23 shear test, the specimen of different V-notch depth ($W = 8$ mm, 10 mm and 12 mm) was very hard to manufacture especially for the $h = 3$ mm specimen, because of the fibres are along the transverse direction of the specimen, and it is easy to damage the sample by water jet cut. And the results for G23 with $W = 8$ mm and 10 mm were not repeatable, so the V-notch depth of G23 shear test was chosen as 12 mm. However, for G13 shear test, the V-notch is still worth to be investigated.

4.3.3.1. G13 shear property

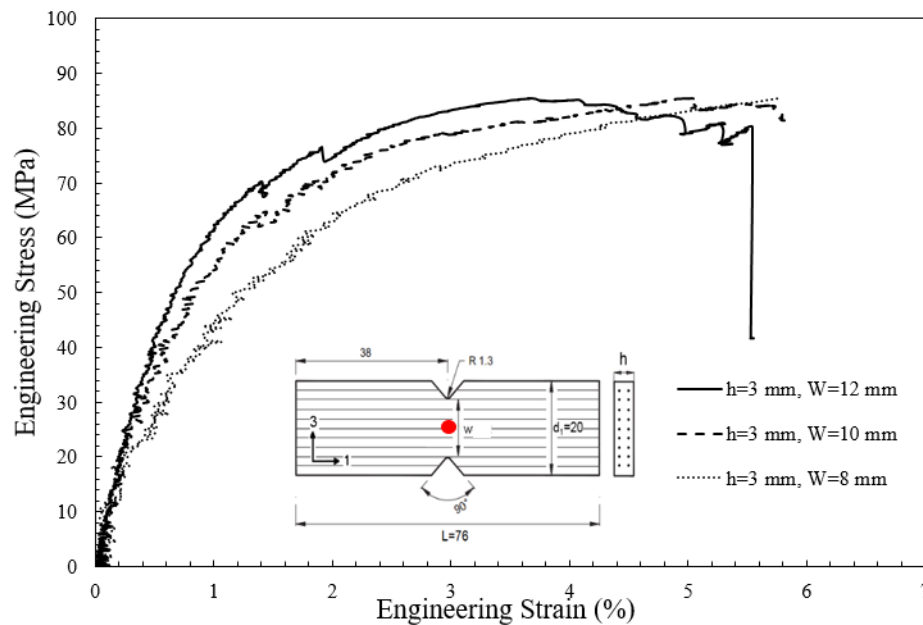


Figure 4-9. The load versus displacement trace for G13 shear test with the same thickness ($h = 3$ mm) but different V-notch depth ($W = 8, 10, 12$ mm).

In Figure 4-9, the engineering strain versus engineering stress of the centre point on the G13 specimen was compared. As shown in Figure 4-9, the maximum engineering stress for three cases which $W = 8, 10$ and 12 mm were nearly the same with 82 MPa. However, the G13 specimen with $W=12$ mm provided the stiffer strength than the other two depth of V-notch.

4.3.4. Effect of specimen thickness

The specimen thickness, h , is an important parameter for the shear strength analysis. The strain distribution across the V-notch was compared for different specimen thickness.

4.3.4.1. G13 shear property

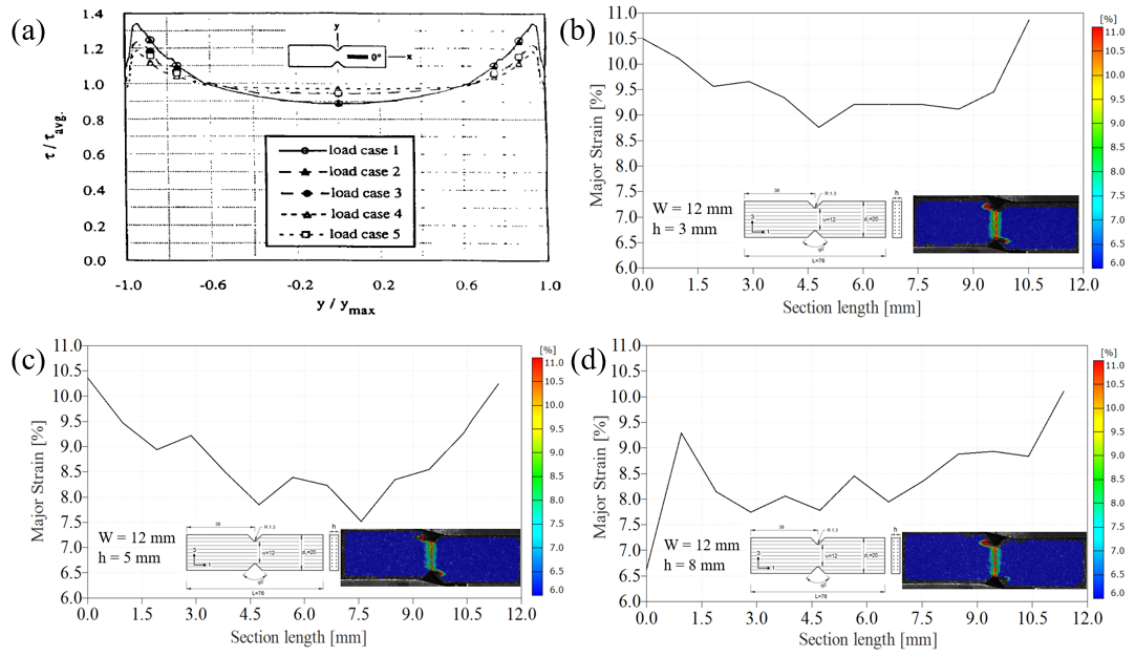


Figure 4-10. (a) the standard stress distribution on G13 shear specimen [170]; (b) the strain distribution on G13 shear specimen ($W = 12$ mm, $h = 3$ mm); (c) the strain distribution on G13 shear specimen ($W = 12$ mm, $h = 5$ mm); (d) the stress distribution on G13 shear specimen ($W = 12$ mm, $h = 8$ mm);

As shown in Figure 4-10 (a) [170], The peak shear stresses occur near the notch root and drop in the centre of the V-notch area. When the fibres are parallel to the longitudinal axis of the specimen, the stress at the centre of the specimen are lower than the average stress across the test section. Therefore, the shear modulus, determined using Equation 4-5, will be higher than the true value [170]. In Figure 4-10 (b), (c) and (d), the strain distributions were evaluated for $h = 3$ mm, 5 mm and 8 mm. However, for $h = 5$ mm, the strain distribution across the V- notch area had the same trend compared to Figure 4-10 (a).

4.3.4.2.G23 shear property

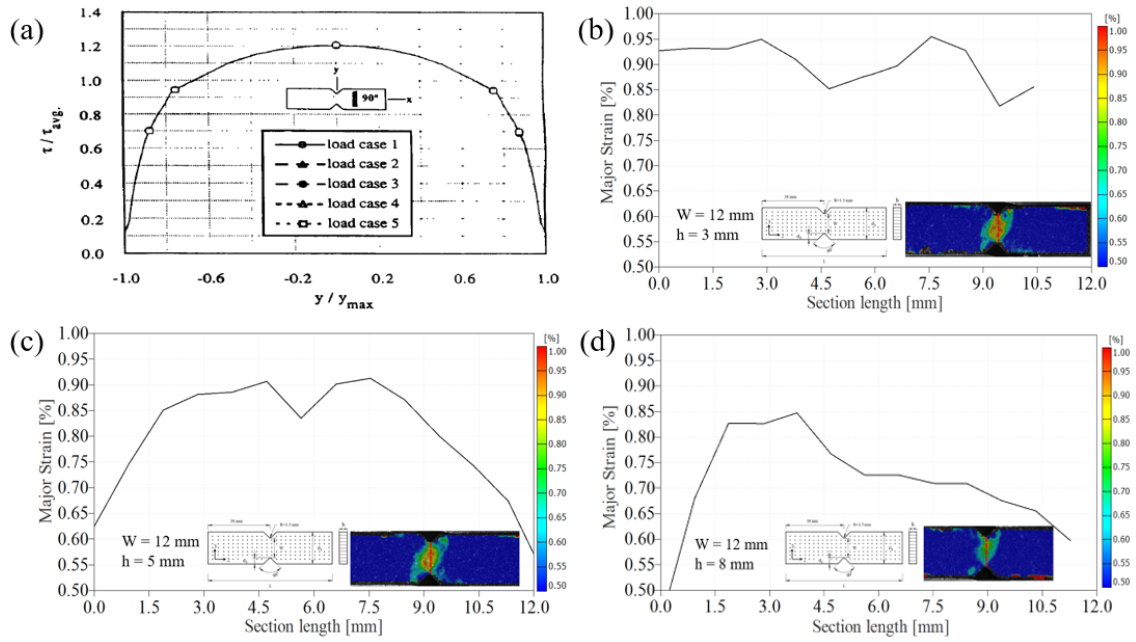


Figure 4-11. (a) the standard stress distribution on G23 shear specimen [170]; (b) the strain distribution on G23 shear specimen (W = 12 mm, h = 3 mm); (c) the strain distribution on G23 shear specimen (W = 12 mm, h = 5 mm); (d) the stress distribution on G23 shear specimen (W = 12 mm, h = 8 mm)

As shown in Figure 4-11 (a). The converse is true for G23 shear test compared with G13 shear test, that is, the highest shear stresses occur at the centre of specimen the V-notch area, and the shear modulus, determined using Equation 4-5, will be lower than the true value [170]. As shown in Figure 4-11 (b), (c) and (d), the strain distributions were evaluated for h = 3 mm, 5 mm and 8 mm. However, for h = 5 mm, the strain distribution across the V-notch area had the same trend compared to Figure 4-11 (a).

4.4. Conclusion

The evaluation of shear test specimen geometry was investigated by using DIC method on the Iosipescu shear test. As shown in the results, the DIC method can obtain accurate results for the strain map during the test. Moreover, the results were compared with the strain gauges on the specimen which showed a good agreement.

For the different V-notch depth, the $W = 12$ mm was chosen because of easy manufacturing and the higher structural stiffness. However, the strain distribution across the V-notch area is one of the most important parameters to consider in order to get a good shear strength analysis. With the comparison of different specimen thicknesses, 3 mm, 5 mm and 8 mm, the $h = 5$ mm was chosen as it shows a clearer strain distribution than the other geometries. By using the shear test results, the load versus displacement trace, the delamination between layers and the final failure of shear test can be compared with the FE Model developed in Chapter 5.

5. Finite element model of thick laminate composite shear strength

5.1. Introduction

A multi-scale model validated with out-of-plane shear testing is presented to analyse thick composite structural failure. Key features of this multi-scale analysis approach are inclusion of shear non linearity and modelling the response at a sub-laminate level whilst the structural failure is predicted at a ply level. Based on this multi-scale approach, a user-defined FORTRAN subroutine (VUMAT) has been written for ABAQUS/EXPLICIT solver and is used to model the shear nonlinearity and intra-laminar failure. In addition, a cohesive zone model is used to predict the inter-laminar delamination. The modelling has been employed to predict the failure processes for Iosipescu shear test specimens with different fibre orientations. The results show that both the failure mode and the load-displacement trace for finite element simulations agree closely with the experimental findings. This demonstrates the validity of this multi-scale, nonlinear, three-dimensional model for thick laminates. In particular, for the Iosipescu shear test, the effect of the fibres being aligned along the length of the specimen or out-of-plane is investigated as well as different dimensions of the specimen. These simulations are validated by experiments using Digital Image Correlation (DIC). This FE modelling was developed in a collaboration with researchers, Dr Liyong Jia, Dr Miao Li and Professor Yuming Jia from AVIC-FAI and a joint paper covering the experiments and modelling is published in Reference [1].

5.2. Methods

5.2.1. Multi-scale modelling of stress-strain behaviour

5.2.1.1. The multi-scale approach

The multi-scale approach crosses the macroscopic ply and sub-laminate level. In this case, an entire thick laminate is divided into several sub-laminates in the thickness direction. In each sub-laminate, there are a number of inner plies. For each sub-laminate, the

equivalent stiffness must be firstly calculated with material mechanical properties and the layup information. After this calculation, the equivalent stiffness is used to calculate the general response of the entire structure, such as global sub-laminate level displacements, stresses and strains. Then, the local ply level stresses and strains in the material coordinate system are obtained through coordinate transformation and decomposition of sub-laminate level stresses and strains. Finally, the ply level stresses and strains are used to predict the damage status and the corresponding stiffness reduction after damage occurs. This procedure is shown in Figure. 5-1 (a). In order to compare the difference between the current multi-scale modelling and the traditional ply-by-ply modelling, the procedure of ply-by-ply analysis is also supplied (see Figure. 5-1 (b)).

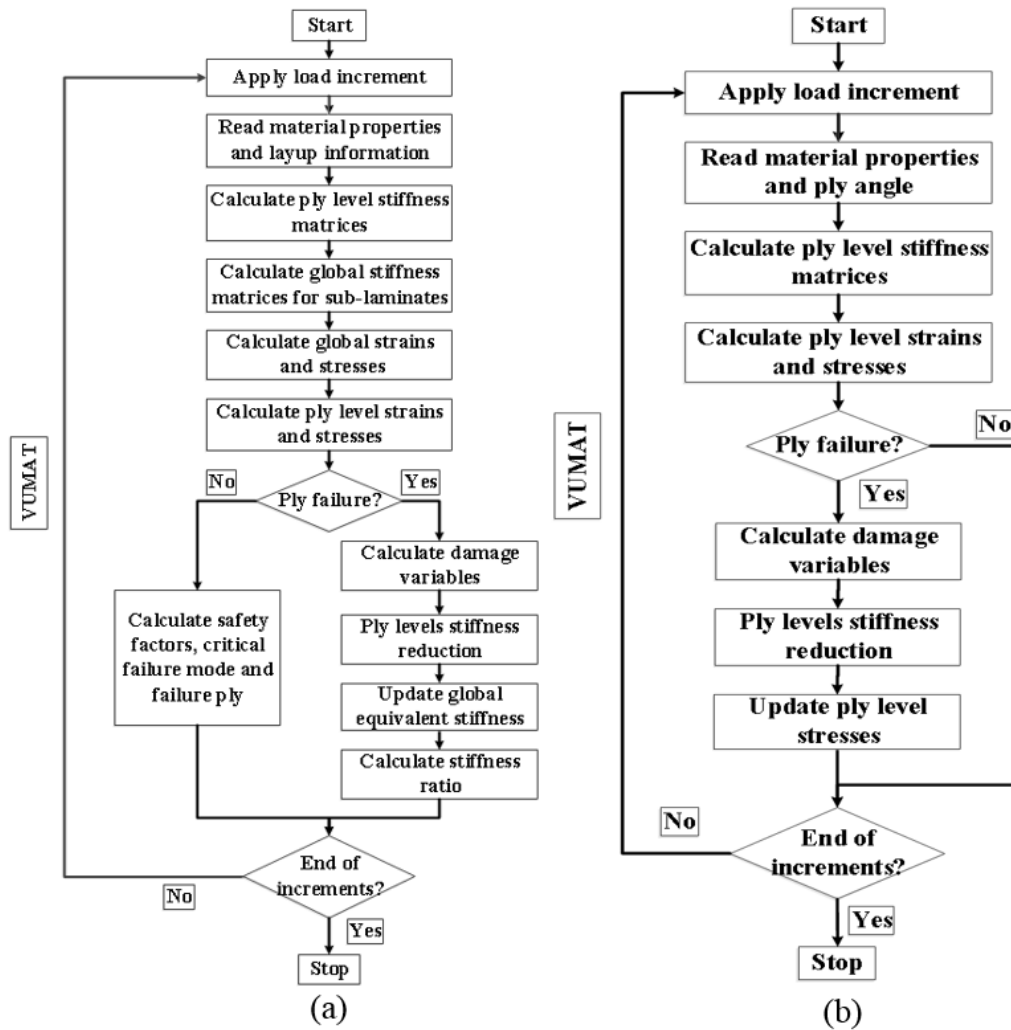


Figure 5-1 Procedure for multi-scale and ply-by-ply modelling.

5.2.1.2.3D equivalent stiffness

According to Chou's 3D equivalent theory [9], a sub-laminate comprised of a number of plies with different ply angle or different material could be equivalent to a monoclinic material, the equivalent stiffness matrix is shown as follows:

$$[\bar{C}_{ij}^*] = \begin{bmatrix} \bar{C}_{11}^* & \bar{C}_{12}^* & \bar{C}_{13}^* & 0 & 0 & \bar{C}_{16}^* \\ & \bar{C}_{22}^* & \bar{C}_{23}^* & 0 & 0 & \bar{C}_{26}^* \\ & & \bar{C}_{33}^* & 0 & 0 & \bar{C}_{36}^* \\ & & & \bar{C}_{44}^* & \bar{C}_{45}^* & 0 \\ & sym & & & \bar{C}_{55}^* & 0 \\ & & & & & \bar{C}_{66}^* \end{bmatrix} \quad (5-1)$$

Where the barred notation “ $\bar{}$ ” is used to indicate that the stiffness coefficient is in the global coordinate system of the laminate and the star “ $*$ ” signifies that it is an equivalent value. The stress-strain constitutive relationship for the sub-laminate is described as:

$$[\bar{\sigma}^*] = [\bar{C}_{ij}^*][\bar{\varepsilon}^*] \quad (5-2)$$

where $[\bar{\sigma}^*]$ and $[\bar{\varepsilon}^*]$ are the global equivalent stresses and strains.

The coefficients of the sub-laminate stiffness matrix, $\bar{C}_{ij}^*$, are defined in Equations 5-3 to 5-6:

$$\bar{C}_{ij}^* = \sum_{k=1}^n V^k \left[\bar{C}_{ij}^k - \frac{\bar{C}_{i3}^k \bar{C}_{3j}^k}{\bar{C}_{33}^k} + \frac{\bar{C}_{i3}^k \sum_{l=1}^n V^l \bar{C}_{3j}^l}{\bar{C}_{33}^k \sum_{l=1}^n V^l} \right] \quad (i, j = 1, 2, 3, 6) \quad (5-3)$$

$$\bar{C}_{ij}^* = \bar{C}_{ji}^* = 0 \quad (i = 1, 2, 3, 6; j = 4, 5) \quad (5-4)$$

$$\bar{C}_{ij}^* = \frac{\sum_{k=1}^n \frac{V^k}{\Delta_k} \bar{C}_{ij}^k}{\sum_{k=1}^n \sum_{l=1}^n \frac{V^k V^l}{\Delta_k \Delta_l} (\bar{C}_{44}^k \bar{C}_{55}^l - \bar{C}_{45}^k \bar{C}_{54}^l)} \quad (i, j = 4, 5) \quad (5-5)$$

$$\Delta_k = \bar{C}_{44}^k \bar{C}_{55}^k - \bar{C}_{45}^k \bar{C}_{54}^k \quad (5-6)$$

where k refers to the k^{th} ply of the sub-laminate, n refers to the number of plies in the sub-laminate, V^k is the ratio of the original thickness of the k^{th} ply to the original total thickness of the entire sub-laminate and $\bar{C}_{ij}^k$ represents the stiffness coefficient of the k^{th} ply in a sub-laminate in the global coordinate system, this is obtained from coordinate transformation of stiffness matrix in local material coordinate system [171].

5.2.1.3. Stress and strain decomposition

With the global equivalent stiffness, the general response of the entire structure, such as global stresses $[\bar{\sigma}^*]$ and strains $[\bar{\varepsilon}^*]$ can be easily obtained. However, all these values are in sub-laminate level. In order to decompose the ply level stresses and strains, the following assumptions should be introduced [129]:

$$\bar{\varepsilon}_i^k = \bar{\varepsilon}_i^* \quad (i=1,2,6; k=1,2,\dots,n) \quad (5-7)$$

$$\bar{\sigma}_i^k = \bar{\sigma}_i^* \quad (i=3,4,5; k=1,2,\dots,n) \quad (5-8)$$

where $\bar{\sigma}_i^k$ and $\bar{\varepsilon}_i^k$ are the stress and strain components for the k^{th} ply in global coordinate system, and $\bar{\sigma}_i^*$, $\bar{\varepsilon}_i^*$ refer to the stress and strain components for the sub-laminate in global coordinate system. With the above assumptions, all the remaining ply level stress and strain components could be obtained from Equations 5-9 and 5-10:

$$\begin{bmatrix} \bar{\varepsilon}_3^k \\ \bar{\varepsilon}_4^k \\ \bar{\varepsilon}_5^k \end{bmatrix} = \begin{bmatrix} \bar{C}_{33}^k & \bar{C}_{34}^k & \bar{C}_{35}^k \\ \bar{C}_{43}^k & \bar{C}_{44}^k & \bar{C}_{45}^k \\ \bar{C}_{53}^k & \bar{C}_{54}^k & \bar{C}_{55}^k \end{bmatrix}^{-1} \left[\begin{bmatrix} \bar{\sigma}_3^k \\ \bar{\sigma}_4^k \\ \bar{\sigma}_5^k \end{bmatrix} - \begin{bmatrix} \bar{C}_{31}^k & \bar{C}_{32}^k & \bar{C}_{36}^k \\ \bar{C}_{41}^k & \bar{C}_{42}^k & \bar{C}_{46}^k \\ \bar{C}_{51}^k & \bar{C}_{52}^k & \bar{C}_{56}^k \end{bmatrix} \begin{bmatrix} \bar{\varepsilon}_1^k \\ \bar{\varepsilon}_2^k \\ \bar{\varepsilon}_6^k \end{bmatrix} \right] \quad (k=1,2,\dots,n) \quad (5-9)$$

$$\begin{bmatrix} \bar{\sigma}_1^k \\ \bar{\sigma}_2^k \\ \bar{\sigma}_6^k \end{bmatrix} = \begin{bmatrix} \bar{C}_{11}^k & \bar{C}_{12}^k & \bar{C}_{13}^k & \bar{C}_{14}^k & \bar{C}_{15}^k & \bar{C}_{16}^k \\ \bar{C}_{21}^k & \bar{C}_{22}^k & \bar{C}_{23}^k & \bar{C}_{24}^k & \bar{C}_{25}^k & \bar{C}_{26}^k \\ \bar{C}_{61}^k & \bar{C}_{62}^k & \bar{C}_{63}^k & \bar{C}_{64}^k & \bar{C}_{65}^k & \bar{C}_{66}^k \end{bmatrix} \begin{bmatrix} \bar{\varepsilon}_1^k \\ \bar{\varepsilon}_2^k \\ \bar{\varepsilon}_3^k \\ \bar{\varepsilon}_4^k \\ \bar{\varepsilon}_5^k \\ \bar{\varepsilon}_6^k \end{bmatrix} \quad (k=1,2,\dots,n) \quad (5-10)$$

As this stage, all the ply level stresses and strains in the global coordinate system have been obtained. However, before using them to predict the failure status of each ply, they have to be transformed from the global coordinate system to the local material coordinate system.

5.2.1.4. Non-linear shear constitutive response

For in-plane or out-of-plane shear in laminate, the non-linear response is obvious [127]. In the present work, the shear nonlinearity in each ply is represented by using the Ramberg-Osgood constitutive equation [172]. Taking the non-linear stress-strain relationship between τ_{13} and γ_{13} as an example, it is defined as:

$$\tau_{13} = \frac{G_{13}^0 \gamma_{13}}{\left(1 + \left(\frac{G_{13}^0 \gamma_{13}}{S_{13}} \right)^n \right)^{\frac{1}{n}}} \quad (5-11)$$

The secant modulus G_{13}^s could be obtained by the following equation [172]:

$$G_{13}^s = \frac{\tau_{13}}{\gamma_{13}} = \frac{G_{13}^0}{\left(1 + \left(\frac{G_{13}^0 \gamma_{13}}{S_{13}} \right)^n \right)^{\frac{1}{n}}} \quad (5-12)$$

Where G_{13}^0 refers to the initial shear modulus, S_{13} is the shear strength and n is the shape factor of the nonlinear stress-strain curve. The parameter n is determined by fitting to experimental data as shown in Figure 5-2.

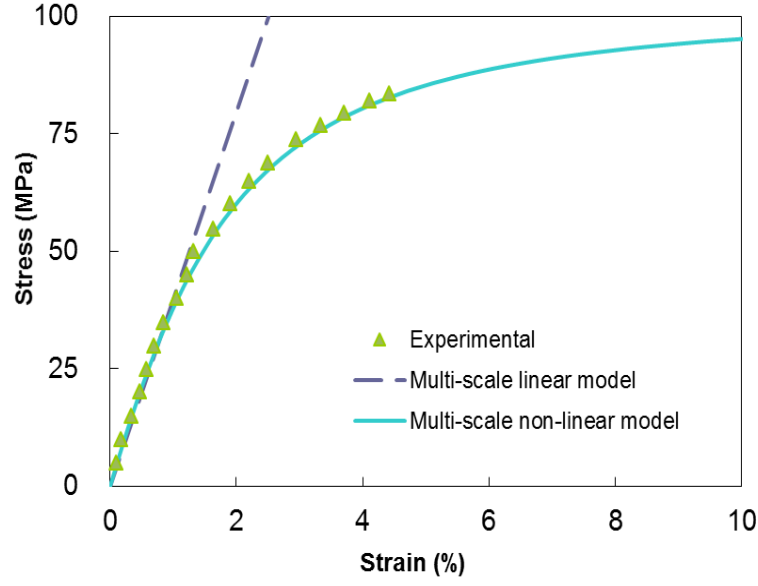


Figure 5-2 Non-linear stress-strain relationship and the effect of shape factor.

5.2.2. Progressive failure modelling

5.2.2.1. Failure criterion

For in-plane fibre tensile and compressive failure, the simple maximum-stress criterion is used [125, 173, 174, 175] :

$$f_{ft} = \frac{\sigma_{11}}{X_T} \quad (\sigma_{11} \geq 0) \quad (5-13)$$

$$f_{fc} = -\frac{\sigma_{11}}{X_C} \quad (\sigma_{11} < 0) \quad (5-14)$$

where X_T and X_C are the axial tensile and compressive strength.

For matrix failure, the following equations are used to calculate the normal traction σ_n and the shear tractions τ_L and τ_T in the potential fracture plane [176].

$$\sigma_n = \frac{\sigma_{22} + \sigma_{33}}{2} + \frac{\sigma_{22} - \sigma_{33}}{2} \cos(2\phi) + \tau_{23} \sin(2\phi) \quad (5-15)$$

$$\tau_T = -\frac{\sigma_{22} - \sigma_{33}}{2} \sin(2\phi) + \tau_{23} \cos(2\phi) \quad (5-16)$$

$$\tau_L = \tau_{12} \cos(\phi) + \tau_{13} \sin(\phi) \quad (5-17)$$

where ϕ is the fracture angle as shown in Figure. 5-3.

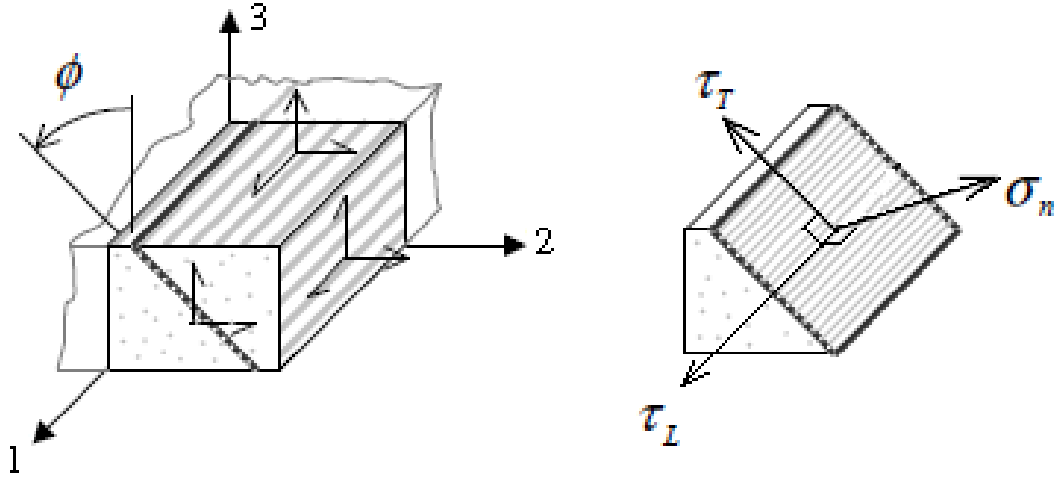


Figure 5-3 Fracture plane for a 3D stress state and the associated stresses.

For matrix compressive failure ($\sigma_n < 0$), the following criterion is used [176]:

$$f_{mc} = \left(\frac{\tau_T}{S_T - \mu_T \sigma_n} \right)^2 + \left(\frac{\tau_L}{S_L - \mu_L \sigma_n} \right)^2 \quad (\sigma_n < 0) \quad (5-18)$$

Where S_L and S_T are the longitudinal and transverse (to the fibres) shear strengths, μ_L and μ_T are longitudinal and transverse friction coefficients which can be obtained from the following equations:

$$\mu_T = -\frac{1}{\tan(\phi_0)} \quad (5-19)$$

$$\mu_L = S_L \frac{\mu_T}{S_T} \quad (5-20)$$

The longitudinal shear strength, S_L , is obtained from a shear test, whilst the transverse shear strength, S_T , is calculated from the compressive strength in the matrix direction, Y_C and the fracture angle for pure matrix compression, ϕ_0 (typically, $\phi_0 \approx 53^\circ$) [176]:

$$S_T = Y_C \cos(\phi_0) \left(\sin(\phi_0) + \frac{\cos(\phi_0)}{\tan(2\phi_0)} \right) \quad (5-21)$$

For matrix tensile failure ($\sigma_n \geq 0$), the quadratic stress criterion is employed [176].

$$f_{mt} = \left(\frac{\sigma_n}{Y_t} \right)^2 + \left(\frac{\tau_T}{S_T} \right)^2 + \left(\frac{\tau_L}{S_L} \right)^2 \quad (\sigma_n \geq 0) \quad (5-22)$$

5.2.2.2. Damage evolution

For fibre failure, the damage variable d_f is given by

$$d_f = 1 - (1 - d_{ft})(1 - d_{fc}) \quad (5-23)$$

where

$$d_{ft}(\varepsilon_{11}) = \frac{\varepsilon_{f,1}^t}{\varepsilon_{f,1}^t - \varepsilon_{0,1}^t} \left(1 - \frac{\varepsilon_{0,1}^t}{\varepsilon_{11}} \right) \quad (5-24)$$

$$d_{fc}(\varepsilon_{11}) = \frac{\varepsilon_{f,1}^c}{\varepsilon_{f,1}^c - \varepsilon_{0,1}^c} \left(1 - \frac{\varepsilon_{0,1}^c}{\varepsilon_{11}} \right) \quad (5-25)$$

The superscripts t and c refer to tension and compression, respectively, $\varepsilon_{0,1}^t$ and $\varepsilon_{0,1}^c$ correspond to the strain in tension and compression when the failure initiation appears, $\varepsilon_{f,1}^t$ and $\varepsilon_{f,1}^c$ correspond to the maximum strain in tension and compression at final failure, i.e. when the damage variables are equal to one. The maximum failure strain $\varepsilon_{f,1}^t$ and $\varepsilon_{f,1}^c$ are then obtained by:

$$\varepsilon_{f,1}^t = \frac{2G_{IC}^t}{X_T L} \quad (5-26)$$

$$\varepsilon_{f,1}^c = \frac{2G_{IC}^c}{X_C L} \quad (5-27)$$

where X_T and X_C are the tensile and compressive strengths in fibre direction, G_{IC}^t and G_{IC}^c are the intra-laminar fracture toughness in fibre tension and compression and L is the characteristic length of an element. In the present research, all the hexahedral elements (see Figure 5-4) used in the potential failure area are with equal in-plane length, so the characteristic length, L , has been obtained by:

$$L = \sqrt{\frac{V}{T}} \quad (5-28)$$

where V is the volume of an element and T is the dimension in the thickness direction of an element as shown in Figure. 5-4 (a). The stress–strain behaviour in fibre tension and compression before and after damage occurs is depicted in Figure. 5-4 (b).

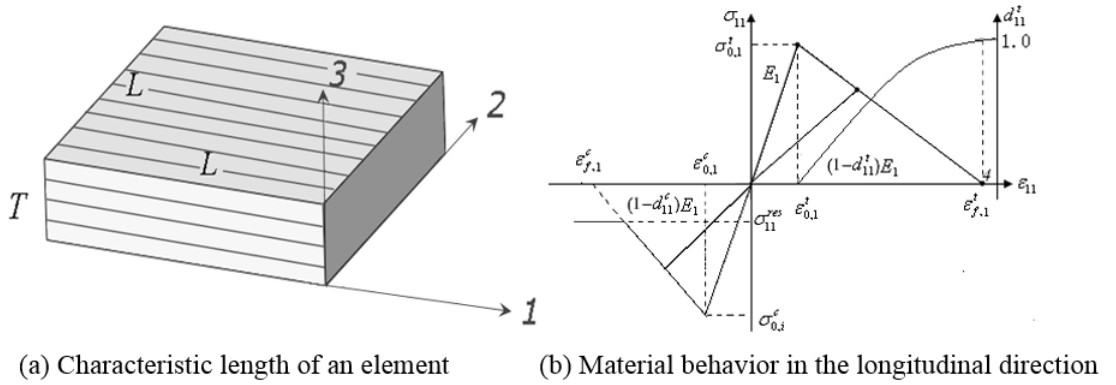


Figure 5-4 Determination of the characteristic length and the longitudinal material behaviour.

When the energy-based damage evolution criterion is used, there is a lower limit to the permissible element size that can be used, otherwise, for very coarse meshes ($L \rightarrow \infty$) and ε_f tends to zero [176].

For matrix failure, the critical strain energy release rate for matrix fracture is much smaller than that of fibre fracture, so the element size has to be limited to be very small. For thick-sectioned laminates, and FEA model with too many small elements is unacceptable due to its low efficiency. Therefore, in matrix failure analysis, a parametric damage evolution method is typically used to replace the energy-based damage evolution. When $f_{mt} \geq 1$, the damage variable for matrix tensile failure d_{mt} is set to zero, or when $f_{mc} \geq 1$, the damage variable for matrix compressive failure d_{mc} is set to 0.6, as was shown to be suitable in [177, 178], the damage variable d_m is given by:

$$d_m = 1 - (1 - d_{mt})(1 - d_{mc}) \quad (5-29)$$

When damage occurs in the k^{th} ply and the fracture angle ϕ for matrix failure equals to zero, the stiffness matrix for this damage ply is updated by

$$\mathbf{C}_d^k = \begin{bmatrix} (1-d_f)C_{11}^0 & (1-d_f)(1-d_m)C_{12}^0 & (1-d_f)C_{13}^0 & 0 & 0 & 0 \\ (1-d_f)(1-d_m)C_{12}^0 & (1-d_m)C_{22}^0 & (1-d_m)C_{23}^0 & 0 & 0 & 0 \\ (1-d_f)C_{13}^0 & (1-d_m)C_{23}^0 & C_{33}^0 & 0 & 0 & 0 \\ 0 & 0 & 0 & (1-d_m)C_{44}^0 & 0 & 0 \\ 0 & 0 & 0 & 0 & (1-d_f)C_{55}^0 & 0 \\ 0 & 0 & 0 & 0 & 0 & (1-d_f)(1-d_m)C_{66}^0 \end{bmatrix} \quad (5-30)$$

where C_{ij}^0 is the undamaged stiffness coefficient in the local material coordinate system.

When damage occurs in a ply and the fracture angle ϕ for matrix failure does not equal to zero, the stiffness matrix should be firstly transformed from local material coordinate system to fracture coordinate system, and then the stiffness coefficients in fracture coordinate system are all discounted, after that, the damaged stiffness matrix should be transformed back from fracture coordinate system to local material coordinate system. In

this research, this transformation of stiffness matrix is used for both matrix tensile and compressive failure, for which the fracture angle is usually not equal to zero.

After the damaged stiffness matrix for the k^{th} ply in local material coordinate system C_d^k is obtained, then it should be transformed from the local material coordinate system to the global coordinate system in order to get the global damaged stiffness matrix, $\bar{C}_d^k$, after that, equation 5-5 to 5-8 are re-used to calculate the global damaged equivalent stiffness matrix for one sub-laminate, $\bar{C}_d^*$.

For thick laminated structures, it is not efficient to monitor and save the damage variables for each ply, as this would consume a large amount of computation time and storage space. A much better approach has been followed in this work using the concept of the stiffness ratio for the sub-laminate, as discussed in [128-129]. It then follows that:

$$R_{ij} = \frac{\bar{C}_{ij}^{c,*}}{\bar{C}_{ij}^{0,*}} \quad (i, j = 1, 6) \quad (5-31)$$

where R_{ij} is the stiffness ratio, $\bar{C}_{ij}^{0,*}$ refers to the equivalent undamaged stiffness coefficient in the global coordinate system at the beginning of loading, and $\bar{C}_{ij}^{c,*}$ is the current equivalent damaged or discounted stiffness coefficient in global coordinate system. The initial value of each stiffness ratio is one. At the beginning of each load increment, all the stiffness ratios are used to update the current stiffness coefficients, whilst, at the end of each load increment, the up-to-date stiffness ratios are calculated and saved according to the current damage status. In this way, a major saving in computational time and storage space is achieved.

5.3. Results and discussion

5.3.1. Validation of multi-scale approach

Two virtual composite laminates ($[0_4]$ and $[0/90]_s$) measuring $20 \times 10 \times 0.5 \text{ mm}^3$ were used to evaluate the performance of the multi-scale model. The laminates were continuously loaded in the fibre direction under displacement control (see Figure 5-5). The material

properties for the composite (CCF300/5228A) were selected as shown in Table 4-1, and the thickness of a single ply was taken as 0.125mm [179, 180].

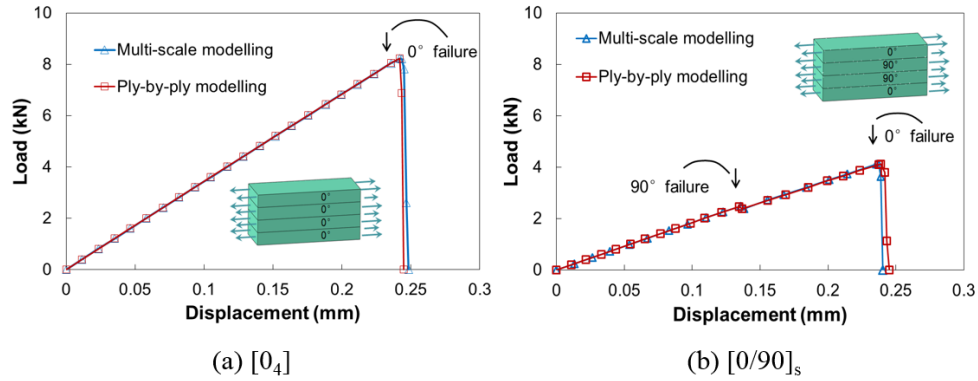


Figure 5-5 Validation of multi-scale modelling as compared with ply-by-ply modelling.

For comparison purposes, the load-displacement responses for the composite panel obtained using multi-scale modelling and traditional ply-by-ply modelling are represented on a single graph in Figure 5-5. All the computation has been implemented by using the same number of CPUs and under the same hardware conditions. The loading velocity in each FEA model is also the same. Figure 5-5 (a) compares the structural response obtained by using the two different modelling approaches for laminate with stacking sequence $[0_4]$. Correspondingly, Figure 5-5 (b) compares the structural response for laminate with stacking sequence $[0/90]_s$. Table 4-1 gives the mechanical properties of the composite laminates evaluated in this research as quoted by the prepreg manufacturer and Table 5-2 compares the accuracy and computing efficiency for the two different modelling methods for the $[0_4]$ and $[0/90]_s$ stacks sequences.

From Figure 5-5, the load versus displacement trace for the current multi-scale modelling and traditional ply-by-ply modelling are similar. Table 5-2 also gives the first peak load and the maximum load obtained from the two methods which are also similar. This demonstrates that the accuracy of multi-scale modelling is acceptable. Moreover, the stable time increment for the multi-scale modelling is much larger than that for ply-by-ply modelling. With such a larger stable time increment and relatively fewer finite elements, the current multi-scale modelling is much more efficient in calculation than that of the traditional ply-by-ply modelling.

Table 5-1. Comparison of accuracy and computing efficiency for different analysis methods.

Stacking sequence	Modelling Type	First peak load(N)	Maximum Load(N)	Stable Time Increment
[0 ₄]	Multi-scale		8221.00	1.87299e-07
	Ply-by-ply		8238.46	4.83202e-08
[0/90] _s	Multi-scale	2489.91	4108.59	1.87299e-07
	Ply-by-ply	2490.92	4144.41	4.83202e-08

5.3.2. Out-of-plane shear test and simulation for thick laminates

As introduced in chapter 4, Six variants of Iosipescu shear specimens (see Figure 4-1) with different fibre orientation (G13 shear test and G23 shear test) and different out of plane thickness or specimen height, h , were designed according to the ASTM D5379 standard [44].

For each specimen with 160 plies, the thickness d_1 is 20 mm and the entire laminate is divided into 20 sub-laminates through the thickness direction, each sub-laminate is modelled with C3D8R elements, specially, for the G13 shear test, 5 layers of cohesive elements (COH3D8) are used to model the failure of delamination [181]. The other dimensions of the specimens are given in Figures. 4-1(c) and 6(d).

Material properties were taken from Table 4-1 and Table 5-2 gives the inter-laminar stiffness, strength and fracture toughness for the cohesive zone model as used in the modelling. The G13 shear test results are shown in Figure 5-6 for when the fibres are in the longitudinal direction. The shear test specimens were continuously loaded under displacement control and the full field strain fields were measured using 3D Digital Image Correlation (DIC). The G13 & G23 shear tests are firstly presented for specimens with out-of-plane thickness or specimen height, $h=5$ mm and they are then compared with specimens of height, $h=8$ mm and $h=3$ mm.

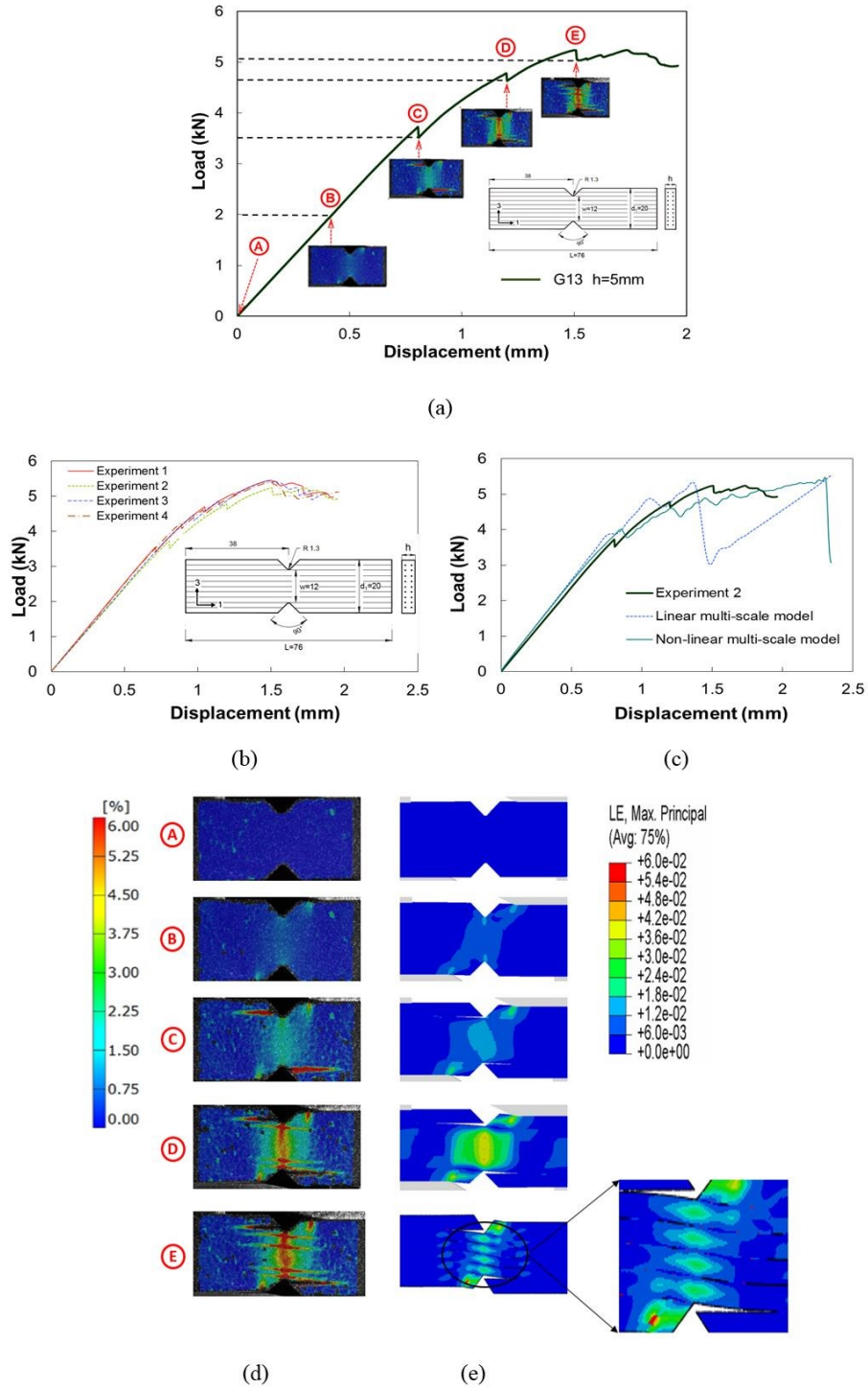


Figure 5-6. Comparison of experimental and simulated results for G13 shear test ($h=5\text{mm}$): (a) Load-displacement trace and failure mode; (b) Load versus displacement trace; (c) Comparison of experimental and simulated load-displacement traces; (d) Failure mode and maximum principal strain from DIC; (e) Failure mode and maximum principal strain from FEA.

For the G13 shear test ($h=5$ mm), the experimental load-displacement curve and strain field are used with DIC to explain the failure procedure of G13 shear test (Figure 5-7). In Figure. 5-6 (a), position ① refers to the beginning of loading, where both the load and displacement are near zero. Position ② is approximately at the middle of the linear portion, the strain concentration on the specimen appears in two areas, one is at the bottom of the V-notch and another is at the contact area near the sharp corner of the fixture. At position ③, the first two cracks caused by delamination appear at the bottom of the two V-notches, accompanied with the first load drop on the load-displacement curve. With the increase of the displacement, the cracks continue to propagate, then it comes to position ④, here the second two cracks appear, which are nearly parallel to the initial cracks, correspondingly, accompanied with the second load drop on the load-displacement curve. The curve between position ③ and position ④ is slightly non-linear. When the displacement continues to increase, the reaction load gradually reaches its peak point, and then a number of cracks begin to appear at the middle of two notches, at the same time, the third load drop on the load-displacement curve appears, shown as position ⑤. After that, with the increase of displacement, the reaction load climbs slightly and then gradually decrease. Due to the constraint of the fixture, a maximum of 2 mm displacement was set to protect the fixture.

Table 5-2. Inter-laminar stiffness, strength and fracture toughness of composite laminate for Iosipescu shear tests.

E	G	N_0	S_0	T_0	G_{IC}	G_{IIC}	G_{IIIc}
(MPa)	(MPa)	(MPa)	(MPa)	(MPa)	(N/mm)	(N/mm)	(N/mm)
9000	4500	71.4	102	102	0.75	1.5	1.5

Four specimens (Experiment 1, Experiment 2, Experiment 3, and Experiment 4) were tested to confirm the reproducibility of the experiment and the results are shown in Figures. 5-6 (b) and 5-6 (c) compares the simulated load-displacement curves with that of

Experiment 2, both the linear multi-scale model and the non-linear multi-scale model are used to predict the structural response of the specimen.

The load-displacement curve obtained from the non-linear multi-scale model fit better than that of linear model. For the first peak load, the error between non-linear multi-scale model and the average experimental results is about 0.2%, whilst the error of linear multi-scale model is about -2.9%; for the maximum load, the error of non-linear multi-scale model is about 1.7%, whilst the error for linear multi-scale model is larger than 12.3%. It is clear that, for G13 shear test, the non-linear effect of material on structural response is significant and cannot be ignored.

Figures. 5-6 (d) and 5-6 (e) show the comparison of failure mode and maximum principal strain from DIC and non-linear FEA at the five positions (Ⓐ to Ⓔ) for G13 shear test (h=5mm). It can be seen that the predicted failure mode and the position of strain concentration are in good agreement with experimental results from DIC. But there is notable difference for the position and value of the maximum principal strain in the strain plots. The reason for this difference is that, when damage is initiated in the FEA model, the corresponding elements will be deleted in the FEA model in order to form the cracks, so the maximum principal strain becomes zero (this is the area in white in Figure. 5-6 (d)). However, for the DIC strain plots, when a crack appears and the size of the crack is small, the strain value around the tip of the crack is very large (this is the area in red in Figure. 5-6 (c)).

For G23 shear test (h=5mm), Figure. 5-7 shows the failure process in a similar way as in Figure. 5-6. The load-displacement curve is almost linear through the entire loading history. With the increase of the displacement, the reaction force gradually reaches its peak value, and then two cracks appear and propagate instantaneously, accompanied with the load drops drastically (Position Ⓔ as shown in Figure. 5-7 (a)).

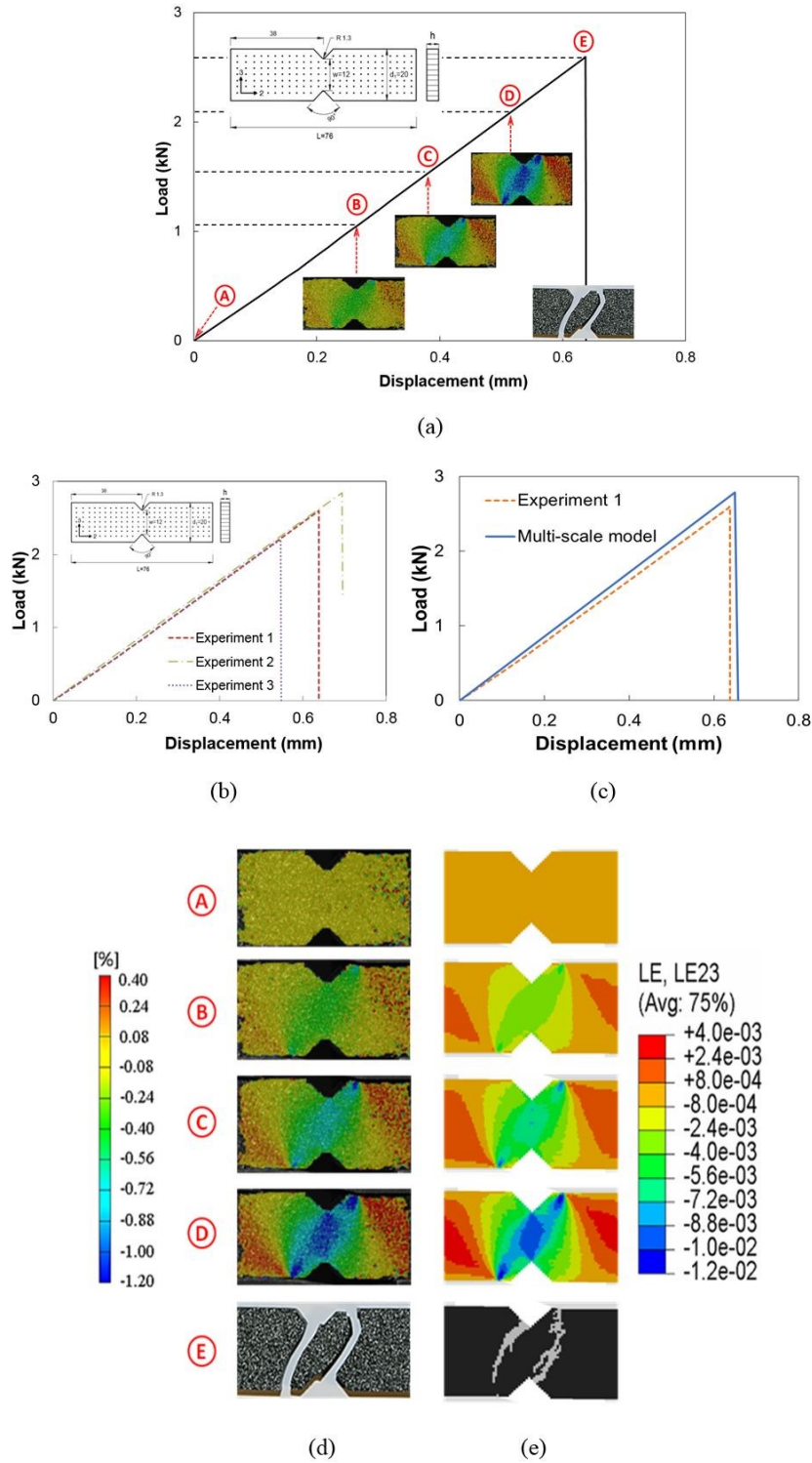


Figure 5-7. Comparison of experimental and simulated results for G23 shear test (h=5mm): (a) Load-displacement trace and failure mode; (b) Load versus displacement trace; (c) Comparison of experimental and simulated load-displacement traces; (d) Failure mode and γ_{23} from DIC; (e) Failure mode and γ_{23} from FEA.

Three specimens (Experiment 1, Experiment 2, and Experiment 3) were tested to confirm the experimental load-displacement curve. The results are shown in Figure. 5-7 (b). The average experimental load capacity for G23 shear test is 2.55 kN, and the maximum experimental load capacity is 2.8 kN, whilst the predicted load capacity is 2.8 kN. The G23 shear test is quite sensitive to the manufacturing quality and the mating position of specimen, which is the reason why the experimental results differ.

Figure. 5-7 (c) shows the comparison of experimental load-displacement curve (Experiment 1) with predicted results from non-linear multi-scale model. Since the rigs are modelled with rigid elements, the slope of the simulated load-displacement curve is higher than for the experimental result.

Figures. 5-7 (d) and 5-7 (e) compare the failure mode and shear strain γ_{23} from DIC and non-linear FEA at five positions (as shown in Figure. 5-9) for G23 shear test (h=5mm). The strain plot from FEA compares well with the experimental strain plots from DIC. Features such as the stress concentration position and the crack propagation path are numerically captured by the FEA model.

To confirm further the accuracy of the three-dimensional multi-scale modelling, experiments and FEA analysis for G13 and G23 shear test with a height of 8 mm & 3 mm were also implemented. Figure.5-8 (a) shows the load-displacement trace and failure mode for G13 shear test (h=8 mm). The characteristic load-displacement trace and failure mode for the shear test with a height of 8 mm is similar with the results of shear test with a height of 5 mm. Three specimens (Experiment 1, Experiment 2, and Experiment 3) were tested to confirm the experimental reproducibility and the results are shown in Figure. 5-8 (b). Both the linear multi-scale model and the non-linear multi-scale model are used to predict the structural response of the specimen. As shown in Figure. 5-8 (c), the comparison between experimental and simulated load-displacement curves confirms that, for G13 shear test, the non-linear multi-scale model has a much higher accuracy than the linear model.

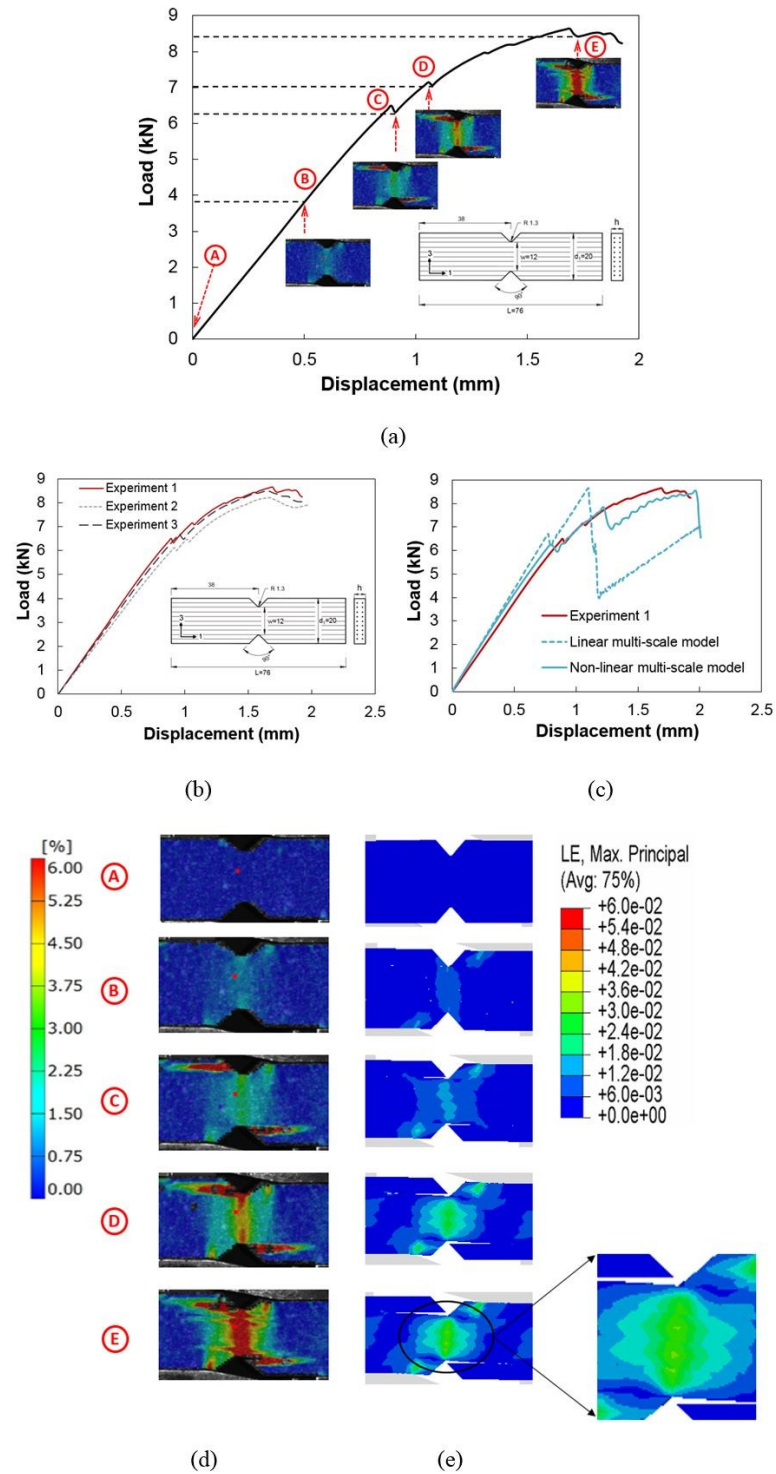


Figure 5-8. Comparison of experimental and simulated results for G13 shear test (h=8mm): (a) Load-displacement trace and failure mode; (b) Load versus displacement trace; (c) Comparison of experimental and simulated load-displacement traces; (d) Failure mode and maximum principal strain from DIC; (e) Failure mode and maximum principal strain from FEA.

Figures 5-8 (d) and 5-8 (e) show the similar comparison of failure mode and maximum principal strain from DIC and non-linear FEA at the specified positions for the G13 shear test ($h=8$ mm). The failure mode and the position of strain concentration from FEA agrees well with experimental results from DIC. Again, for FEA when damage is initiated the corresponding elements are deleted to give the white region between the crack surfaces (Figure 5-8 (e)). However, for the DIC strain plots, when a crack appears and the size of the crack is small, the strain value around the tip of the crack is very large and gives a red region (Figure 5-8 (d)).

For G23 shear test ($h=8$ mm), the failure procedure as shown in Figure. 5-9 is quite similar with the experimental result of G23 shear test with a height of 5 mm. Three specimens (Experiment 1, Experiment 2, and Experiment 3) were tested and the load-displacements curves are shown in Figure. 9 (b). The average experimental load capacity for G23 shear test ($h=8$ mm) is 4.3 kN, and the maximum experimental load capacity is 4.7 kN, whilst the predicted load capacity is 4.6 kN. The load-displacement curve of Experiment 3 is selected to compare with FEA simulated results (as shown in Figure. 5-9 (c)).

Figures 5-9 (d) and 9 (e) compare the failure mode and shear strain γ_{23} from DIC and non-linear FEA at the five positions (Ⓐ to Ⓔ) for the G23 shear test ($h=8$ mm). The failure mode, the crack propagation path and the strain from FEA all agree well with experimental results from DIC.

Figures 5-10 and 5-11 shows the load-displacement trace and failure mode for G13 and G23 shear tests ($h=3$ mm). For G13, there were two specimens (Experiment 1 and Experiment 2) and for G23, there were three specimens (Experiment 1, Experiment 2 and Experiment 3). The characteristic load-displacement trace and failure mode for the shear test with a $h=3$ mm are similar as for $h=8$ mm. For all three out of plane thicknesses, the numerical simulation of the G23 shear test indicated that matrix failure due to tensile and shear tractions in the potential fracture plane is the dominant failure mode in the specimen. By introducing the appropriate failure criterion when predicting the potential fracture plane in the multi-scale analysis, the crack propagation path has been captured effectively.

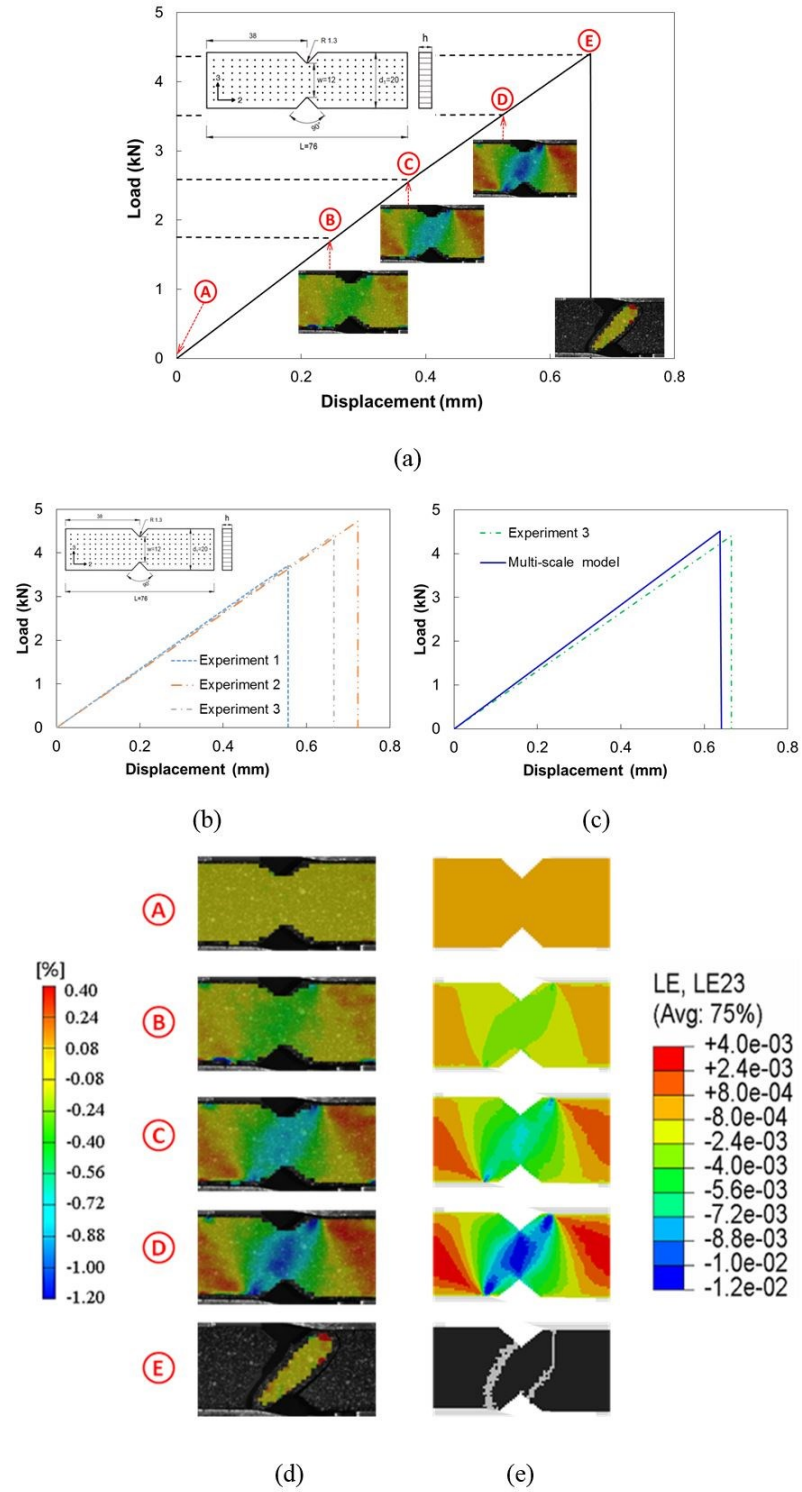


Figure 5-9. Comparison of experimental and simulated results for G23 shear test ($h=8\text{mm}$): (a) Load-displacement trace and failure mode; (b) Load versus displacement trace; (c) Comparison of experimental and simulated load-displacement traces; (d) Failure mode and γ_{23} from DIC; (e) Failure mode and γ_{23} from FEA.

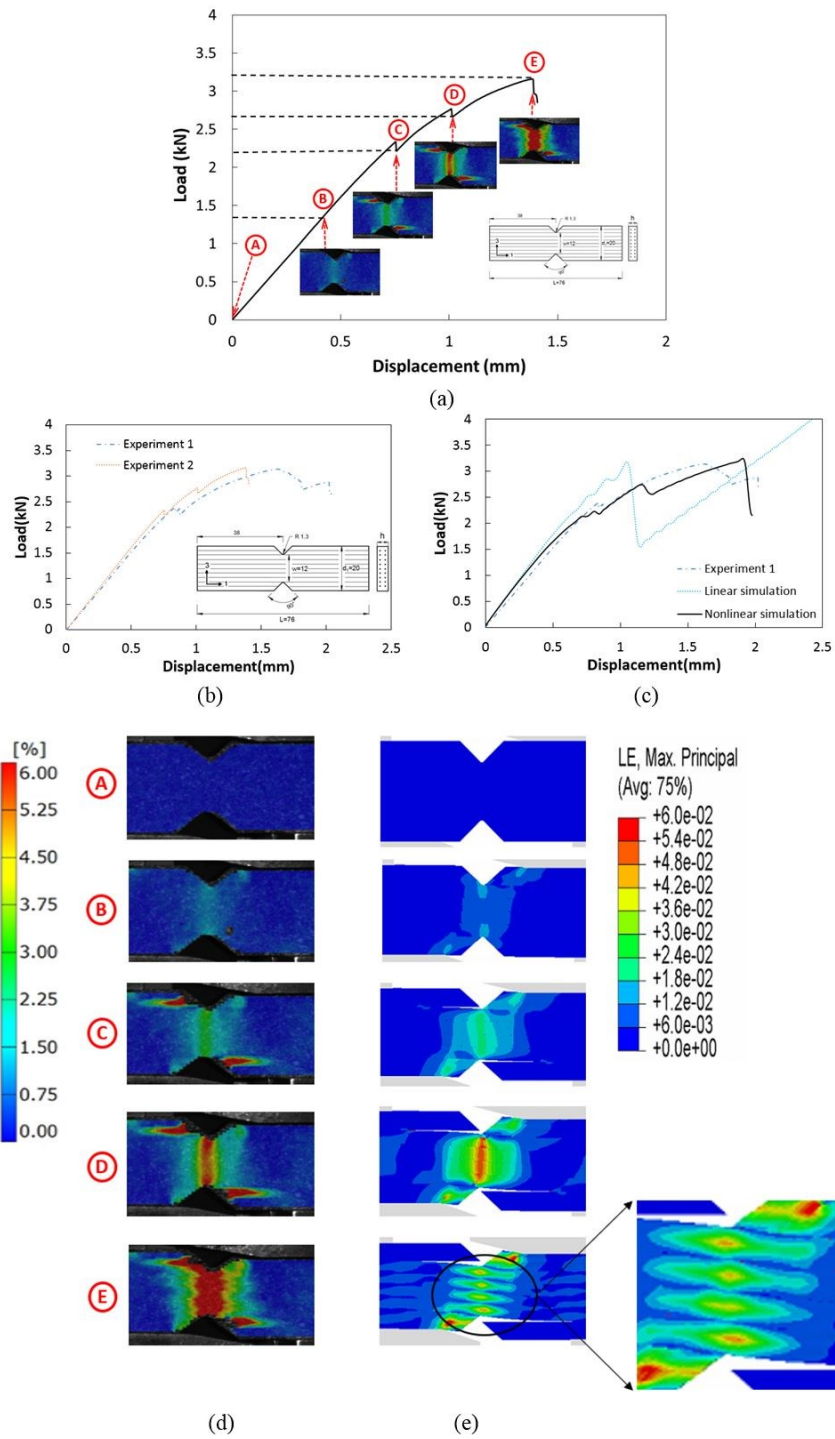


Figure 5-10. Comparison of experimental and simulated results for G13 shear test ($h=3\text{mm}$): (a) Load-displacement trace and failure mode; (b) Load versus displacement trace; (c) Comparison of experimental and simulated load-displacement traces; (d) Failure mode and maximum principal strain from DIC; (e) Failure mode and maximum principal strain from FEA.

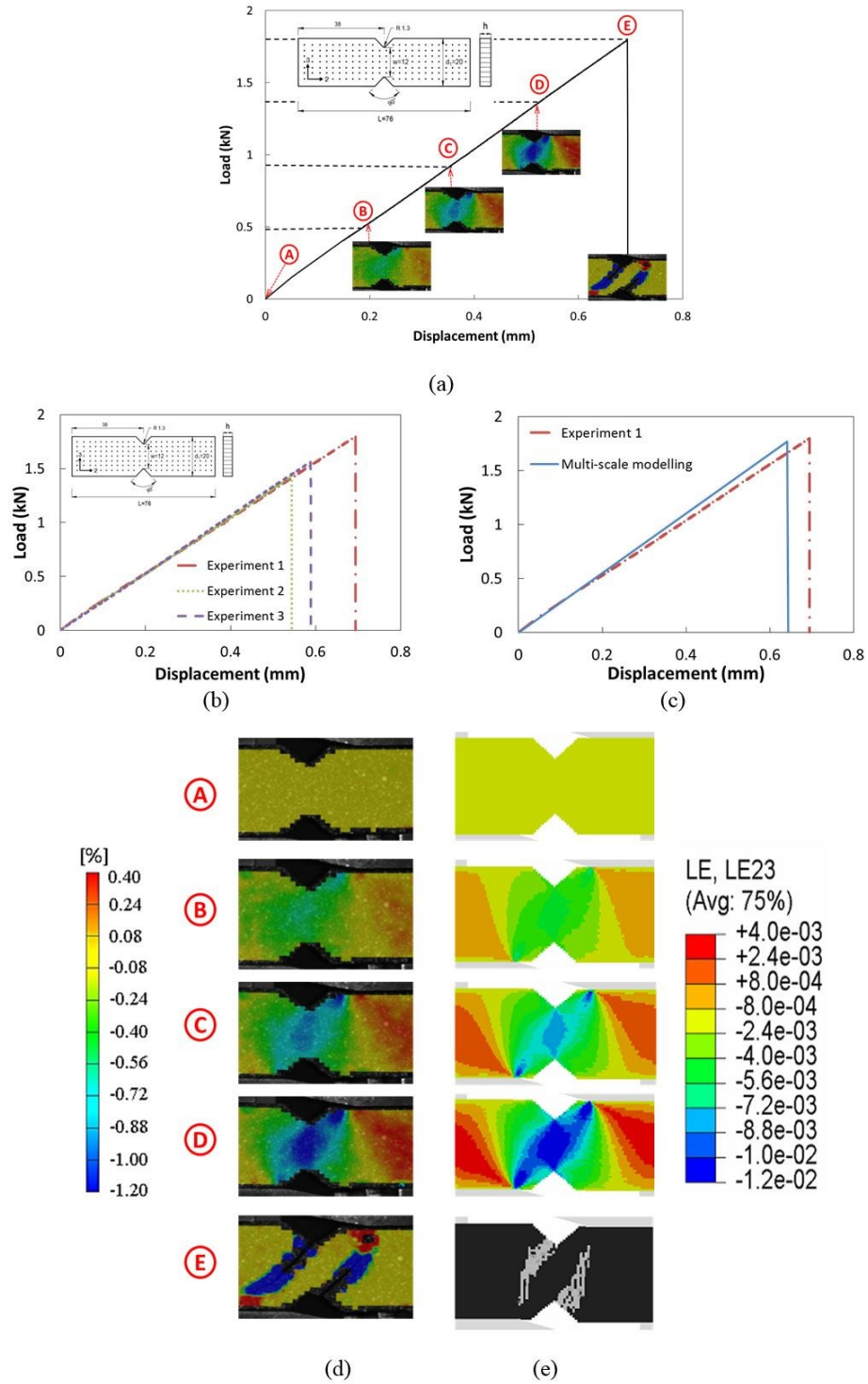


Figure 5-11. Comparison of experimental and simulated results for G23 shear test ($h=3\text{mm}$): (a) Load-displacement trace and failure mode; (b) Load versus displacement trace; (c) Comparison of experimental and simulated load-displacement traces; (d) Failure mode and γ_{23} from DIC; (e) Failure mode and γ_{23} from FEA.

5.4. Conclusions

The major findings of the shear testing research can be summarised as follows:

- A user-defined material subroutine for a multi-scale composite material failure model has been developed and was employed in the simulation of the Iosipescu shear test.
- By comparing with ply-by-ply modelling, the efficiency and accuracy of the multi-scale modelling approach have been verified.
- The model was successfully employed to predict the effect of fibre orientation and the effect of varying the thickness dimension in the Iosipescu shear test specimen.
- For the G13 specimens, the non-linear model accurately predicts the response of a structure subjected to shear load and the formation of delamination at the notch root and through the specimen thickness.
- The G23 Iosipescu test is well represented by this multi-scale modelling approach with the position and onset of the shear crack accurately reproduced.

6. Low velocity impact on the composite sandwich structure

6.1. Introduction

In this chapter, the main research objective was to determine the response of graded foam core composite sandwich structure under low velocity impact with different foam density orientations. The maximum impact velocity was 3.7 m s^{-1} at a drop height of 0.7 m and the impactor mass was 30 kg. The composite sandwich structures can sustain the threat of low velocity impact with rigid projectiles and compliant projectiles. These are, for example, a dropped tool. It is important to know the effect of layering and grading the foam cores between skins for the impact energy absorption ability. The grading of the foam core instead of one single layer foam core is one way to improve the impact energy absorption ability. In this Chapter, the effect of layering the foam core is the first topic to be investigated in order to verify the idea of graded foam core sandwich structure. After knowing the results of layering the foam core, the next step is to determine how it performs under low velocity with different density foam cores. This would give us a better understanding of the effect of different density foam core between composite skins. Then the use of different density and layering of the structure to find out the best way to improve the composite sandwich structure under low velocity impact can be determined.

As described in Chapter 3, the drop-weight tower can be employed to perform low velocity impact both with rigid and compliant impactors. The steel impactor is used for the rigid impact, whilst the HDPE impactor is used for compliant impact.

6.2. Experimental

6.2.1. Materials

In this research, the composite sandwich structure made by Glass Fibre Reinforced Polymers (GFRP) for the composite sandwich structure skins and polyvinyl chloride (PVC) for the foam cores. The Biaxial E-glass fibres of skin materials were placed in a quasi-isotropic layout [0/90/45/-45/90/0]_s. The fibre was made of the 0.601 kg m⁻² areal density +/- 45 Biaxial. The resin system used was Prime-20 LV with slow hardener and the front skin and the back skin consisted of identical layup and materials.

The core materials used in the present study were Ariex C70-series polyvinyl chloride (PVC) foam, which were manufactured by 3A Composites Company for aircraft and aerospace composite sandwich structures applications. The three types of Ariex C70-series foam that were used in this study were C70.55, C70.75 and C70.90. Table 6-1 lists the important material properties of the three foams from the manufacture's data. The cell structures for the three foams were very similar and the only difference appears in the cell wall thickness and node sizes, which accounted for the different densities of the foams.

Table 6-1. Material properties for PVC foam cores

	C70.55	C70.75	C70.90
Density (kg m ⁻³)	60	80	100
Compressive modulus (MPa)	69	97	125
Compressive strength (MPa)	0.9	1.3	1.9
Compressive fracture strain	0.7	0.7	0.7
Tensile modulus (MPa)	45	66	84
Tensile strength (MPa)	1.3	2	2.7
Work of fracture in tension (kJ m ⁻²)	0.26	0.44	0.62
Shear modulus (MPa)	22	30	38
Shear strength (MPa)	0.8	1.2	1.6
Shear fracture strain	0.16	0.23	0.27
Work of fracture in Shear (kJ m ⁻²)	6.48	12.6	18.4

Composite sandwich structure specimens were made employing Resin Infusion using a Flexible Tool (as described in Chapter 3). During the RIFT process, the sandwich specimens were infused under the same conditions, i.e. temperature process, humidity and vacuum pressure, with the same volume of resin. For the PVC foam core, each layer of foam was 5 mm thick except one arrangement with 15 mm thick foam. So for different arrangement specimens the core itself was 15 mm for three layers, whilst the composite skin thickness was 1.3 mm for each side.

The eight types of composite sandwich structure specimens with different core layer gradation were studied (shown in Table 6-2), the green colour foam represent 80 kg m^{-3} , the red colour foam is 100 kg m^{-3} and the yellow colour foam is 60 kg m^{-3} . The comparison of configurations M15 with MMM is to investigate the impact performance between one single layer of foam and three layers with same average core density, and the MMM configuration is the control specimen, whilst the group of LLL, MMM and HHH is aiming to see the different impact performance by different densities foam core. The group of MMM, LHM and HML is to compare the composite sandwich structure with stepwise graded foam core, whilst the last group of MMM, LHL and HLH is to check the low and high density foam core effect the impact performance by different average core densities.

Table 6-2. Configurations of composite sandwich structure specimens

Schematic	Code	Configurations	Average core density (kg m^{-3})
	M15	C80	80
	MMM	C80/C80/C80	80
	HHH	C100/C100/C100	100
	LLL	C60/C60/C60	60
	LMH	C60/C80/C100	80
	HML	C100/C80/C60	80
	LHL	C60/C100/C60	73.3
	HLH	C100/C60/C100	86.7

6.2.2. Instrumentation

As it shown in Figure 6-1, the drop-weight tower provides the low velocity impact on the composite sandwich structure specimens. The drop weight tower can provide up to 300 Joule impact energy by free body dropping, the weight is adjustable and the height can be changed easily to get the different impact energy. The load cell connects to the adjustable weight to obtain the force data, and the impactor driven at the load cell end. The real time load versus time trace was obtained from an oscilloscope. The fixture with specimen was fixed on the rigid ground to prevent the shock wave rebound.

6.2.3. High Speed Cameras and Digital Image Correlation (DIC) system

The high speed camera used in the drop weight tower is to track the speed of the impactor. The high speed camera shooting from the side as shown in Figure 6-1 to calibrate the impact speed and track the damage on the specimen surface. The high speed camera can be automatically triggered from the load cell signal. By matching up the results from both high speed camera and load cell, we can make sure each experiment performed in the same speed of impact as well as the impact energy.

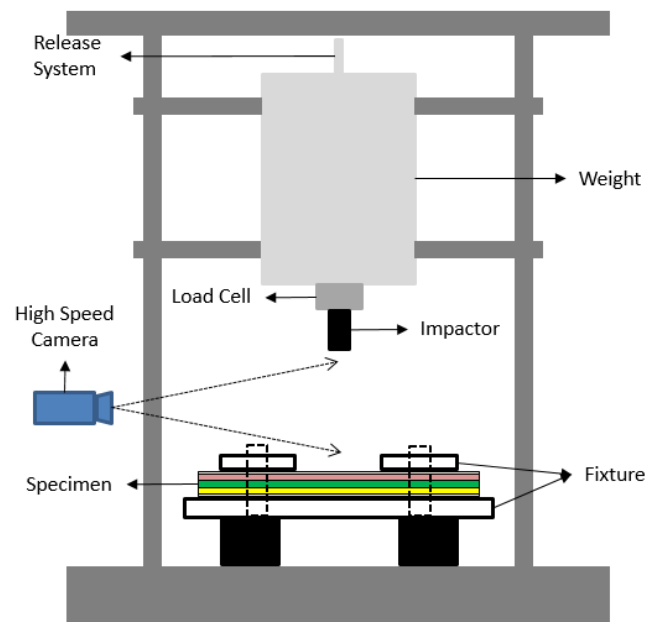


Figure 6-1. Experiment fixture drop weight tower.

6.2.4. Boundary condition and Projectiles

The fixture and impactor geometry were shown in Figure 6-2, as the specimen is 160 x 160 mm², and the fixture is 75 x 75 mm² whilst the projectile is 24.8 mm diameter with 40 mm length. The fixture made by mild steel and the sample bolted to keep the boundary condition stable. And each bot was screwed with 1 kN force by using the spanner with force measurement.

The impactor material is made by the High Density Polyethylene (HDPE) for compliant impact and Aluminium as the rigid impact for drop tower. Both impactors are cylinder shape with a flat – end nose as introduced in Chapter 3. The HDPE provides a soft edge (due to deformation). Whilst the Aluminium projectile provides a sharp edge (very little deformation of impactor). Both impactors have the same weight, the HPDE is the solid not hollow whilst the steel projectile has the same length with HDPE projectile but the inside is drilled out to reduce mass to approximately 18.2 g. This means the mass of both projectiles is the same at 18.2 g.

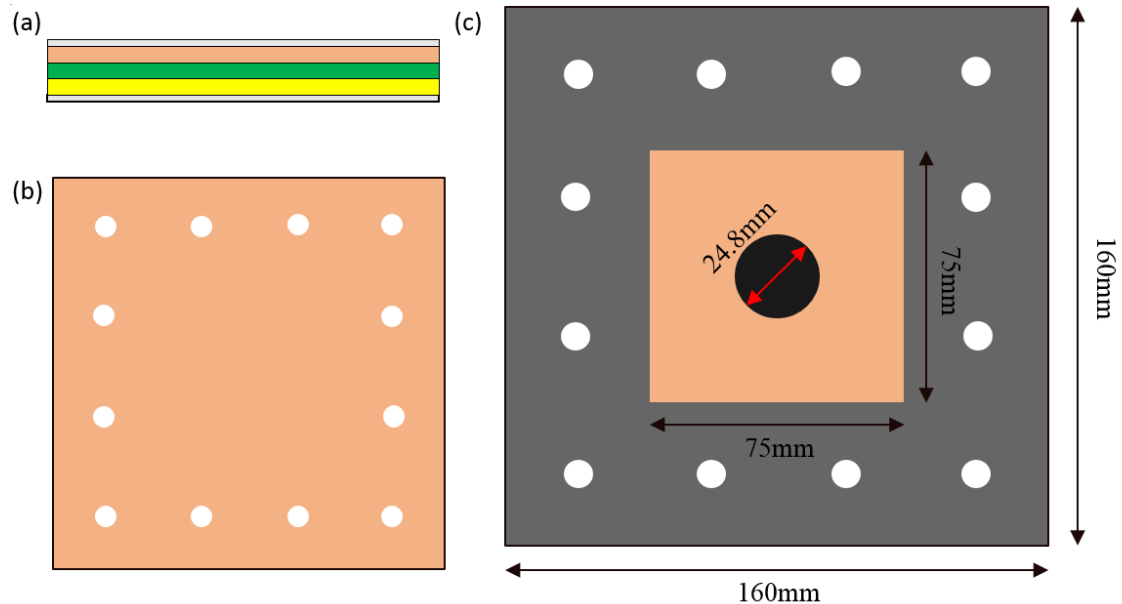


Figure 6-2. Test specimen: (a) Section view of sandwich structure specimen; (b) top view of sandwich structure specimen; (c) top view of sandwich structure specimen with fixture and impactor

6.3. Results and discussion

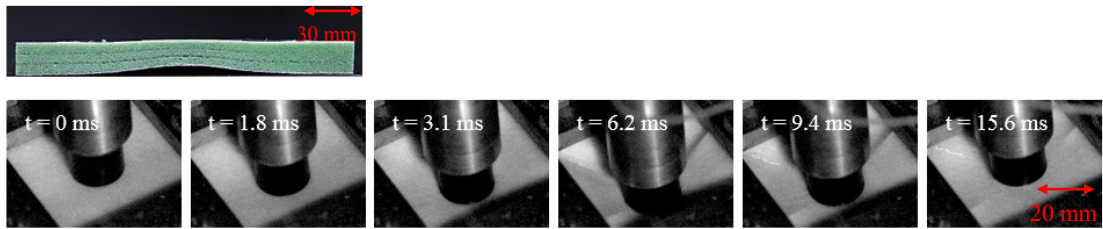
The low velocity impact results were presented in this research. Firstly, the rigid and compliant impact on the composite sandwich structures were investigated. Secondly, the effect of design parameters including layering the foam core, different densities of foam core and grading the foam core were studied. Lastly, the effect of the different density foam cores on the impact side was investigated for the graded foam core composite sandwich structure.

6.3.1. Rigid and compliant impactor

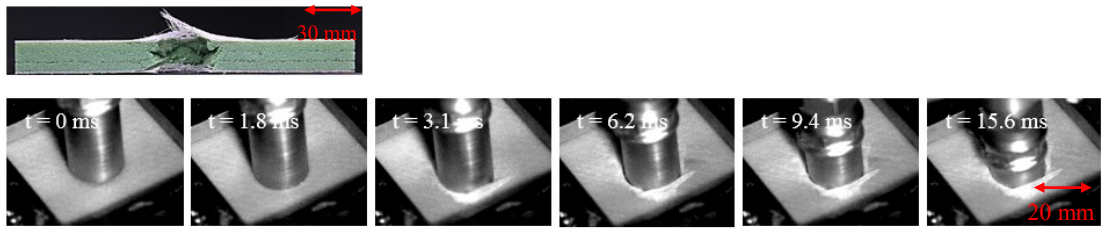
In the drop weight-weight tower test, the impactor of Aluminium and HDPE perform differently because of deformation of the edges of the impactor. The Figure 6-3 (a) and (b) show the compliant and rigid impactor deformation during the drop weight tower impact process on the MMM configuration specimen. The compliant impactor (Figure 6-3 (a)) rebound back after some fibre break on the specimen surface whilst the impact time is nearly 6.2 ms. For the rigid impact (Figure 6-3 (b)), the impactor penetrated through the specimen surface at approach 3.1 ms whilst not much fibre break damage on the specimen surface. In Figure 6-3 (c), for rigid impact, the stiffness is the same with the compliant impact until the peak load reached to break the front composite skin, then the load drop off and compressed the foam. When the impactor reached the back composite skin, the stiffness of specimen will arrive a higher peak load and go through the specimen by breaking the back face composite skin.

However, for the compliant, it has the same structural stiffness with rigid impact, but the load will drop off after the peak load. The small load vibration near the peak load is because of the fibre break on the front face composite skin. The effect of the compliant impactor and rigid impactor has shown in Figure 6-3. The sharp edge of the rigid impactor helps to cut both front and back face composite skins, whilst the soft edge of the compliant impactor helps to deform of the impactor and disperses the load on the specimen surface.

(a) Compliant (HDPE) impactor



(b) Rigid (steel) impactor



(c)

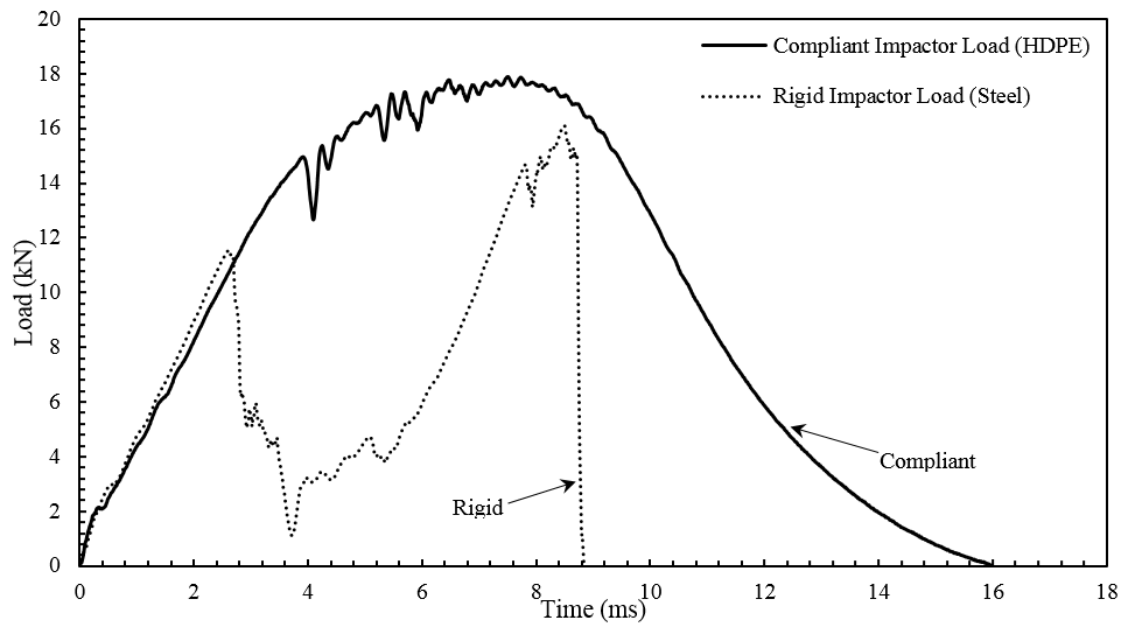


Figure 6-3. (a) Compliant (HDPE) impactor; (b) Rigid (steel) impactor deformation and (c) load and displacement versus time trace during the drop-weight tower impact for MMM (Three layers of 5 mm thick PVC foam with 80 kg m^{-3} density) with 205 Joules impact energy (Impactor mass = 30 kg; impact velocity = 3.7 m s^{-1})

6.3.1.1. Effect of layering the foam cores

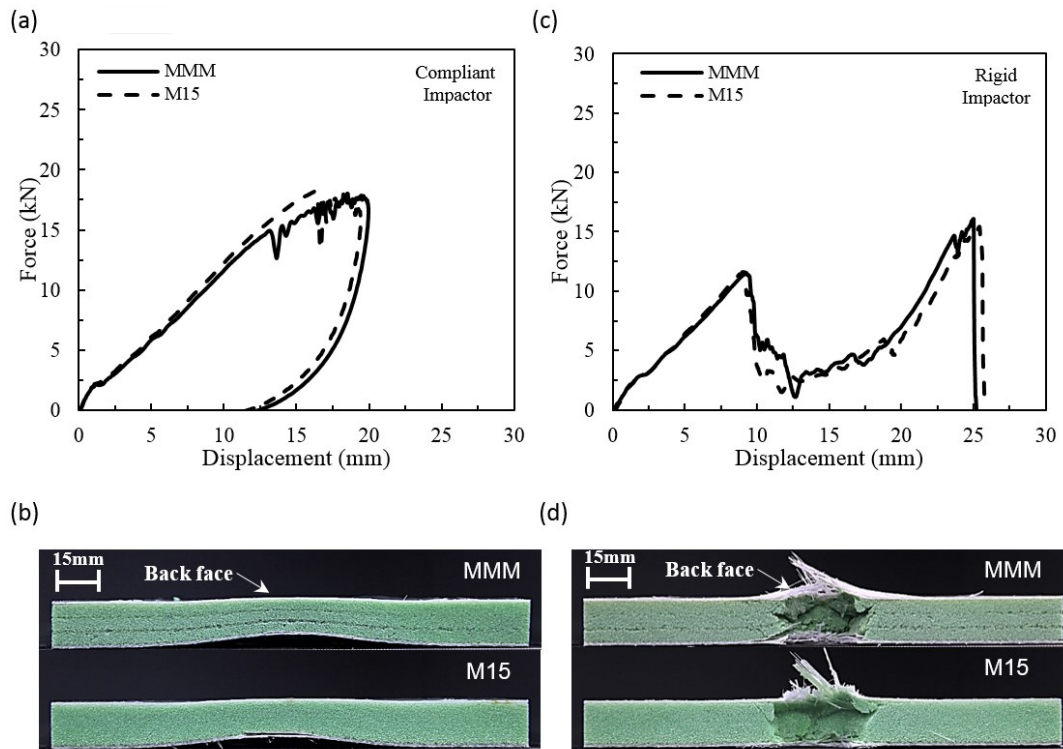


Figure 6-4. Force versus displacement trace and sectional view for MMM (Three layers of 5 mm thick PVC foam with 80 kg m^{-3} density) and M15 (Single layer of 15 mm thick PVC foam with 80 kg m^{-3} density) under compliant (HDPE) impactor (a) & (b) and rigid (steel) impactor (c) & (d) with the same impact energy 205 Joules (Impactor mass = 30 kg; impact velocity = 3.7 m s^{-1})

Figure 6-4 shows the comparison between the single layer of 15 mm thick 80 kg m^{-3} PVC foam and the three layers of 5 mm thick 80 kg m^{-3} PVC foam under compliant impact (a) & (b) and rigid impact (c) & (d). The two tests were impacted by the impactor mass 30 kg and velocity 3.7 m s^{-1} . From the force versus displacement trace for the compliant impact (Figure 6-4 (a)), it shows that the M15 and MMM configurations have the same structural stiffness from the initial impact up to peak load, and the same energy absorption for both configurations. From the sectional view (Figure 6-4 (b)), the impactor rebounded back with no damage on the front composite skins. However, from the rigid impact (c) & (d), the impactor broke the front composite skin for both configurations with the same stiffness and the same peak load. After the front skin break, the impactor continued to compress the foam and break the back face skin until the final failure. In the force versus

displacement trace (Figure 6-4 (c)), it also shows that the same amount of energy absorbed by the composite sandwich structures for both configurations.

For this comparison, the single layer foam core and the three layers' foam core structures performed the same energy absorption under the low velocity impact by rigid and complaint impact. However, after the impact, the specimen under complaint impact deformed without penetrate through, whilst the rigid impactor broke through the whole specimen.

6.3.1.2. Effect of different foam core densities

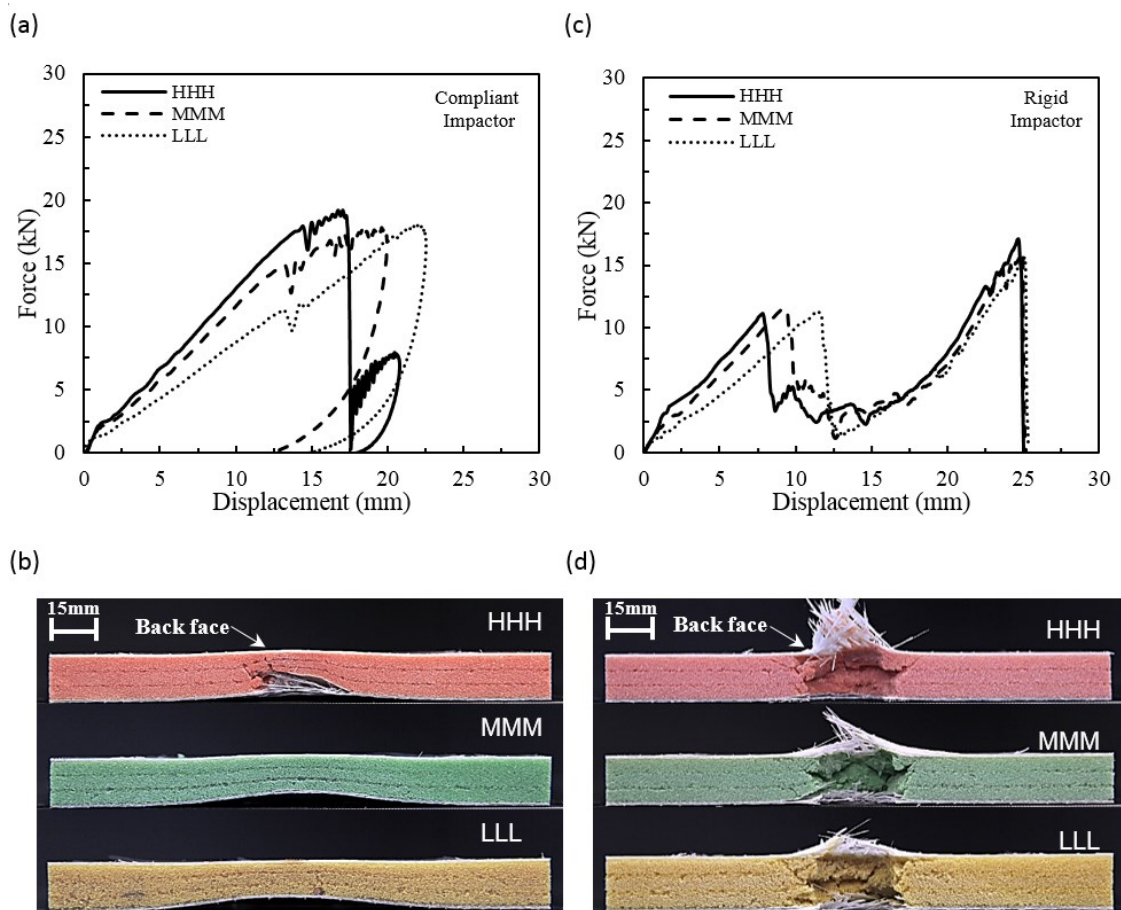


Figure 6-5. Force versus displacement trace and sectional view for HHH (Three layers of 5 mm thick PVC foam with 100 kg m^{-3} density), MMM (Three layers of 5 mm thick PVC foam with 80 kg m^{-3} density) and LLL (Three layers of 5 mm thick PVC foam with 60 kg m^{-3} density) under compliant (HDPE) impactor (a) & (b) and rigid (steel) impactor (c) & (d) with the same impact energy 205 Joules (Impactor mass = 30 kg; impact velocity = 3.7 m s^{-1})

After indicating the difference between the single layer and the multiple layer composite sandwich structures, the difference of the foam densities needs to be taken into account by repeating the drop tower under the same impact velocity and impact energy. From Figure 6-5 (a) and (b), the compliant impact was performed on the three different densities of three layers of foam composite sandwich structures. In the force versus displacement trace (Figure 6-5 (a)), it shows that the higher density foam core with 100 kg m^{-3} provided the higher stiffness of the structure, whilst the lower density foam core has a lower structure stiffness. However, for the higher density foam core with 100 kg m^{-3} , the load dropped to zero after it reached the peak load, whilst the other two lower density foams did not absorb the impact energy. This can also be seen in the sectional view of the three configurations (Figure 6-5 (b)), the front two layers of the higher density foam were compressed and the shear failure was appeared on the second layer of the foam core, whilst the other lower density foam cores only have the compressed damage without break the front composite skins.

For the rigid impact on the three different density foam cores (Figure 6-5 (c) & (d)), the three configurations perform that the higher density provide the higher stiffness until the break point in the load versus displacement trace (Figure 6-5 (c)). After the first layer break, the load drops quickly until the impactor reaches the back face skin to the final failure. In the sectional view of the structures (Figure 6-5 (d)), the damage on the three foam core sandwich structures are the same with both of the composite skins broke and the foam cores were penetrated with the shear failure through the three foam layers.

For the comparison, the higher density foam core promotes the damage on the front skin for both rigid and compliant impact, whilst the lower density foam can absorb more impact energy for the structure under both impact.

6.3.1.3. Effect of grading the foam cores

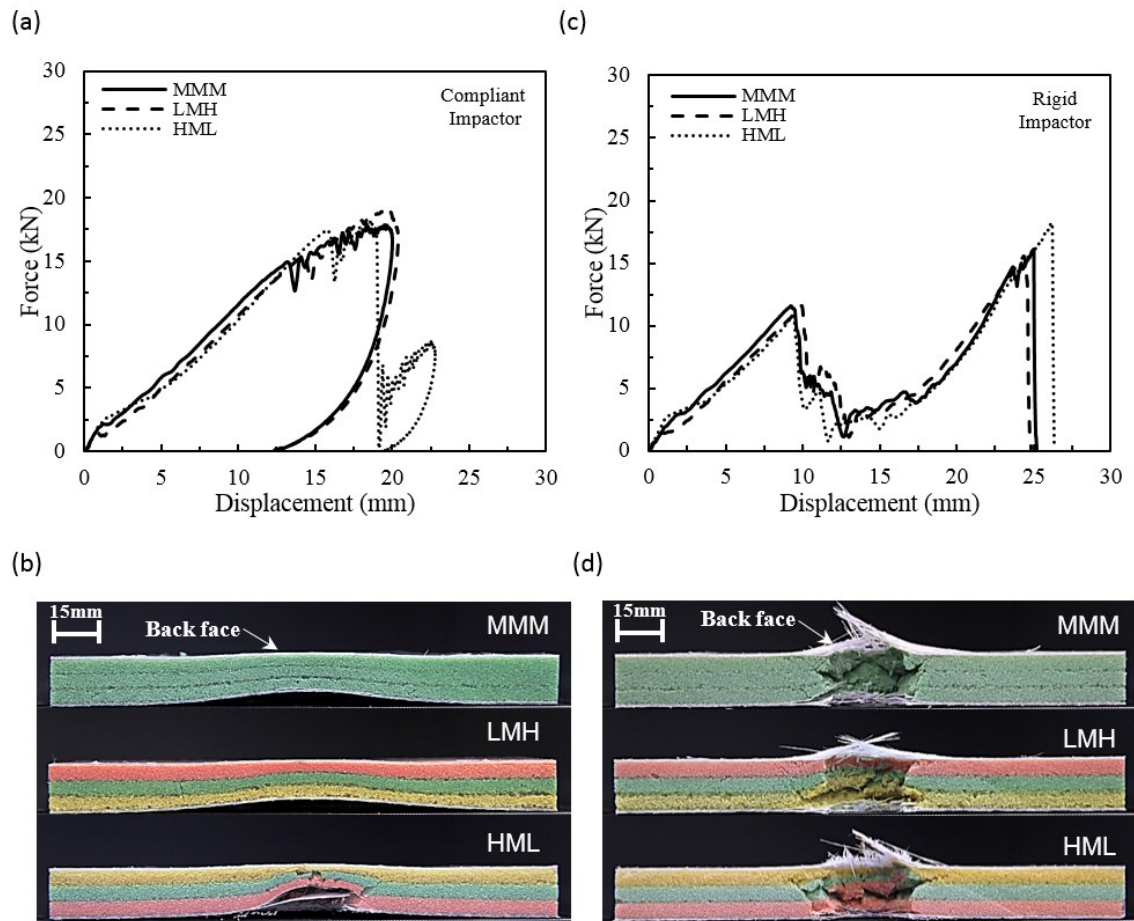


Figure 6-6. Force versus displacement trace and sectional view for MMM (Three layers of 5 mm thick PVC foam with 80 kg m^{-3} density), LMH (Three layers of 5 mm thick PVC foam with $60/80/100 \text{ kg m}^{-3}$ density) and HML (Three layers of 5 mm thick PVC foam with $100/80/60 \text{ kg m}^{-3}$ density) under compliant (HDPE) impactor (a) & (b) and rigid (steel) impactor (c) & (d) with the same impact energy 205 Joules (Impactor mass = 30 kg; impact velocity = 3.7 m s^{-1})

The effect of layering the foam core and the effect of the different densities for the composite sandwich structure has been investigated. It is become very important to find the effect of grading the foam cores by different densities under low velocity impact. The orientations of MMM, LMH and HML are the same average density of foam core with average 80 kg m^{-3} .

The Figure 6-6 (a) shows that the three configurations of the composite sandwich structures have the same stiffness before it reaches the peak load, and the peak load are the same for the three different foam core density orientations, but for the HML configuration which the higher density foam core on the impact side, the load dropped to zero without return back. This can be seen in the sectional view (Figure 6-6 (b)), for the HML configuration, the front face composite skin on the impact side was damaged by the impactor and it continued compressed the foam cores and generated the shear damage on the three layers' foam cores. However, it did not penetrate through the specimen. It also shows in the Figure 6-6 (b) that the compression damage on LMH configuration is smaller than MMM configuration as the deformation is much smaller for LMH. But for the HML, the shear damage was produced on the high density foam core, and the compression damage on the medium and low density foam core.

The results of the rigid impact on the composite sandwich structure with MMM, LMH and HML configurations were shown in Figure 6-6 (c) and (d). From figure 6 (c), it shows that they have the same performance as described in the force and displacement trace, as the three configurations performed the same property of the stiffness and the peak load whilst the damage on both front and back composite skins. However, in Figure 6-6 (d), the damage in the foam core shows the differences for the three configurations. In the MMM configuration, the shear damage through the three layers, whilst in the LMH configuration, the compress damage happens on the low density foam core and transfer the shear damage to the medium and high density foam layers. For the HML configuration, the shear damage produced on the high density layer and transfer the shear damage into medium density and compression damage on the low density foam core.

From Figure 6-6 (a) and (b) it can be seen that when the high density foam layer on the impact side it will promote the damage on the front skin, and the shear damage in the high density foam core. When the low density on the impact side, the low density foam helps to absorb the impact energy and reduce the damage on the front skin. For the rigid impact (Figure 6-6 (c) and (d)). Although, the energy absorption is nearly the same for the three configurations, but the damage inside the structure does show that when the low density foam core on the impact side, the compressed damage is generated instead of shear failure.

6.3.2. Effect of the foam core density on the front layer of sandwich structure

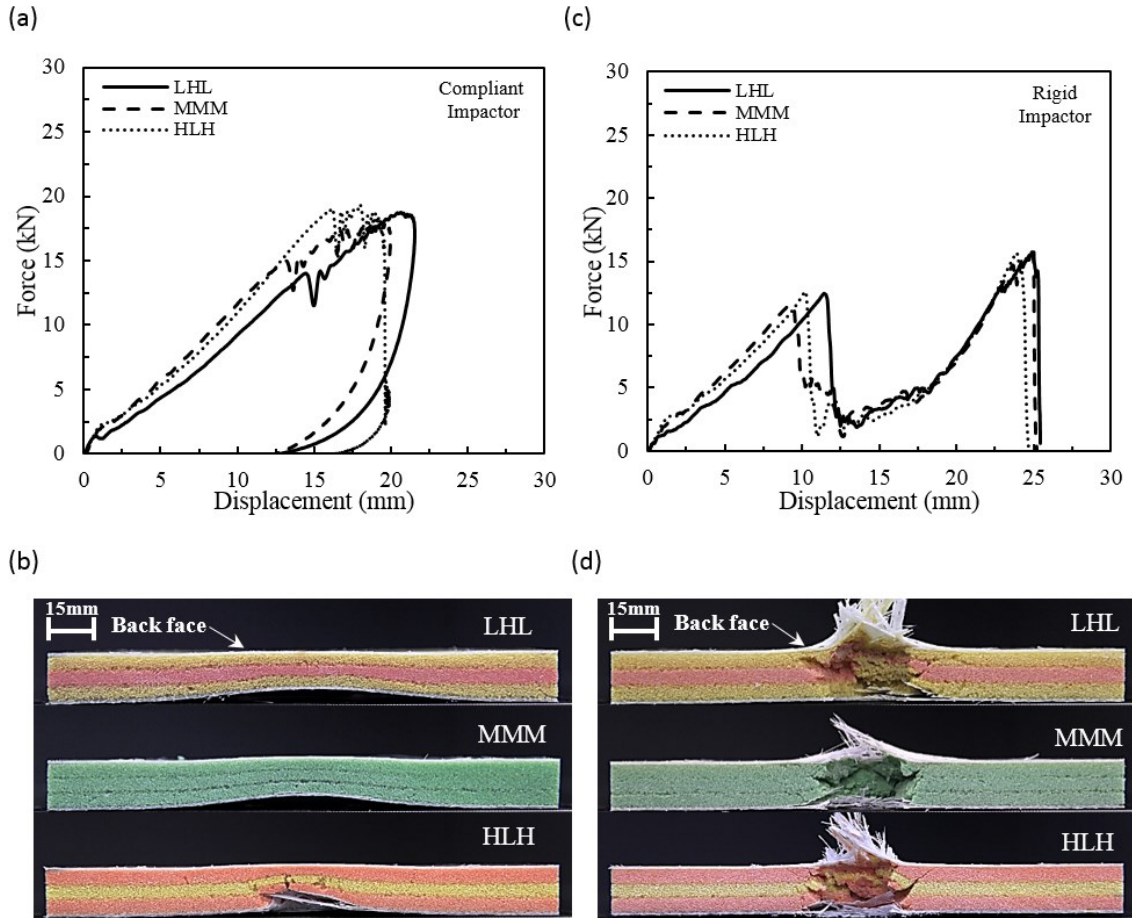


Figure 6-7. Force versus displacement trace and sectional view for LHL (Three layers of 5 mm thick PVC foam with 60/100/60 kg m⁻³ density), MMM (Three layers of 5 mm thick PVC foam with 80 kg m⁻³ density) and HLH (Three layers of 5 mm thick PVC foam with 100/60/100 kg m⁻³ density) under compliant (HDPE) impactor (a) & (b) and rigid (steel) impactor (c) & (d) with the same impact energy 205 Joules (Impactor mass = 30 kg; impact velocity = 3.7 m s⁻¹)

In order to confirm the findings that the high density can promote the damage on the front composite skin, the configurations of LHL and HLH were tested under the same low velocity impact energy by the rigid and compliant impact. In the load versus displacement trace for the compliant impact (Figure 6-7 (a)), it has a lower stiffness for the LHL which the lower density foam on the impact face, whilst the HLH has a higher peak load, but the contact force on the HLH configuration dropped whilst the LHL and MMM did not. This could be seen from Figure 6-7 (b) that the front skin of HLH broke, but the other

configurations only compressed the foam core and bend the front face skin without damaged the foam core. In Figure 6-7 (c), the load and displacement trace for the rigid impact shows the similar trend in the compliant impact that the stiffness of the structure for LHL is lower the others, but the stiffness for breaking the back face skin are the same the three configurations. The energy absorption of the LHL configuration is larger than the other two configurations. In Figure 6-7 (d), the rigid impactor produced the shear damage in the high density foam core and transferred the shear damage to the back face skin. But for the LHL configuration, the compression damage in the low density foam and the shear damage initial from the high density foam in the middle layer.

6.4. Conclusions

The main findings in the low velocity response of composite sandwich structures can be summarized below:

- The soft edge of the compliant impactor helps to distribute the impact load on the composite sandwich structure, which reduces the damage on the front composite skin and the damage on the foam core layers.
- The layering of the foam cores between the composite sandwich structure skins does not have a major effect on the energy absorption for the low velocity rigid and compliant impact. Moreover, the failure mechanism is the same for the single and multiply layers of the composite sandwich structures.
- The higher density of the foam core provided the higher overall stiffness of the structure. However, the shear failure would damage the foam core and transfer the load to the back face composite skin.
- Graded foam core composite sandwich structure has the benefit that when the low density foam core is on the impact side, there is a reduction in the damage on the front skin and shear failure is reduced and the back face is intact.

7. High velocity impact on the composite sandwich structure

7.1. Introduction

The high velocity impact experiments were performed on the composite sandwich structures in this Chapter. The impact velocity range was from 178 m s^{-1} to 203 m s^{-1} with the mass of projectile being 18.2 g. Forward facing components of wings, nose-cone, tail-section and aero-engine parts are examples where sandwich structures face the threat of high-speed localized impact. As shown in Chapter 6, there are benefits of a graded foam core composite sandwich structure in low velocity impact. It is also worth investigating if the graded foam core also has the advantages under the various high velocity impact for both rigid and compliant impact.

The gas gun facility was used to provide the various velocities for both the compliant and rigid projectiles. The failure mechanism can be compared by sectioning the tested specimen. By introducing the high speed DIC system, as described in Chapter 3, the full field strain and displacement on the back skin of the composite sandwich structure can be compared to identified the differences between the configurations. Moreover, the energy absorption of the different configurations can be quantified by knowing the incident speed and the residual speed of the projectile.

7.2. Experimental

7.2.1. Materials

The specimens used in this chapter is the same material properties with the specimens used in the low velocity impact (Table 6-1 and Table 6-2). The manufacturing process of the specimen is also the same with the ones used in the low velocity impact experiment. In order to compare the low and high speed impact on the specimen, the same specimen geometry and the same fixture boundary condition were used in the high velocity impact as shown in Figure 6-2.

7.2.2. High velocity impact experiment design

The driving force of the gas gun is generated by compressed air. By using the pressure, the projectiles travel through the 3 m length barrel. When the projectile cut the two Infra-Red (IR) sensor at the end of the barrel with 50 mm distance, the oscilloscope can receive the signals from the two sensors, the velocity of the projectile can be obtained by knowing the time difference of the two sensors. The specimen fixture was placed in the fully covered chamber with thick metal and polymer shield.

In the high velocity impact test, the two synchronised high speed cameras were placed at the back of the chamber for the Three Dimensional DIC method for the lower velocity which did not penetrate through the specimen as shown in Figure 7-1. The distance between two high speed cameras is 410 mm and the distance from centre of the target to the camera is 925 mm. This gives approximately 25° between the two cameras which is the recommended angle to do stereo vision measurements. When the projectile penetrates the specimen under higher velocity, the high speed camera placed at the side of the chamber as shown in Figure 7-2. One of the high speed camera placed before the specimen to calibrate the incident speed and the other high speed camera placed after the specimen to track the residual speed.

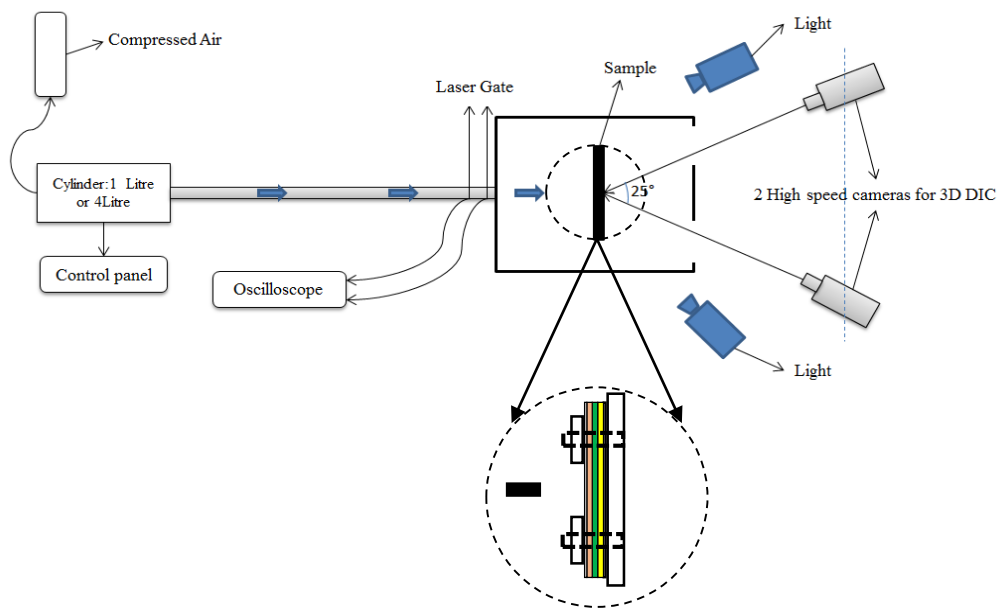


Figure 7-1. Experiment fixture: gas gun facility.

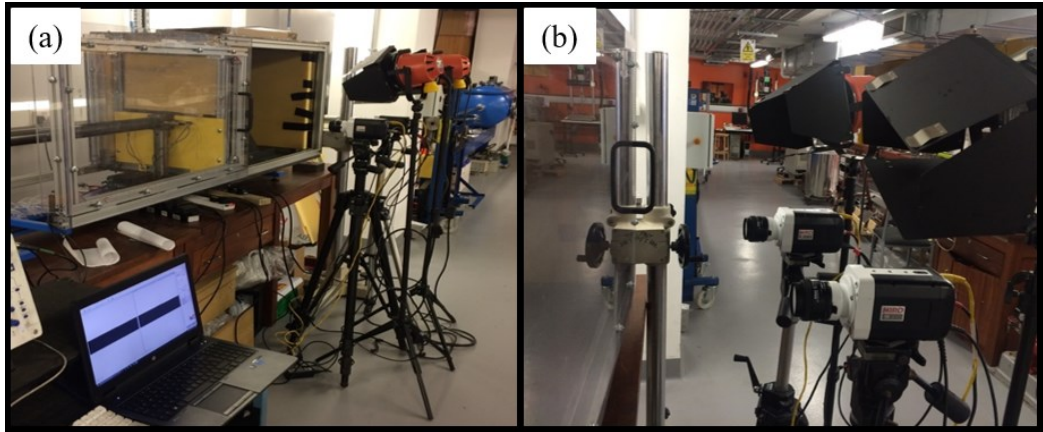


Figure 7-2. The high speed cameras set up for the penetration experiment.

The DIC speckle pattern of the specimen is shown in Figure 7-3 (a). In order to get enough pictures during the high velocity impact experiment, the resolution of the cameras reduced to 340 x 340 with 20,000 frame per second so the size of the speckle should increase to approach 1 mm diameter as introduced in Chapter 3. In Figure 7-3 (b), the thick polymer at the end of the barrel is to protect the equipment in case the projectile rebound back. The front and back of the specimen are shown in Figure 7-3 (c) and (d).

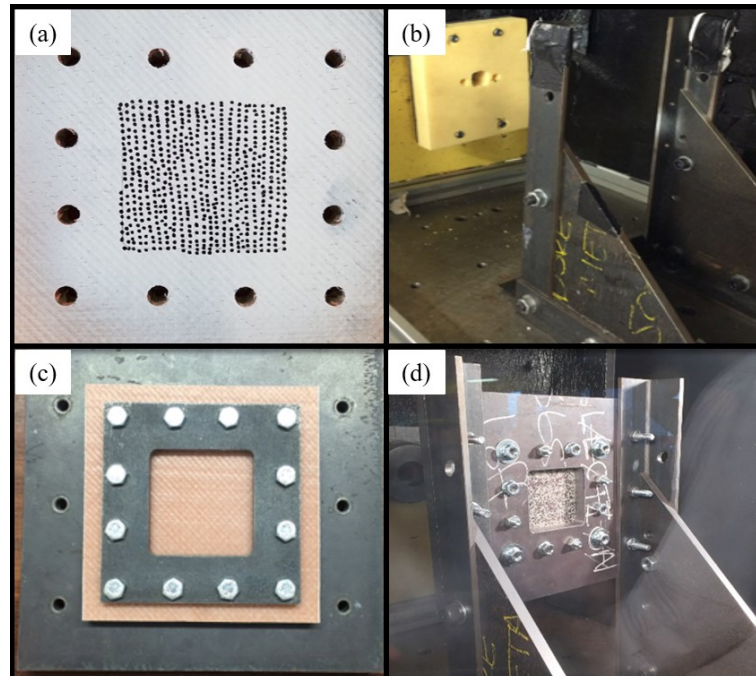


Figure 7-3. The specimen speckle pattern (a) and fixture setup for high velocity impact experiment (b) & (c) & (d).

7.3. Results and discussion

The high velocity impact generated by the gas gun facility, as introduced above, the strain of back face skin can be measured by using the DIC system, before the DIC result can be trusted, the results from DIC system need to be checked by compared the results from the strain gauges. The strain gauges placed on one side of the sample, by compared with speckle on the symmetry side.

The in-plane strain (strain in y direction) calculated by DIC is compared in Figure 7-4 with the results of the strain gauges mounted at the back surface of the plate for the specimen. The top surface of the strain gauges was painted and speckled; therefore, the deformation of the gauge could be monitored during the impact event. Overall, there is a good agreement between the two methods.

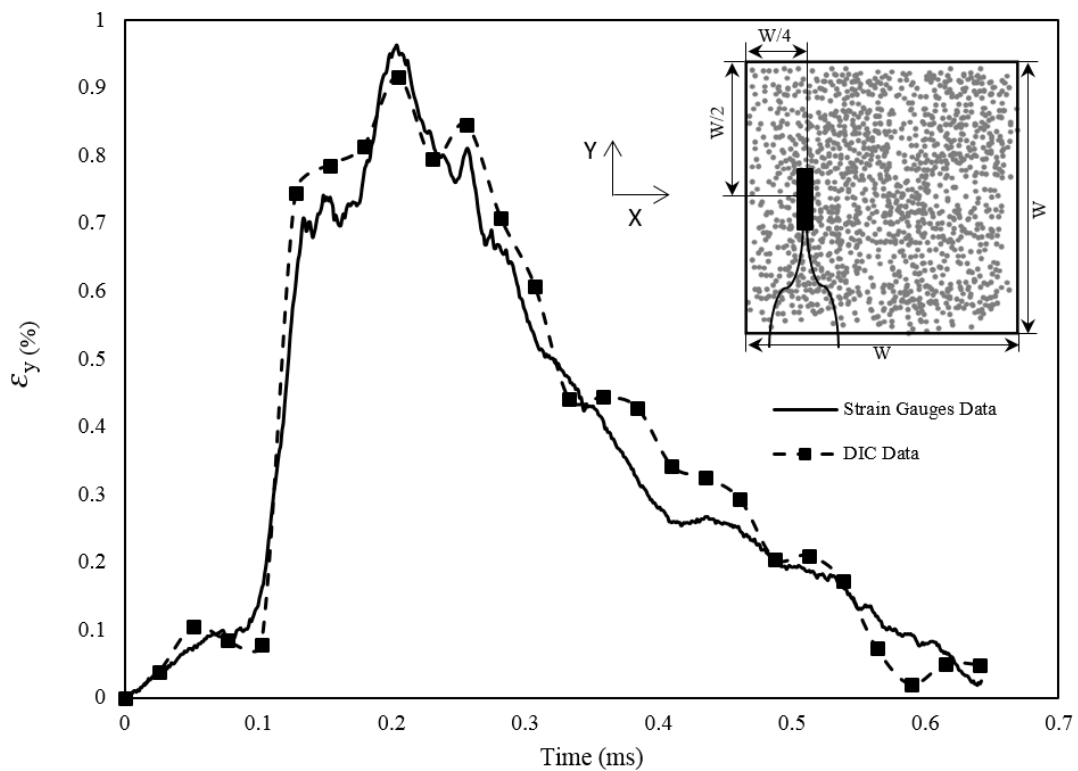
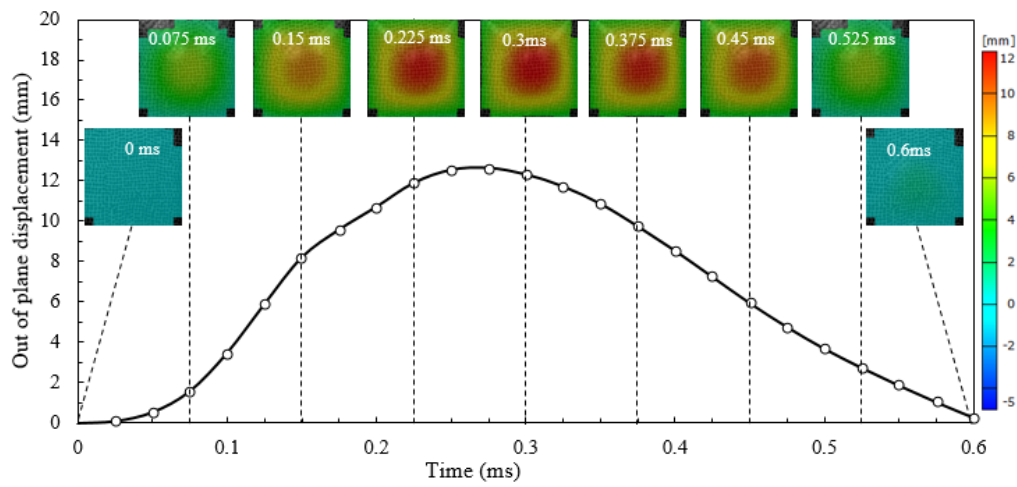


Figure 7-4. Comparison of principle major strain on y direction (ϵ_y , %) between strain gauges and DIC result under compliant (HDPE) gas gun impact for LLL (Three layers of 5 mm thick PVC foam with 60 kg m^{-3} density) under 285 Joules impact energy. (Impactor mass = 18.2 g; impact velocity = 178 m s^{-1})

As it is shown in Figure 7-5 (a) and (b), the contour plots of the out-of-plane displacement and principal major strain on the back face skin of composite sandwich structure LMH under complaint impact with 481 Joules. In Figure 7-5 (a), the time versus displacement trace indicates the middle point on the back face of the LMH composite sandwich structure. The contour plots reflect how the structure impacted on the out-of-plane direction whilst in Figure 7-5 (b), the contour plots show the major strain on the out-of-plane direction.

(a) Out of plane displacement



(b) Principal major strain

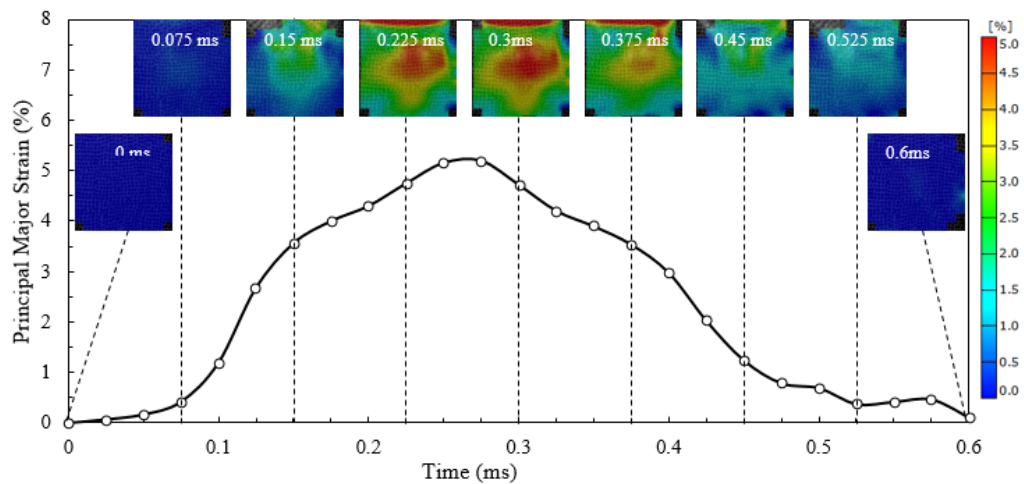


Figure 7-5. 3D DIC results: (a) contour plots of out of plane displacement (U_z , mm) and (b) contour plots of principal major strain ($\epsilon_{\max}$), during the complaint (HDPE) gas gun impact for LMH (Three layers of 5 mm thick PVC foam with 60/80/100 kg m⁻³ density) with 481 Joules impact energy (Impactor mass = 18.2 g; impact velocity = 230 m s⁻¹).

Figure 7-6 shows the displacement profile on back face skin in the centre section, from this figure, it can be seen that the specimen was moving out of the plane direction during the impact process. Moreover, we can see how it perform during the impact process, which could help to understand the movement of the structures.

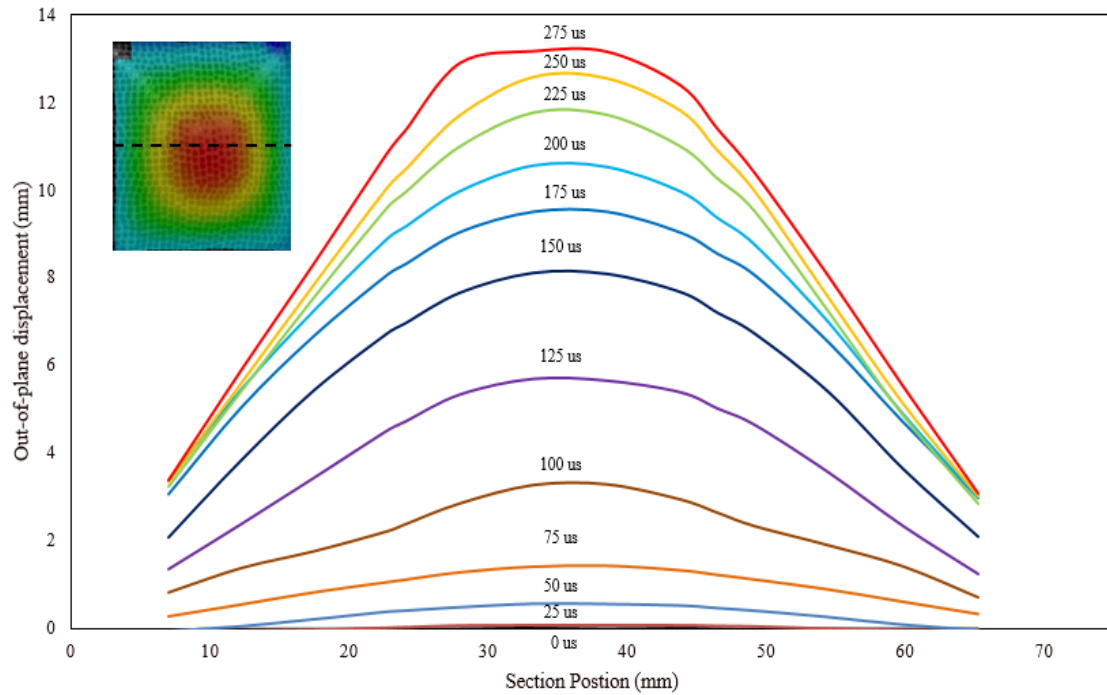


Figure 7-6. Out-of-plane displacement profile along the section position during the compliant (HDPE) gas gun impact for LMH (Three layers of 5 mm thick PVC foam with 60/80/100 kg m⁻³ density) with 481 Joules impact energy (Impactor mass = 18.2 g; impact velocity = 230 m s⁻¹).

7.3.1. Effect of layering the foam cores under compliant impact

In Figure 7-7, the section view of MMM and M15 under different velocities of compliant impact shows that the damage in the foam core was increased by increasing the impact velocity, and it shows the same compression and shear damage in both the MMM and M15 configurations. And the front composite skin break under the same velocity impact with 215 m s⁻¹ and 230 m s⁻¹. So the layering of the foam core in the composite sandwich structure does not affect the performance for absorbing the impact energy.

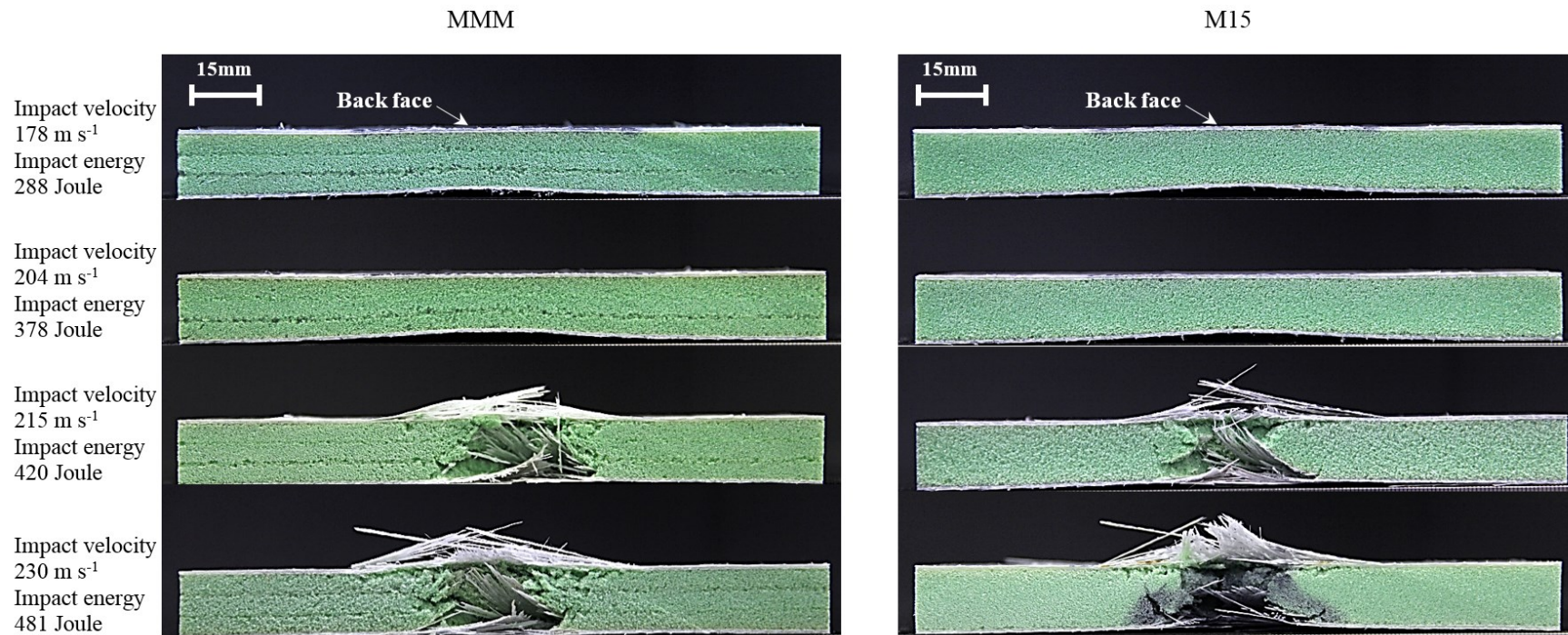


Figure 7-7. Sectional view for MMM (Three layers of 5 mm thick PVC foam with 80 kg m⁻³ density) and M15 (Single layer of 15 mm thick PVC foam with 80 kg m⁻³ density) of compliant (HDPE) gas gun impact under different velocities.

7.3.2. Effect of different foam core densities under compliant impact

As the same as the low velocity impact experiments, the differences of impact performance in various densities of foam core need to be investigated under compliant impact with HDPE projectiles. In Figure 7-8, it shows the sectional view of three configurations of LLL, MMM and HHH with the impacted specimens. The velocity of the impact experiments ranged from 178 m s^{-1} to 230 m s^{-1} with the impact energy increase from 288 Joule to 481 Joule. The visualized damage on those three configurations of composite sandwich structures increased when the impact velocity increase.

For the 178 m s^{-1} velocity with 288 Joule energy impact, there is a bending deformation at the edge of the boundary condition on the LLL configuration, and the front and back composite skins attached on the sandwich structure. Moreover, the MMM and HHH configurations got indentation damages on the front composite skins, whilst the back skins of both MMM and HHH configurations remained flat shape without any damage on the foam cores.

When the impact velocity increased to 204 m s^{-1} with 378 Joule impact energy, the low density foam core of LLL configuration started to compressed and there was a small compressed damage on the foam core around the impact area, whilst the indentation damage on the front skin of MMM configuration expand. However, as it is shown under the 204 m s^{-1} impact, the front composite skin of HHH configuration broke and the high density foam core started to have shear damage, and the shear damage was transfer through the three layers of the high density foam core to the back face composite skin. Although, the back skin was still attached on the sandwich structure, there is a delamination damage between the foam core and the back composite skin.

For the 215 m s^{-1} impact experiment, the front composite skin of LLL configuration broke and the compressed damage has appeared in front two layer of the low density foam core. But the back composite skin was kept the flat shape without any damage. For the MMM configuration under 215 m s^{-1} impact, the front skin broke, and the shear damage happened through the three layers of medium density foam core. But the back skin was still attached to hold the sandwich structure with some fibre break. However, the

projectile penetrated through the HHH configuration sandwich structure with both of the composite skins damaged and the large amount of shear damage appeared on all of the three layers of high density foam cores.

In the highest velocity impact experiment with 230 m s^{-1} impact velocity and 481 Joule impact energy. Both of the composite skins broke of LLL configuration, and the compressed and shear damage on the foam cores. But the projectile did not penetrate through the sandwich structure and the damage on the back skin of LLL configuration is much smaller than the MMM and HHH configurations. For the MMM and HHH configurations, the projectiles penetrated through the sandwich structures and the damage on the foam cores for both configurations are the same.

In general, the LLL configuration of composite sandwich structure performed the best as the it was not penetrated through for all the different velocity impact, whilst the MMM configuration penetrate at 230 m s^{-1} and the HHH configuration penetrate at 215 m s^{-1} . Furthermore, the HHH configuration composite sandwich structure performed the worst among the three configurations. Because the front skin of the HHH configuration broke at the lowest impact velocity of 205 m s^{-1} with 378 Joule impact energy.

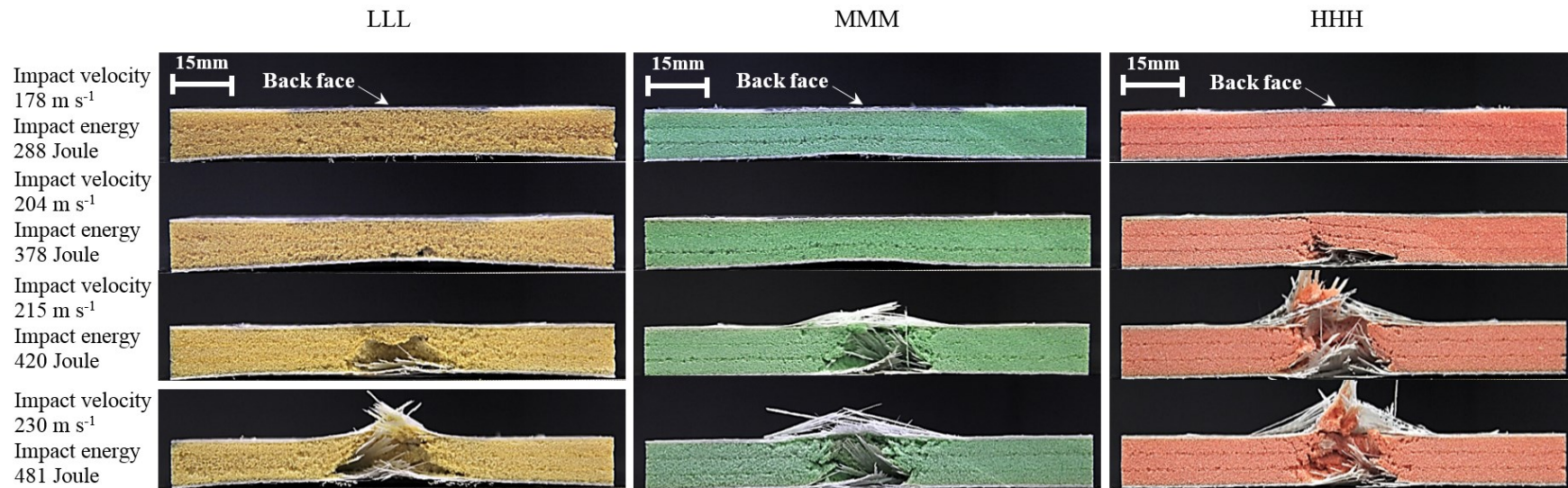


Figure 7-8. Sectional view for LLL (Three layers of 5 mm thick PVC foam with 60 kg m⁻³ density), MMM (Three layers of 5 mm thick PVC foam with 80 kg m⁻³ density) and HHH (Three layers of 5 mm thick PVC foam with 100 kg m⁻³ density) of compliant (HDPE) gas gun impact under different velocities.

7.3.3. Effect of grading the foam cores under compliant impact

The effect of layering foam core and different density were investigated in the previous experiments, the orientation of different foam density in the composite sandwich structure should discuss to improve the impact performance.

In Figure 7-9, the sectional view of the damaged sandwich structure with LMH, MMM and HML configurations were shown. As the impact velocity and the impact energy increased for those three configurations, the damage was uncreased, but they showed very different performance. In this part, the damage on LMH, MMM and HML configurations were compared.

For the 178 m s^{-1} velocity with 288 Joule energy impact, the LMH configuration had a compressed damage on the low density foam core, and the delamination appeared under the impact area. But the front and back face skin were still attached on the sandwich structure without any damage. However, the indentation damage was shown on the MMM and HML configurations.

When the impact velocity increased to 204 m s^{-1} , the compressed damage on the low density foam core of LMH configuration was expanded and the delamination between the foam and composite skin increased to a bigger area near the boundary of the experiment fixture. Whilst, the MMM configuration got the indentation on the front skin and no damage on foam cores and back face skin. However, as it is shown in Figure, the front skin of HML configuration had already got fibre break damage, whilst the high density foam core got shear damage and transferred to the other foam cores.

In the compliant impact experiment with 215 m s^{-1} , the compressed damage on both low and medium density foam core of LMH configuration increased, and the delamination between front composite layer and low density foam core increased too. But there was no damage on the high density foam core and the back skin of the sandwich structure was still attached with some fibre break. The damage on the MMM configuration was described above as the MMM configuration is the reference orientation for the comparison. However, for the HML configuration, the HDPE projectile penetrated through the sandwich structure with both of the composite skins broke. Furthermore, the

shear failure appeared on the high density foam core whilst the compressed damage on the low density foam core and delaminated from the back skin.

For the 230 m s^{-1} impact velocity and 481 Joule impact energy experiment. The back composite skin of LMH configuration was still attached with little fibre break. But the compressed damage on the low density became larger and the shear failure appeared on the high density foam core. For the HML configuration, the projectile penetrated through the sandwich structure with the shear failure on the foam core and the fibre break on the composite skins.

In general, for the LMH configuration composite sandwich structure. Although, the compressed damage on the low density foam core appeared under 178 m s^{-1} impact, the back composite skin was still attached on the sandwich structure under the compliant impact experiments range from 178 m s^{-1} to 230 m s^{-1} . Whilst, the shear failure on the foam cores on HML configuration when the velocity was only 204 m s^{-1} , and the HDPE projectile penetrate through under 215 m s^{-1} . So in the graded foam core structures comparison, the LMH configuration performed better than the MMM and HML configurations under compliant impact range from 178 m s^{-1} to 230 m s^{-1} .

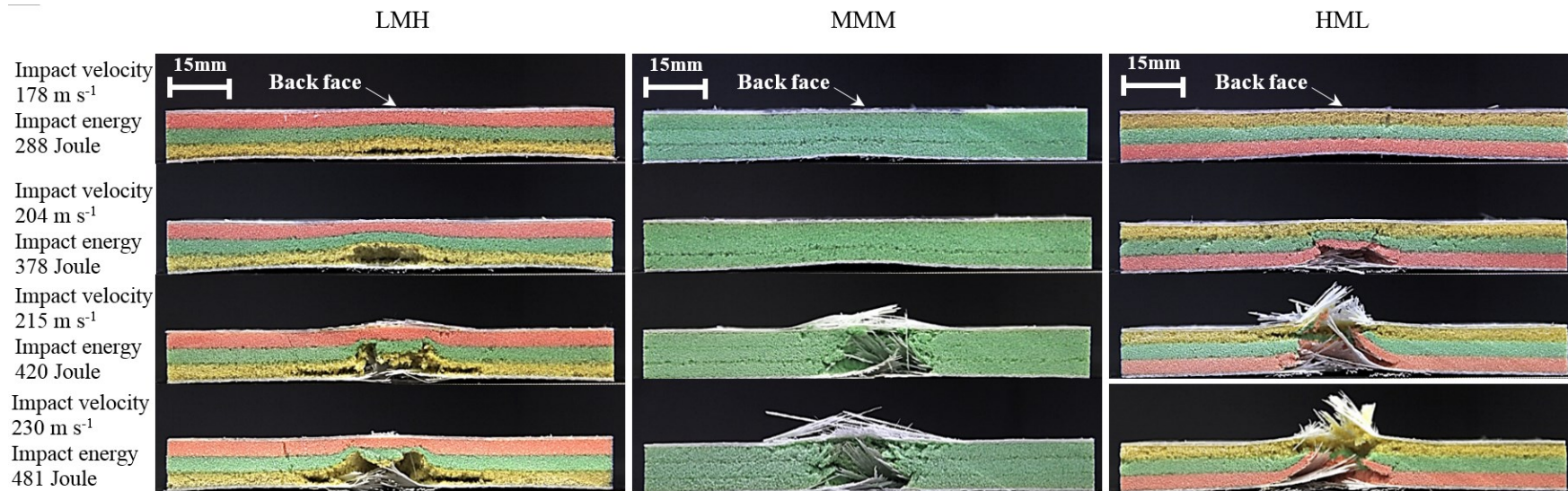


Figure 7-9. Sectional view for LMH (Three layers of 5 mm thick PVC foam with 60/80/100 kg m⁻³ density), MMM (Three layers of 5 mm thick PVC foam with 80 kg m⁻³ density) and HML (Three layers of 5 mm thick PVC foam with 100/80/60 kg m⁻³ density) of compliant (HDPE) gas gun impact under different velocities.

The result of the impact performance on graded foam core composite sandwich structure for LMH, MMM and HML configurations under compliant was discussed above. In order to find out the effect of the orientation with different density foam cores, the LHL and HLH configurations need to be investigated to see the influence of low and high density foam core in the sandwich structure under high velocity compliant impact.

Figure 7-10 shows the LHL, MMM and HLH configurations under different velocities compliant impact. As shown in the sectional views, under the 204 m s^{-1} velocity with 378 Joule compliant impact, the compression damage accrues on the low density foam core and the delamination between low density foam core and front composite skin. But for the HLH configuration, the front composite skin breaks, and the high density foam core has the shear damage transferred to the low density foam layer in the middle. When the impact velocity increased to the 215 m s^{-1} with 420 Joule energy. For the LHL configuration, the front layer of composite skin breaks and the low density foam layer on the impact side compressed more than the 204 m s^{-1} impact, but no damage to the high density foam layer. In the HLH configuration, the shear failure transferred from the front high density to the back high density foam core whilst the MMM configuration breaks the three layers of foam core and break the back face composite skin as tensile failures. For the highest velocity impact with 481 Joule energy, the LHL configuration shows that the compression damage pushes the both low density to disappear and shear the high density foam core layer, and break the both front and back composite skin. For the HLH configuration, the shear damage initials from the high density foam and through the three layers of foams and break the back face composite skin.

In general, the low density foam core on the impact side of the structure can prevent the shear failure by compressed damage, whilst when the high density on the impact side would cause the shear failure on the foam core and promote the damage on the front composite skin.

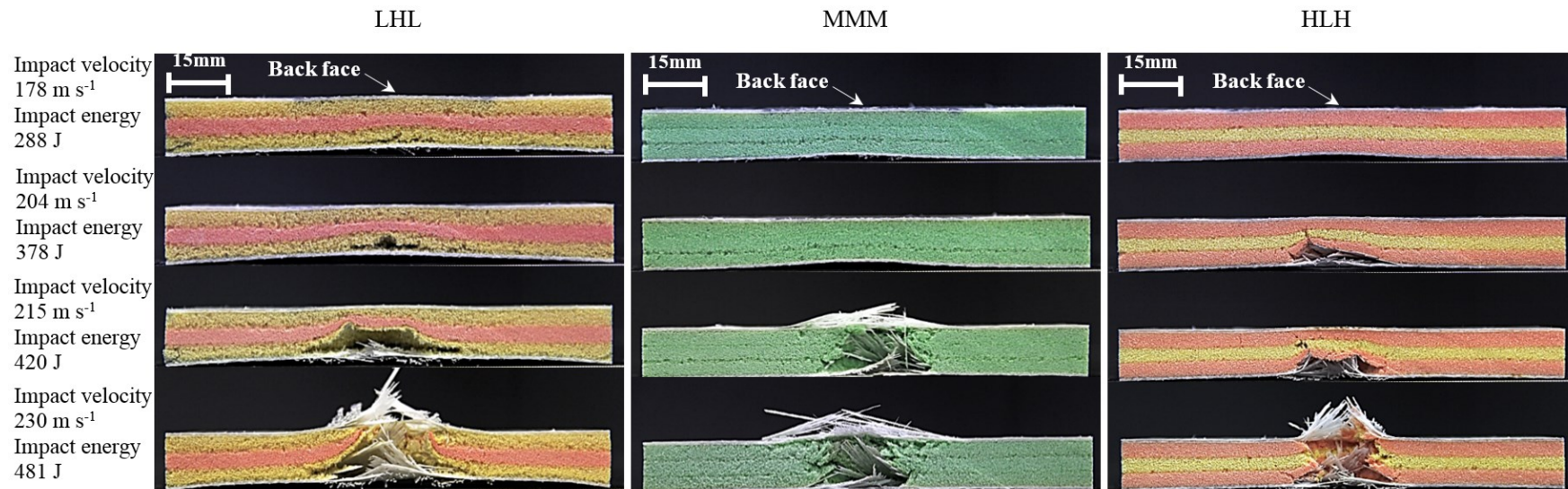


Figure 7-10. Section view for LHL (Three layers of 5 mm thick PVC foam with 60/100/60 kg m⁻³ density), MMM (Three layers of 5 mm thick PVC foam with 80 kg m⁻³ density) and HLH (Three layers of 5 mm thick PVC foam with 100/60/100 kg m⁻³ density) of compliant (HDPE) gas gun impact under different velocities.

7.3.4. Effect of impactor under high velocity impact experiment

For the high velocity rigid impact on composite sandwich structure, there are two situations to compare the impact performance of different configurations. If the rigid projectile did not penetrate through the structure, the back skin of major strain and out-of-plane displacement would be compared by using the high speed 3D DIC method. If the rigid projectile penetrate through the structure, the energy absorbsion would be compared by knowing the incident velocity and the residual velocity.

In Figure 7-11, the out-of-plane displacement and major strain were compared for different configurations under 178 m s^{-1} and 204 m s^{-1} impact velocity which did not break the back face.

Comparing the results, it showed that there is evidence to suggest that a graded core structure exhibits better impact resistance than monolithic equivalents. Comparing structures of equal weight (LMH and HML to MMM and M15) it was seen the graded specimens performed better in terms of both major strain and Z-displacement, apart from comparing the displacement of HML and MMM. LMH having 4% and 1.5% lower major strain values, and 7.4% and 4.9% lower z-displacement values than M15 and MMM respectively. HML having 3.0% and 0.5% lower major strain values than M15 and MMM respectively. Although HML did have 0.3% lower and 2.0% higher Z-displacement.

The results also show that the order of core grade configuration can positively affect the impact resistance. The LMH specimen had considerably lower values of max. major strain at the back face (4%) when compared to grade configurations that were of high density front layers (HLH and HHH with strains of 4.67% and 4.75% respectively). Moreover, the core grade configurations that had a low density front layer (LLL and LMH) also had considerably lower values of max displacement of the back face (10.22mm and 10.52mm respectively) when compared to grade configurations that had high density front layers (HLH and HHH with displacements of 11.49mm and 11.27mm respectively).

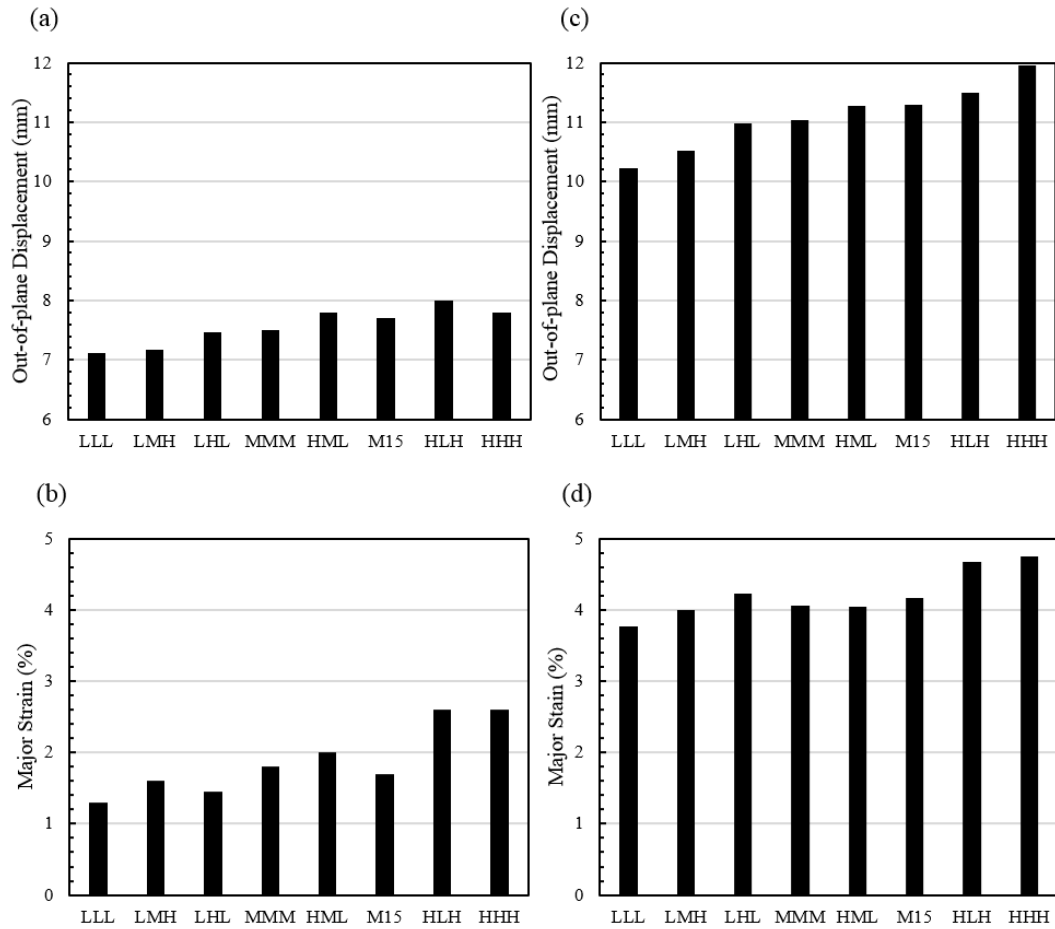


Figure 7-11. The comparison of out-of-plane displacement and major strain at the center point on different configurations with 288 Joules impact energy (a) & (b) (Impactor mass = 18.2 g; impact velocity = 178 m s⁻¹) and 378 Joules impact energy (c) & (d) (Impactor mass = 18.2 g; impact velocity = 204 m s⁻¹).

The incident velocity and residual velocity can be obtained by using the set up in Figure 7-2, and the velocity would be checked from the high speed camera as shown in Figure 7-12 for the incident velocity and Figure 7-13 for the residual velocity. The incident velocity was measured directly from the high speed camera as the projectile did not have any rotation and twisting during the impact process, and it can also be checked by the two gas gun IR sensors.

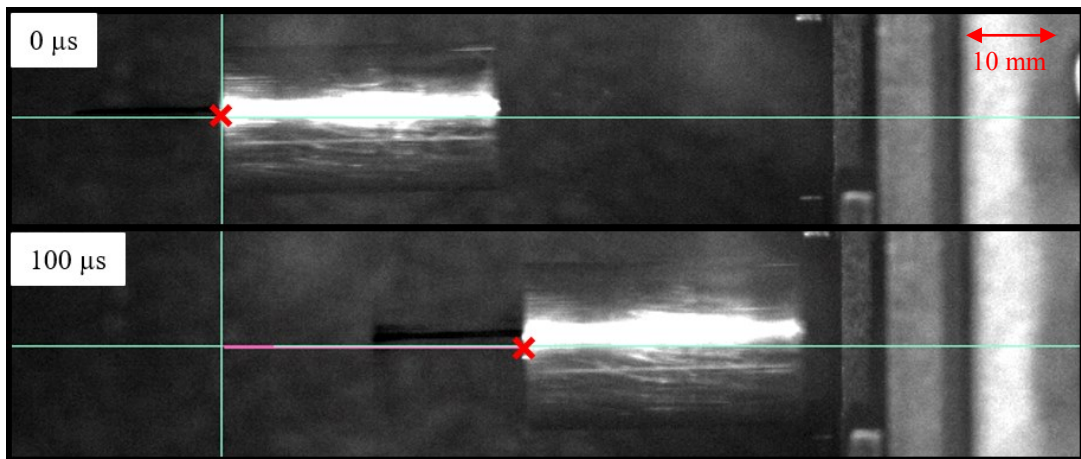


Figure 7-12. The incident velocity measure method by high speed camera.

The residual velocity should have two parts with linear velocity and rotational velocity. Because the projectile was rotating when it came out from the specimen. The linear and rotational velocity can be calculated by selecting two point on the projectile surface and the software will calculate the speed after calibrate the real dimension on the photos.

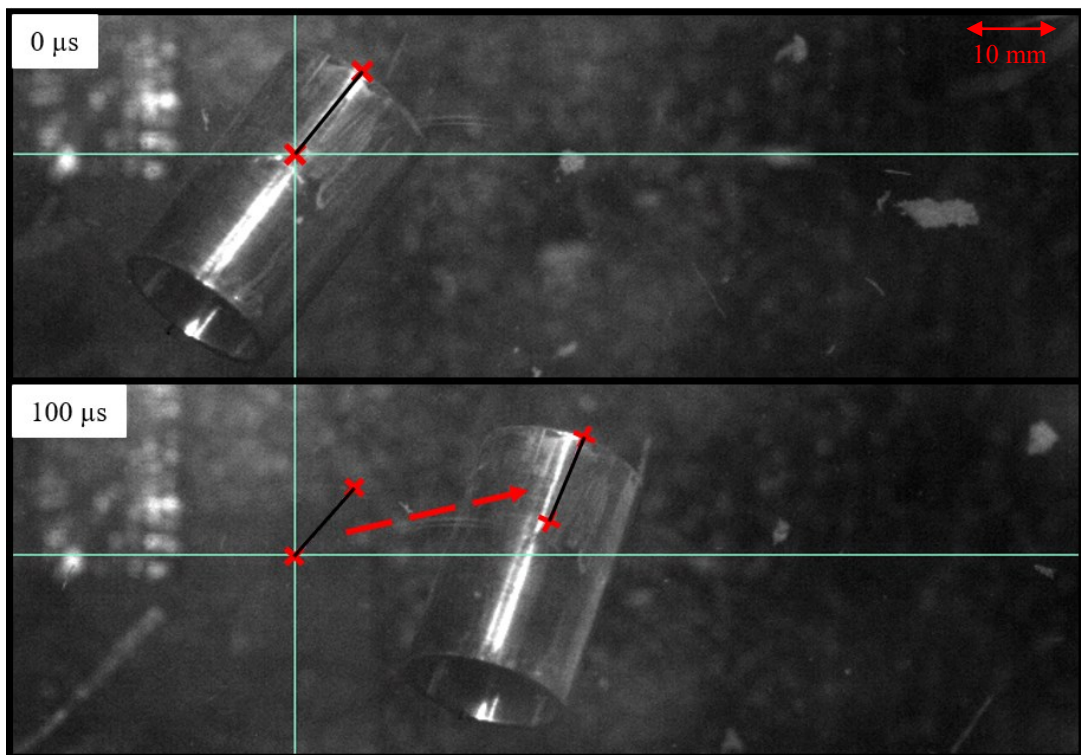


Figure 7-13. The residual velocity measure method by high speed camera.

The data from the perforation tests were used to calculate the energy absorption for each specimen and compiled into a graph for comparison between the different core grade configurations.

The linear kinetic energy of the projectile, before the impact, was calculated from the velocity obtained during the experiment.

$$E_{Kinetic,before} = \frac{1}{2}mv_{incident}^2 \quad (7-1)$$

where m is the mass of the projectile and $v_{incident}$ is the incident velocity.

The kinetic energy of the projectile, after the impact, was calculated by summing the linear kinetic energy and the rotational kinetic energy.

$$E_{Kinetic,after} = \frac{1}{2}mv_{residual}^2 + \frac{1}{2}I\omega^2 \quad (7-2)$$

where I is the moment of inertia about the centre of mass, $v_{residual}$ is the residual velocity of projectile and ω is the rotational velocity in rad/s.

So the energy absorpsion of the composite sandwich structure is:

$$E_{absorb} = E_{Kinetic,before} - E_{Kinetic,after} \quad (7-1)$$

When the impact velocity increased to break the composite structure (Figure 7-14 (a) & (c)), the energy absorpsion of different configurations are nearly the same with less than 4 % difference, and the sectional view on the foam cores (Figure 7-14 (b) & (d)) show that the similar damage to cut the front face fibre and penetrate through the whole structure.

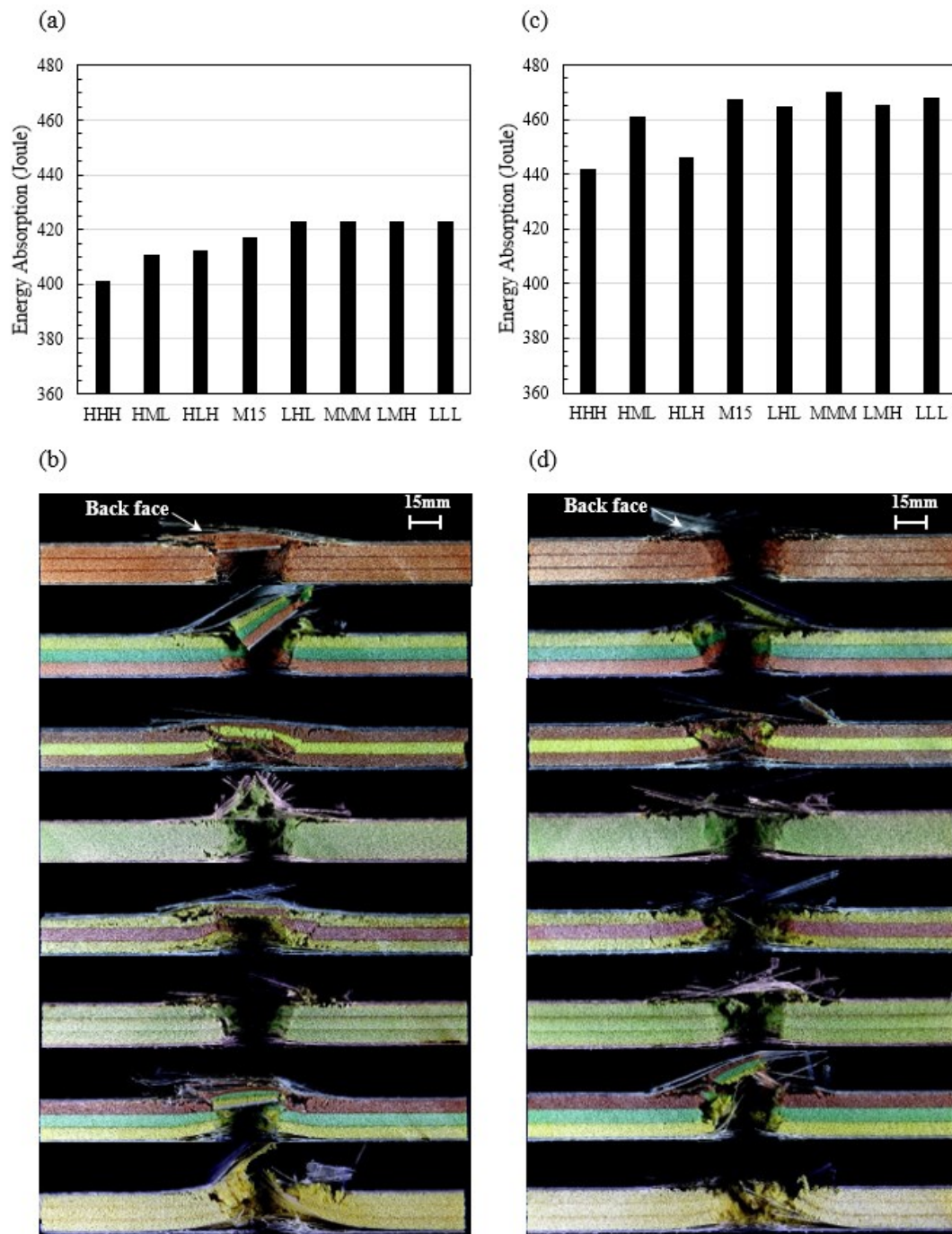


Figure 7-14. The energy absorption and sectional view of different configurations with 420 Joules impact energy (a) & (b) (Impactor mass = 18.2 g; impact velocity = 215 m s^{-1}) and 481 Joules impact energy (c) & (d) (Impactor mass = 18.2 g; impact velocity = 230 m s^{-1})

7.4. Conclusions

The following points describe the major conclusions from this high velocity impact research on composite sandwich structures:

- The layering of foam cores in the composite sandwich structures has little difference regarding the structural stiffness and energy absorption for both rigid and compliant impactors under high velocity impact.
- The higher density foam cores provide the higher structural stiffness. However, it promoted the damage on the composite front skin by shearing the top layer high density foam core. Whilst, the lower density foam cores gave a lower structural stiffness but the front composite skin did not break because the compression damage on the top layer of low density foam core absorbs the impact energy.
- For the same average density graded foam core composite sandwich structures, the back surface maximum strain and the out-of-plane displacement is smaller when the lower density foam core is on the impact side. When the higher density foam core is on the impact side, this can promote damage on the front composite skin and generate shear damage on the top high density foam core. This leads to a higher back surface maximum strain and out-of-plane displacement. This is because the compression damage can absorb the impact energy instead of shear damage for the total failure of whole structure.
- By increasing the impact energy, the damage on the sandwich structure foam cores would be different for different orientations. When the low density foam core was on the impact side, the compression damage occurred at an early stage. However, with the high density foam core on the impact side, the shear failure occurred at an early stage.

- The sharp edge of the rigid impactor helped cutting the front composite skin and the structure lost stiffness to sustain the impact load. The foam cores would be cut without the composite skin support. There is not much difference of energy absorption for the same average density foam cores when the impactor penetrates through the sandwich structure. However, the compliant impactor would have deformed the edge and dispersed the load on the impact surface. This would help the composite skins support the sandwich structure and absorb the impact energy by compression and shear of the foam cores.

8. Conclusions

In this research, both shear experiments on thick laminate composite structures and dynamic experiments on composite sandwich structures were performed. The two composite structures are widely used on the aerospace applications, and the problems when using these structures on the aerospace applications were studied in detail in this research. The experimental method and numerical modelling for the thick laminate composite were tested and verified, whilst, the low and high velocity impact performance on the composite sandwich structure were tested by using the DIC and damage comparison method.

8.1. Shear test for the thick laminate composite

The Iosipescu shear test was chosen to determine the out-of-plane shear property for the thick laminate composite structure. The different geometries of the shear test specimen were tested, by the help of DIC system, the best specimen geometry was selected for the comparison with numerical modelling. The effect of the v-notch depth and the effect of the specimen thickness were investigated in this part.

The increasing of the v-notch depth can help to get more delamination between layers during the shear test, which could reflect the real material property and provide a better understanding of how the delamination initiate and the delamination propagate, after compare the several different dimensions, the 12 mm of v-notch was chosen. However, the thickness of the specimen is another dimension to affect the shear test result. By choosing the different thickness, the best specimen thickness was chosen as 5 mm. The thin specimen could affect the stress concentration on the specimen and not stable for the initial delamination during the shear test, however, when the specimen thickness increase too thick, the stress distribution became unacceptable, because it reaches the limited of the fixture and the fixture cannot provide the uniform shear force anymore.

After exploring the shear test geometry, the FE model was employed to simulate the failure process for thick section CFRP composite was developed.

8.2. FE Modelling of shear test of thick-section composite

Employing a progressive failure approach to thick-sectioned composite structures, a non-linear multi-scale methodology based on sub-laminate homogenization and decomposition of sub-laminate stresses and strains has been presented. The accuracy of this approach has been checked by comparing numerical predictions with experimental results for out-of-plane shear tests, and the efficiency of the multi-scale approach has been verified by comparing multi-scale modelling and traditional ply-by-ply modelling for a virtual composite specimen under uniaxial tensile loading. The model predicts the effects of fibre orientation for different out-of-plane dimensions ($h = 3, 5 \text{ \& } 8 \text{ mm}$) with most of the failure features observed with good accuracy, e.g. failure mode, peak load and crack propagation path. The comparison between the linear model and the non-linear multi-scale models in predicting the structural response of the G13 shear test shows that the non-linear material effect on the structural response is significant and the non-linear model is much more accurate than the traditional linear model.

The non-linear multi-scale methodology based on sub-laminate homogenization developed in this research is a useful design tool and has been employed by FAI and AVIC and will be helpful to other aircraft manufacturers of thick-sectioned composites subjected to shear load. The model developed can be effectively applied to realistic engineering applications e.g. pin-jointed and other metallic/composites jointed structures subjected to the shear and other mechanical loading.

A joint paper was published in Composites Part B. The multi-scale model has subsequently been employed for engineering design problems at AVIC-FAI and the result show good agreement. However, it shows that the multi-scale model with the combined failure criteria can predict the interlaminar failure successfully under complex loading conditions. This has demonstrated that the time consuming nature of multi-scale modelling for the overall experiments for aerospace applications are saved significantly compared to the traditional method for thick section CFRP composite.

8.3. Low and high velocity impact of composite sandwich structures

Deformation and damage mechanisms of composite sandwich structures were investigated experimentally under low and high velocity. The impact experiments were performed using HDPE and steel projectiles for compliant and rigid impact. The impact velocities were range from 3.7 m s^{-1} to 230 m s^{-1} . High-speed cameras were used in the experiments to monitor the impact speed and the high-speed 3D digital image correlation to get the deformation and strain at the back surface of target.

The effect of various design parameters on the impact performance including the layering of the foam cores, density grading the foam cores and the effect of the impactor edge. The performance of different composite sandwich structures was assessed by comparing the load and displacement traces, sectional damage, energy absorbsion and maximum displacement and strain values at their back surface. From these research findings, several important conclusions can be made related to the design of composite sandwich structures for improving the energy absorbsion ability.

The layering of the foam core of similar density between the composite skins has little effect on the impact performance of composite sandwich structure under low and high velocity impact with rigid and compliant impactor, e.g. same as single core. This gives potential to rearrange the order of the core densities for a better impact energy absorption. Of course, overall stiffness of the composite sandwich structure need to be taken into account. It was found that the low density foam core on the impact side of the composite sandwich structure performs better for impact energy absorbsion.

8.4. Other PhD research

In addition to the above research, I was involved in research to characterise the impact performance of laminated glass windows subjected to high velocity soft impact, and the paper has been submitted to International Journal of Solids and Structures (please see Appendix A).

I was also involved in research on the characterisation of composite-titanium alloy hybrid joints using digital image correlation which is published in Composite Structures (please see Appendix B).

9. Suggestions for future research

This PhD study could be enhanced by future research. Suggestions for future work are summarized below:

9.1. Investigation of shear test for thick sectioned CFRP

The standard geometry of shear test specimen was investigated in this PhD research. The different specimen thickness and different specimen v-notch depth were evaluated for the good stress concentration and stress distribution. However, the different shapes of the v-notch can also affect the shear test result. The angle of the v notch and the shape of the v notch root could affect the stress concentration and the stress distribution on the specimen. But the quality of the specimen should be pay attention as the shape of the v notch root is hard to control and the finish of the specimen edge will cause the delamination of fibres. The novel methods of shear test should be improved, and the advantaged non-destructive test method should apply on the shear test to get useful information for the failure mechanism.

9.2. Multi scale modelling applications

In this PhD research, the multi-scale modelling for the shear test has been verified and has good agreement with the shear test results. However, the stability of the model for complex loading conditions for real aerospace structures should be evaluated. In the real structures and loading conditions, the components may be facing not only the tensile, compression and shear, but also need to face the threat of the bolt pull out, fatigue or thermal radiation. Furthermore, the different composite materials properties can also be used in this multi scale model to verify the universality of this model, such as, thermoplastic composite, short fibre composite and 3 dimensional woven composite. If the model can solve a huge range of materials and loading conditions by changing some of the model details, that would be extremely useful for aerospace engineers and save lots of time for the simulation on real aerospace structures.

9.3. Strength after impact study on composite sandwich structure

The research is currently underway examining the failure mechanism of the foam core and composite during the low and high velocity impact. However, the strength after impact need to be considered as one of the important aspect to evaluate the impact performance of composite sandwich structure. It is the residual strength which is a key factor when deciding whether or not these constructions are suitable for such applications. Various stationary loads exist in such structures e.g. compression under its own weight as well as flexure from subsequent impact. The first stage of this project would be to determine a suitable test method for such an investigation bearing in mind the variety of thickness of samples that need to be tested. Then each sample will need to be cut from the tested specimen. Digital image correlation (DIC) will be employed to highlight any existing flaws spotted during the visual inspection previously as well as highlighting the cause or initiation of damage during the tests. The tests that would be involved would include: compression through the thickness of the core; edgewise compression of the sandwich panel samples; and flexural tests of beam sections.

9.4. FE Model for the low and high velocity impact on composite sandwich structure

As far as I achieved the experimental results for the low and high velocity impact on composite structure, the simulation of the impact process could be a challenge for researchers. The failure mechanism of fibre skins and the foam cores and the impact velocity should be considered in the model. However, if the model can evaluate the composite sandwich structure under different velocity impact, there would be lots of potential to improve the impact performance of composite sandwich structures for the aerospace applications. This aspect of research has already been started a collaborate with RIMT researchers.

10. List of publication

Journal paper:

1. L. Jia, L. Yu, K. Zhang, M. Li, Y. Jia, B.R.K. Blackman, J.P.Dear. “Combined Modelling and Experimental Studies of Failure in Thick Laminates under Out-of-Plane Shear” *Composites Part B: Engineering* PII: S1359-8368(16)31139-8 DOI: 10.1016/j.compositesb.2016.08.017 (In press).
2. L. Yu, I. Mohagheghian, Y Wang, D. Ali, S. John, J.P.Dear. “Low and High Velocity Impact Response on a Graded Core Sandwich Structure for Hard and Compliant Impactors” *Composite structures* (To be submitted).
3. I. Mohagheghian¹, Y. Wang, J. Zhou, L. Yu, X. Guo, Y. Yan, M.N.Charalambides and J.P. Dear. “Deformation and damage mechanisms of laminated glass windows subjected to high velocity soft impact” *International Journal of Solids and Structures* (submitted).
4. Xichang Wang, Joseph Ahn, Cihan Kaboglu, Long Yu, Bamber R.K. Blackman. “Characterisation of composite-titanium alloy hybrid joints using digital image correlation” *Composite structures* Volume 140, 15 April 2016, Pages 702-711, ISSN 0263-8223.

Conference paper:

1. Yu, L., Mohagheghian, I., Blackman, B.R.K. and Dear, J.P., “Impact performance of composite sandwich structure under high velocity impact”, ICCM 20 - 20th International Conference on Composite Materials, Thomsen, O.T., Chair, (19-24 July, 2015), Copenhagen, Denmark, Paper Ref: 150701-1060.
2. Zhang, K., Yu L., Jia, Y., Blackman, B.R.K., Dear, J.P., Williams, J.G. “Investigation of thickness effects on shear characterisation of composites materials for aircraft structures”, ICCM 20 - 20th International Conference on Composite Materials, Thomsen, O.T., Chair, Copenhagen, Denmark.
3. Mohagheghian, I., Wang, Y., Yu, L., Zhou, J., Charalambides, M., Dear, J.P., “Impact damage assessment of aircraft components using bird substitute materials”, 5th EASN

Association International workshop on Aerostructures, Soutis, C. Chair, University of Manchester, UK.

4. Kelly, M., Arora, H., Del Linz, P., Worley, A., Kaye, M., Wang, Y., Chen, S., Yu, L., Fergusson, A., Hooper, P.A. and Dear, J.P., “Blast loading of composite sandwich structures”, 7th International Conference (Technical Committee 4) on Fracture Mechanics related to Polymers, Polymeric Composites and Adhesives, organised by the European Structural Integrity Society.

11. References

- [1] Jia, L., Yu, L., Zhang, K., Li, M., Jia, Y., Blackman, B.R.K., Dear, J.P. “Combined Modelling and Experimental Studies of Failure in Thick Laminates under Out-of-Plane Shear” *Composites Part B: Engineering* PII: S1359-8368(16)31139-8 DOI: 10.1016/j.compositesb.2016.08.017 (In press).
- [2] Yu, L., Mohagheghian, I., Wang, Y., Ali, D., John, S., Dear, J.P. “Low and High Velocity Impact Response on a Graded Core Sandwich Structure for Hard and Compliant Impactors” *Composite structures* (To be submitted).
- [3] Smith, A. (2014) Airbus, a leading aircraft manufacturer. Available at: <http://www.airbus.com> (Accessed: 29 July 2016).
- [4] Ladevèze, P., Allix, O., Deü, J., Lévêque, D. A mesomodel for localisation and damage computation in laminates, *Computer Methods in Applied Mechanics and Engineering*, Volume 183, Issues 1–2, March 2000, Pages 105-122, ISSN 0045-7825.
- [5] Shamsian, J. and Griffin, K. (2016) The wings of this Airbus plane can bend way more than you thought possible. Available at: <http://www.businessinsider.com.au/the-wings-of-this-airbus-plane-can-bend-way-more-than-you-thought-possible-2016-1> (Accessed: 29 July 2016).
- [6] Hinton, M. J., Kaddour, A. S. and Soden, P. D. Failure criteria in fibre reinforced polymer composites: the world-wide failure exercise. Oxford, UK: Elsevier Science Ltd, 2004.
- [7] Kaddour, A. S. and Hinton, MJ. (Guest eds) Evaluation of theories for predicting failure in polymer composite laminates under 3-D states of stress: Part A of the second worldwide failure exercise (WWFE-II). *J Compos Mater* (special issue) 2012; 46: 19–20.
- [8] Kaddour, A. S. and Hinton, MJ. (Guest eds) Evaluation of theories for predicting failure in polymer composite laminates under 3-D states of stress: Part B of the

second worldwide failure exercise (WWFE-II). J Compos Mater (special issue) 2013; 47: 6–7.

- [9] Chou P C, Carleone J, Hsu C M. Elastic constants of layered media. Journal of Composite Material, 1972, 6(1): 80-93.
- [10] D. Dawson, “Airframers vie for shares in growing short-haul market” High-Performance Composites, Vol. 22, No. 6, pp 32.
- [11] Available at: http://www.cargolaw.com/2000nightmare_singleonly10.html (Accessed: 29 August 2016).
- [12] Hutchinson, J. (2015) Extensive damage of Turkish airlines plane after being hit by bird. Available at: http://www.dailymail.co.uk/travel/travel_news/article-3070051/Incredible-photos-reveal-aircraft-s-nose-COLLAPSED-bird-flew-Turkish-Airlines-plane-carrying-125-passengers.html (Accessed: 29 August 2016).
- [13] Tong, L, Mouritz, A P, Bannister, M K. Introduction. A.P. Mouritz & M.K. Bannister L. Tong. 3D Fibre Reinforced Polymer Composites. Oxford : Elsevier, 2002.
- [14] Lu, X. and Liu, D. (1992) ‘An Interlaminar shear stress continuity theory for Both thin and thick composite Laminates’, Journal of Applied Mechanics, 59(3), p. 502. doi: 10.1115/1.2893752.
- [15] Pagano, N. J., and Pipes, R. B., 1973, "Some Observations on the Interlaminar Strength of Composite Laminates" International Journal of Mechanical Sciences, Vol. 15, pp. 679-688.
- [16] Liu, D., 1988, "Impact-Induced Delamination: A View of Bending Stiffness Mismatching," Journal of Composite Materials, Vol. 22, pp. 674-692.
- [17] Allen, H. G. (1969). Analysis and design of structural sandwich panels. Pergamon Press, Oxford, London, England
- [18] Thomsen, O.T., Bozhevolnaya, E. and Lyckegaard, A. (2005) Sandwich structures 7: Advancing with sandwich structures and materials: Proceedings of the 7th

- international conference on sandwich structures, Aalborg university, Aalborg, Denmark, 29-31 august 2005. Dordrecht: Springer-Verlag New York.
- [19] Kehrle, R. and Kolax, M. “Sandwich structures for advanced next generation fuselage concepts”, SAMPE Europe Technical Conference, Toulouse, France, 13-14 September 2006, pp. 11-16 (2006).
 - [20] Kolax, M. “Concept and technology: advanced composite fuselage structures”, JEC Composites, 10(6/7), 31-33 (2004).
 - [21] Kolesnikov, B.Y. and Herbeck, L. “Carbon fiber composite airplane fuselage: concept and analysis”, ILA International Conference, Berlin, Germany, 11–12 May 2004.
 - [22] van Tooren, M.J.L. Sandwich Fuselage Design, Delft University Press (1998).
 - [23] Tomblin, J., Lacy, T., Smith, B., Hooper, S., Vizzini, A. and Lee, S., “Review of damage tolerance for composite sandwich airframe structures”, Report DOT/FAA/AR-99/49, U.S. Department of Transportation, Washington D.C., USA (1999).
 - [24] Abbott, R. “Damage tolerance evaluation of composite honeycomb structures”, 43rd Int. SAMPE Symposium, Anaheim, CA, USA, 31 May-4 June 1998, pp. 376-386 (1998).
 - [25] Wong, R. “Sandwich construction in the star ship”, 37th International SAMPE Symposium and Exhibition, Anaheim, CA, USA, 9-12 March 1992, pp. 186-197 (1992).
 - [26] Avalor, M., Belingardi, G., Montanini, R., Int. J. Impact Eng. 25 (2001) 455.
 - [27] Zhao, H., Gary, G., Klepaczko, J.R., Int. J. Impact Eng. 19 (4) (1997) 319–330.
 - [28] Gibson, L.J., Ashby (Eds.), M.F., Cellular Solids: Structure and Properties, 2nd ed., Cambridge University Press, Oxford, 1997.

- [29] Jones AL, inventor; Improvement in paper for packing, US patent 122,023; December 19,1874.
- [30] Long O, inventor; Packings for bottles, jars & c; US patent 150,588; May 5, 1874.
- [31] Kazemahvazi S, Russell BP, Zenkert D. Impact of carbon fibre/epoxy corrugated cores. *Composite Structures*. 2012;94(11):3300-8.
- [32] Fleck, NA, Deshpande, VS. The resistance of clamped sandwich beams to shock loading. *Journal of Applied Mechanics*. 2004;71:386-401.
- [33] Wadley Research Group (2014) Wadley research group - UVA. Available at: <http://www.virginia.edu/ms/research/wadley/high-intensity-impulsive.html> (Accessed: 3 May 2016).
- [34] Abrate, S. *Impact on Composite Structures*. Cambridge: Cambridge University Press; 1998.
- [35] Paulino, GH., Jin, ZH. Correspondence Principle in Viscoelastic Functionally Graded Materials. *Journal of Applied Mechanics* 2001;68(1),129-132.
- [36] Koizumi, M. Concept of FGM. *Ceramic Transactions* 1993;34,3-10.
- [37] Suresh, S., Mortensen, A. *Fundamentals of Functionally Graded Materials*. London: Institute of Materials; 1998.
- [38] Miyamoto, Y., Kaysser, WA., Rabin BH., Kawasaki A, Ford RG. *Functionally Graded Materials: Design, Processing and Applications*. Dordrecht: Kluwer Academic; 1999.
- [39] Cannillo, V., Manfredini T., Siligardi C., Sola, A. Preparation and experimental characterization of glass–alumina functionally graded materials. *Journal of the European ceramic society* 2006;26(6),993-1001.
- [40] Al-Ajmi, MA., Alhazza, KA. 5th International Conference on Composite Science and Technology, ICCST/5 2005: conference proceeding. *Vibration and damping*

- analysis of sandwich beams with functionally graded viscoelastic core. Sharjah, United Arab Emirates, 125 - 128.
- [41] Parameswaran, V., Shukla A. Processing and Characterization of a Model Functionally Gradient Material. *Journal of Materials Science* 2000;35(1),21-29.
 - [42] Venkataraman, S., Sankar, BV. 42nd AIAA/ASME/ASCE/AHS/ASC Structures, Structural Dynamics and Materials Conference 2001: conference proceeding. Analysis of sandwich beams with functionally graded core. AIAA-2001-1281, Seattle, Washington.
 - [43] Kirigulige, MS., Kitey, R., Tippur, HV. Dynamic fracture behaviour of model sandwich structures with functionally graded core: a feasibility study. *Composites Science and Technology* 2005;65(7-8),1052-1068.
 - [44] ASTM D3518: Standard Test Method for In-Plane Shear Response of Polymer Matrix Composite Materials by Tensile Test of a 645_ Laminate, *Annual Book of ASTM Standards*, ASTM International, West Conshohocken, PA, 2013.
 - [45] ASTM D5379/D5379M-05: Standard Test Method for Shear Properties of Composite Materials by the V-notched Beam Method, *Annual Book of ASTM Standards*, ASTM International, West Conshohocken, PA, 2005.
 - [46] ASTM D7078: Standard Test Method for Shear Properties of Composite Materials by V-Notched Rail Shear Method, *Annual Book of ASTM Standards*, ASTM International, West Conshohocken, PA, 2012.
 - [47] Adams, D. F. and Lewis, E. Q., "Experimental Assessment of Four Composite Material Shear Test Methods," *J. Test. Eval.*, Vol. 25, 1997, pp. 174–181.
 - [48] Van Leeuwen, D. A., Nijssen, R. P. L., Westphal, T., and Stammes, E., "Comparison of Static Shear Test Methodologies; Test Results and Analysis," 2008 Beijing International Wind Energy Conference, Beijing, China, October 29–31, 2008, China International Exhibition Centre, Beijing.

- [49] Rosselli, F. and Santare, M. H., “Comparison of Short Beam Shear (SBS) and Interlaminar Shear Device (ISD) Tests,” *Composites, Part A*, Vol. 28, 1997, pp. 587–594.
- [50] Pahr, D. H., Rammerstorfer, F. G., Rosenkranz, P., Humer, K., and Weber, H. W., “A Study of Short-beam-shear and Double-lap-shear Specimens of Glass Fabric/Epoxy Composites,” *Composites, Part B*, Vol. 33, 2002, pp. 125–132.
- [51] Adams, D., “Shear Test Methods: Iosipescu vs. V-notched Rail,” *High-Performance Composites*, January 2010, <http://www.compositesworld.com/articles/shear-test-methods- iosipescu-vs-v-notched-rail> (Last accessed 10 Oct 2013).
- [52] EN ISO 14130, 1997, “Fibre-reinforced Plastic Composites— Determination of Apparent Interlaminar Shear Strength by Short-beam Method,” International Organization for Standardization, Geneva, Switzerland.
- [53] EN 2563, 1997, “Aerospace Series—Carbon Fibre Reinforced Plastics— Unidirectional Laminates—Determination of the Apparent Interlaminar Shear Strength,” Aerospace and Defense Industries Association of Europe - Standardization, Brussels, Belgium.
- [54] ASTM D2344/D2344M-00: Standard Test Method for Shortbeam Strength of Polymer Matrix Composite Materials and Their Laminates, *Annual Book of ASTM Standards*, ASTM International, West Conshohocken, PA, 2006.
- [55] SACMA SRM 8R-94, 1999, “Short Beam Shear Strength of Oriented Fiber-Resin Composites,” American Composites Manufacturers Association, Arlington, VA.
- [56] Berg, C. A., Tirosh, J., and Israeli, M., “Analysis of Short Beam Bending of Fiber Reinforced Composites,” *Composite Materials: Testing and Design (Second Conference)*, ASTM STP 497, H. T. Corten, Ed., ASTM International, West Conshohocken, PA, 1972, pp. 206–218.
- [57] Adams, D. F. and Lewis, E. Q., “Current Status of Composite Material Shear Test Methods,” *SAMPE*, Vol. 31, 1994, pp. 32–41.

- [58] Hutchinson JW, Xue Z. Preliminary assessment of sandwich plates subject to blast loading. *International Journal of Mechanical Sciences* 2003;45:687-705.
- [59] Meo M, Vignjevic R, Marengo G. The response of honeycomb sandwich panels under low-velocity impact loading. *International Journal of Mechanical Sciences* 2005;47:1301-26.
- [60] Roach A, Jones N, Evans K. The penetration energy of sandwich panel elements under static and dynamic loading. Part II. *Composite Structures* 1998;42:135-87.
- [61] Mines R, Worrall C, Gibson A. Low velocity perforation behaviour of polymer composite sandwich panels. *International Journal of Impact Engineering* 1998;21(10):855-934.
- [62] Qiu X, Deshpande VS, Fleck NA. Finite element analysis of the dynamic response of clamped sandwich beams subject to shock loading. *European Journal of Mechanics e A/Solids* 2003;22:801-15.
- [63] Fleck NA, Deshpande VS. The resistance of clamped sandwich beams to shock loading. *Journal of Applied Mechanics, ASME* 2004;71:1-16.
- [64] Radford DD, Fleck NA, Deshpande VS. The response of clamped sandwich beams subject to shock loading. *International Journal of Impact Engineering* 2006;32:968-1055.
- [65] Qiu X, Deshpande VS, Fleck NA. Dynamic response of a clamped circular sandwich plate subject to shock loading. *Journal of Applied Mechanics, ASME* 2004;71:637-82.
- [66] Qiu X, Deshpande VS, Fleck NA. Impulsive loading of clamped monolithic and sandwich beam over a central patch. *Journal of the Mechanics and Physics of Solids* 2005; 53:1015-61.
- [67] Hause T, Librescu L. Dynamic response of doubly-curved anisotropic sandwich panels impacted by blast loadings. *International Journal of Solids and Structures* 2007;44(20):667-700.

- [68] Vu-Quoc L, Deng H, Tan XG. Geometrically exact sandwich shells: the dynamic case. *Computer Methods in Applied Mechanics and Engineering* 2001;190 (22-23):2825-73.
- [69] Vonach WK, Rammerstorfer FG, Bathe KJ. Face layer wrinkling in sandwich shells of general configuration, *Computational Fluid and Solid Mechanics* 2003. Oxford: Elsevier Science Ltd; 2003. pp. 727-31.
- [70] Hohe J, Librescu L. A nonlinear theory for doubly curved anisotropic sandwich shells with transversely compressible core. *International Journal of Solids and Structures* 2003;40(5):1059-88.
- [71] Hohe J, Librescu L, Yong Oh S. Dynamic buckling of flat and curved sandwich panels with transversely compressible core. *Composite Structures* 2006;74 (1):10-24.
- [72] Hutchinson JW, He MY. Buckling of cylindrical sandwich shells with metal foam cores. *International Journal of Solids and Structures* 2000;37 (46-47):6777-94.
- [73] Cantwell, W. J., Dirat, C. and Davies, P. Comparative study of the mechanical properties of sandwich materials for nautical construction. *SAMPE J.* 30(4), 45-51 (1994).
- [74] Wen, H. M., Reddy, T. Y., Reid S. R. and Soden, P. D. Indentation, penetration and perforation of composite laminates and sandwich panels under quasi-static and projectile loading. *Key Engng. Matls.* 141-143, pp. 501-552 (1998).
- [75] Goldsmith, W. and Sackmann, J. L. An experimental study of energy absorption in impact of sandwich plates. *Int. J. Impact Engineering.* 12 (2), 241-262 (1992).
- [76] Mines, R. A. W. and Jones, N. An approximate elastic-plastic analysis of the static and impact behaviour of polymer composite sandwich beams. *Composites* 26(12), 803-814 (1995).
- [77] Mines, R. A. W. Impact energy absorption of polymer composite sandwich beams. *Key Engng. Matls.* 141-143, 553-572 (1998).

- [78] Wen, H. M. and Jones, N. Semi empirical equations for the perforation of plates struck by a mass. In Structures under Shock and Impact II (edited by P. S. Bulson), pp. 369-380. Computational Mechanics Publications, Southampton (1992).
- [79] Zee, R. H. and Hsieh, C. Y. Energy loss partitioning during ballistic impact of polymer composites. Polymer Composites 14(3), 265-271 (1993).
- [80] Wierzbicki, T., Alvarez, A. L. and Hoo Fatt, M. S. Impact energy absorption of sandwich plates with crushable core. In Impact, Waves and Fracture, AMD, vol. 205, pp. 391-411. ASME, New York (1995).
- [81] Mines, R. A. W., Worrall, C. M. and Gibson, A. G. The response of GRP sandwich panels to dropped object impact loading. In FRC90, Mechanical Engineering Publications, London, pp. 149-156 (1990).
- [82] Fire Coremat Data Sheet, Lantor B. V., Veenendaal, The Netherlands (1993).
- [83] Aerolam Data Sheet, Hexcel Composites, Duxford, Cambridge, U.K. (1993).
- [84] Das, M., Oterkus, E., Madenci, E., and Razi, H. Residual strength of sandwich panels with hail damage. Composite Structures. 2009;88(3):403-12.
- [85] Liaghat, G.H., Alavinia, A., Daghyani, H.R. and Sadighi, M. Ballistic limit evaluation for impact of cylindrical projectiles on honeycomb panels. Thin-walled Structures 2010;48:55-61.
- [86] Hoo, Fatt MS, Park, KS. Perforation of honeycomb sandwich plates by projectiles. Composites: Part A 2000;31:889-99.
- [87] Roach, AM., Evans, KE., Jones, N. The penetration energy of sandwich panel elements under static and dynamic loading. Part I. Composite Structures 1998;42:119-34.
- [88] Roach, AM., Evans, KE, Jones, N. The penetration energy of sandwich panel elements under static and dynamic loading. Part II. Composite Structures 1998;42:135-52.

- [89] Kepler, J. Impact penetration of sandwich panels at different velocities an experimental parameter study: Part I: parameters and results. *Journal of Sandwich Structures and Materials* 2004;6:357-74.
- [90] Kepler, J. Impact penetration of sandwich panels at different velocities e an experimental parameter study: Part II: interpretation of results and modelling. *Journal of Sandwich Structures and Materials* 2004;6:357-74.
- [91] Villanueva, GR, Cantwell, WJ. The high velocity impact response of composite and FML-reinforced sandwich structures. *Composites Science and Technology* 2004;64:35-54.
- [92] Belingardi, G, Cavatorta, MP, Duella, R. Material characterization of a composite-foam sandwich for front structure of a high speed train. *Composite Structures* 2003;61:13-25.
- [93] Shaker, M., Ko, F., Song, J. Comparison of the low and high velocity impact response of Kevlar fiber-reinforced epoxy composites. *Journal of Composites Technology and Research* 1999;21:224-9.
- [94] Vinson, JR. The behavior of sandwich structures of isotropic and composite materials. 2nd. Technomic; 1999.
- [95] Hou, W., Zhu, F., Lu, G., Fang, DN. Ballistic impact experiments of metallic sandwich panels with aluminium foam core. *International Journal of Impact Engineering* 2010;37:1045-55.
- [96] Villanueva, GR., Cantwell, WJ. High velocity impact response of composite and FML-reinforced sandwich structures. *Composites Science & Technology* 2004;64:35-54.
- [97] Avila, AF. Failure mode investigation of sandwich beams with functionally graded core. *Composite Structures* 2007;81(3):323-30.
- [98] Goldsmith, W., Wang, GT., Li, K., Crane, D. Perforation of cellular sandwich plates. *International Journal of Impact Engineering* 1997;19(5-6):361-79.

- [99] Skvortsov, V., Kepler, J., Bozhevolnaya, E. Energy partition for ballistic penetration of sandwich panels. *International Journal of Impact Engineering* 2003;28: 697-716.
- [100] Velmurugan, R., Ganesh, Babu M., Gupta, NK. Projectile impact on sandwich panels. *International Journal of Crashworthiness* 2006;11: 153-64.
- [101] Kepler J. Impact penetration of sandwich panels at different velocities. *Journal of Sandwich Structures and Materials* 2004;6: 379-97.
- [102] Goldsmith W, Sackman JL. An experimental study of energy absorption in impact on sandwich plates. *International Journal of Impact Engineering* 1992;12: 241-62.
- [103] Hoo Fatt MS, Park KS. Perforation of honeycomb sandwich plates by projectiles. *Composites Part A: Applied Science and Manufacturing* 2000;31: 889-99.
- [104] Lin C, Hoo Fatt MS. Perforation of sandwich panels with honeycomb cores by hemispherical nose projectiles. *Journal of Sandwich Structures and Materials* 2005;7: 133-72.
- [105] Hoo Fatt MS, Sirivolu D. A wave propagation model for the high velocity impact response of a composite sandwich panel. *International Journal of Impact Engineering* 2010;37: 117-30.
- [106] DS. SIMULIA. ABAQUS User Manual. Version 6.10, ABAQUS Inc., Providence, RI, USA, 2010.
- [107] Hallquist J O. LS-DYNA theory manual. Livermore software Technology Corporation, 2006, 3.
- [108] Dytran MSC. Theory manual. Wydawnictwo MSC, 2013.
- [109] Lapczyk, I.; Hurtado, J. A. Progressive Damage Modeling in Fiber-Reinforced Materials. *Composites Part A: Applied Science and Manufacturing* November 2007, 38 (11), *CompTest* 2006, 2333–2341.
- [110] Zhang, Z.; Chen, H.; Ye, L. Progressive Failure Analysis for Advanced Grid Stiffened Composite Plates/Shells, *Composite Structures*. Fourteenth International

Conference on Composite Structures - ICCS/14, Vol. 86, Issues 1-3, November 2008, pp 45–54.

- [111] Spottswood, S. M.; Palazotto, A. N. Progressive Failure Analysis of a Composite Shell. *Composite Structures* 2001, 53 (1), 117–131.
- [112] Huhne, C.; Zerbst, A.-K.; Kuhlmann, G.; Steenbock, C.; Rolfes, R. Progressive Damage Analysis of Composite Bolted Joints with Liquid Shim Layers Using Constant and Continuous Degradation Models. *Composite Structures* 2010, 92 (2), 189–200.
- [113] Xie, D.; Biggers, S. B., Jr. Progressive Damage Arrest Mechanism in Tailored Laminated Plates with a Cutout. *Acta Mechanica Solida Sinica* 2008, 21 (1), 62–72.
- [114] Labeas, G.; Belesis, S.; Stamatelos, D. Interaction of Damage Failure and Post-Buckling Behavior of Composite Plates with Cut-Outs by Progressive Damage Modeling. *Composites Part B: Engineering* 2008, 39 (2), 304–315.
- [115] Tserpes, K. I.; Papanikos, P.; Kermanidis, T. H. A Three-Dimensional Progressive Damage Model for Bolted Joints in Composite Laminates Subjected to Tensile Loading. *Fatigue & Fracture of Engineering Materials & Structures* 2001, 24 (12), 663–675.
- [116] Tserpes, K. I.; Labeas, G.; Papanikos, P.; Kermanidis, T. Strength Prediction of Bolted Joints in Graphite/Epoxy Composite Laminates. *Composites Part B: Engineering* 2002, 33 (7), 521–529.
- [117] Schuecker, C.; Pettermann, H. E. A Continuum Damage Model for Fiber Reinforced Laminates Based on Ply Failure Mechanisms, *Composite Structures*. Fifteenth International Conference on Composite Materials - ICCM-15, Vol. 76, Issues 1-2, October 2006, pp 162–173.
- [118] Hochard, C.; Payan, J.; Bordreuil, C. A Progressive First Ply Failure Model for Woven Ply CFRP Laminates Under Static and Fatigue Loads, *International Journal*

- of Fatigue. The Third International Conference on Fatigue of Composites, Vol. 28, Issue 10, October 2006, pp 1270–1276.
- [119] Ganesan, R.; Liu, D. Y. Progressive Failure and Post-Buckling Response of Tapered Composite Plates Under Uni-Axial Compression. *Composite Structures* 2008, 82 (2), 159–176.
 - [120] Firehole Technologies, Inc. Helius: MCT Version 2.0 for ABAQUS User's Guide, Appendix A.4, Laramie, WY, July 2009.
 - [121] Componeering, Inc. ESAComp 4.0 New Features Datasheet, Helsinki, Finland, March 2009.
 - [122] NEi Software. NEi Nastran Progressive Ply Failure Analysis, Westminster, CA, 2009.
 - [123] Cranes Software, Inc. NISA II/Composite Product Fact Sheet, Troy, MI, 2008.
 - [124] Zhou, Y X, Huang, Z M. A bridging model prediction of the ultimate strength of composite laminates subjected to triaxial loads. *Journal of Composite Materials*, 2012; 46: 2343–2378.
 - [125] Carrere, N., Laurin, F., Maire, J F. Micromechanical-based hybrid mesoscopic 3D approach for non-linear progressive failure analysis of Failure models and criteria for frp under in-plane or three-dimensional stress states including shear non-linearity. *Composite structure. Journal of Composite Materials*, 2012, 46(19-20): 2389-2415.
 - [126] Nelson, E E., Hansen, A C., Mayes, J S. Failure analysis of composite laminates subjected to hydrostatic stresses: A multicontinuum approach. *Journal of Composite Materials*, 2012, 46(19-20): 2461-2483.
 - [127] Hashin Z. Failure criteria for unidirectional fiber composites. *Journal of applied mechanics*, 1980, 47(2): 329-334.

- [128] Pinho S T, Darvizeh R, Robinson P, et al. Material and structural response of polymer-matrix fibre-reinforced composites. *Journal of Composite Materials*, 2012, 46(19-20): 2313-2341.
- [129] Deuschle H M, Puck A. Application of the Puck failure theory for fibre-reinforced composites under three-dimensional stress: Comparison with experimental results. *Journal of Composite Materials*, 2013, 47(6-7): 827-846.
- [130] Bogetti T A, Staniszewski J, Burns B P, et al. Predicting the nonlinear response and progressive failure of composite laminates under tri-axial loading. *Journal of Composite Materials*, 2012, 46: 2443–2459.
- [131] Bogetti, T A, Hoppel, C P R, Drysdale W H. Three-Dimensional Effective Property and Strength Prediction of Thick Laminated Composite Media. U.S. Army Research Laboratory: Aberdeen Proving Ground, MD, October 1995.
- [132] Staniszewski J M, Bogetti T A, Keefe M. An Improved Design Methodology for Modeling Thick-Section Composite Structures Using a Multiscale Approach. U.S. Army Research Laboratory: Aberdeen Proving Ground, MD, 2012.
- [133] Bogetti, T. A.; Hoppel, C.P.R.; Burns, B. P. LAMPAT: A Software Tool for Analyzing and Designing Thick Laminated Composite Structures; Technical Report 890; U.S. Army Research Laboratory: Aberdeen Proving Ground, MD, September 1995.
- [134] Staniszewski J M, Bogetti T A. LAMPAT and LAMPATNL User's Manual. ARMY RESEARCH LAB ABERDEEN PROVING GROUND MD WEAPONS AND MATERIALS RESEARCH DIRECTORATE, 2012.
- [135] Hinton, M.J. and P.D. Soden, Predicting Failure in Composite Laminates: The Background to the Exercise. *Composites Science and Technology*, 1998. 58: p. 1001-1010.
- [136] Greenwood, J.H., German Work on GRP Design. *Composites*, 1977: p. 175- 184.

- [137] Owen, M.J. and D.I. Rice, Biaxial Strength Behaviour of Glass-Reinforced Polyester Resins. *Composite materials: testing and design*, ASTM STP 787, ed. I. M. Daniel, 1982: p. 124-144.
- [138] Swanson, S.R. and A.P. Christoforou, Progressive Failure in Carbon/Epoxy laminates Under Biaxial Stress. *Journal of Engineering Materials and Technology*, Trans. ASME, 1987. 109: p. 12-16.
- [139] Tang, P., A Multiaxial Failure Criterion for Composites. *Computational Probabilistic Methods*, 1988(AMD/ASME, V93, published by ASME, New York, USA): p. 87-96.
- [140] Soden, P.D., M.J. Hinton, and A.S. Kaddour, A Comparison of the Predictive Capabilities of Current Failure Theories for Composite Laminates. *Composites Science and Technology*, 1998. 58: p. 1225-1254.
- [141] Hinton, M.J., A.S. Kaddour, and P.D. Soden, Evaluation of Failure Prediction in Composite Laminates: Background to 'Part B' of the Exercise. *Composites Science and Technology*, 2002. 62: p. 1481-1488.
- [142] Hinton, M.J., A.S. Kaddour, and P.D. Soden, A Comparison of the Predictive Capabilities of Current Failure Theories for Composite Laminates, Judged Against Experimental Evidence. *Composites Science and Technology*, 2002. 62: p. 1725-1797.
- [143] Eckold, G.C., Failure Criteria for use in the Design Environment. *Composites Science and Technology*, 1998. 58(7): p. 1095-1105.
- [144] Edge, E.C., Stress Based Grant-Sanders Method for Predicting Failure of Composite Laminates. *Composites Science and Technology*, 1998. 58(7): p. 1033-1041.
- [145] Gotsis, P.K., C.C. Chamis, and L. Miinetyan, Prediction of Composite Laminate Fracture: Micromechanics and Progressive Fracture. *Composites Science and Technology*, 1998. 58(7): p. 1137-1149.

- [146] Hart-Smith, L.J., Predictions of the Original and Truncated Maximum-Strain Failure Models for Certain Fibrous Composite Laminates. *Composites Science and Technology*, 1998. 58(7): p. 1151-1178.
- [147] Hart-Smith, L.J., Predictions of a Generalised Maximum Shear-Stress Failure Criterion for Certain Fibrous Composite Laminates. *Composites Science and Technology*, 1998. 58(7): p. 1179-1208.
- [148] Liu, K.S. and S.W. Tsai, A Progressive Quadratic Failure Criterion for a Laminate. *Composites Science and Technology*, 1998. 58(7): p. 1023-1032.
- [149] McCartney, L.N., Predicting Transverse Crack Formation in Cross-Ply Laminates. *Composites Science and Technology*, 1998. 58(7): p. 1069-1081.
- [150] Puck, A. and H. Schürmann, Failure Analysis of FRP Laminates by Means of Physically Based Phenomenological Models. *Composites Science and Technology*, 1998. 58(7): p. 1045-1067.
- [151] Rotem, A., Prediction of Laminate Failure with Rotem Failure Criterion. *Composites Science and Technology*, 1998. 58(7): p. 1083-1094.
- [152] Sun, C.T. and J.X. Tao, Prediction of Failure Envelopes and Stress/Strain Behaviour of Composite Laminates. *Composites Science and Technology*, 1998. 58(7): p. 1125-1136.
- [153] Wolfe, W.E. and T.S. Butalia, A Strain-Energy Based Failure Criterion for Nonlinear Analysis of Composite Laminates Subjected to Biaxial Loading. *Composites Science and Technology*, 1998. 58(7): p. 1107-1124.
- [154] Zinoviev, P., et al., The Strength of Multilayered Composites Under Plane Stress State. *Composites Science and Technology*, 1998. 58(7): p. 1209- 1223.
- [155] Hinton, M.J., A.S. Kaddour, and P.D. Soden, Evaluation of Failure Prediction in Composite Laminates: Background to 'Part C' of the Exercise. *Composites Science and Technology*, 2004. 64: p. 321-327.

- [156] Bogetti, T.A., et al., Predicting the Nonlinear Response and Progressive Failure of Composite Laminates. *Composites Science and Technology*, 2004. 64: p. 329-342.
- [157] Cuntze, R.G. and A. Freund, The Predictive Capability of Failure Mode Concept-Based Strength Criteria for Multidirectional Laminates. *Composites Science and Technology*, 2004. 64: p. 343-377.
- [158] Huang, Z.M., A Bridging Model Prediction of the Ultimate Strength of Composite Laminates Subjected to Biaxial Loads. *Composites Science and Technology*, 2004. 64: p. 395-448.
- [159] Mayes, J.S. and A.C. Hansen, Composite Laminate Failure Analysis Using Multicontinuum Theory. *Composites Science and Technology*, 2004. 64: p. 379-394.
- [160] Orifici, A.C., I. Herszberg, and R.S. Thomson, Review of Methodologies for Composite Material Modelling Incorporating Failure. *Composite Structures*, 2008. 86: p. 194-210.
- [161] Kaddour, A.S., et al., Damage Theories for Fibre-Reinforced Polymer Composites: The Third World-Wide Failure Exercise (WWFE III). *Proceedings of the 16th International Conference on Composite Materials*, 8-13 July, Kyoto, Japan, 2007.
- [162] Kaddour, A.S. and M.J. Hinton, Failure Criteria for Polymer Composites Under 3D Stress States: The Second World-Wide Failure Exercise. *Proceedings of the 17th International Conference on Composite Materials*, 27-31 July, Edinburgh, UK, 2009.
- [163] Kaddour, A.S., et al., Damage Prediction in Polymer Composites: Update of Part A of the Third World-Wide Failure Exercise (WWFE-III). *Proceedings of the 17th International Conference on Composite Materials*, 27-31 July, Edinburgh, UK, 2009.
- [164] Puri A, et al. "Analysis of Wind Turbine Material using Digital Image Correlation". *Proceedings of SEM XI International Congress & Exposition on Experimental & Applied Mechanics*. Orlando, Fl, USA, 2-5 June, 2008.

- [165] GOM mbH. "ARAMIS user manual". GOM mbH 2005.
- [166] Sjodahl, M. "Accuracy in electronic speckle photography". *Applied Optics*, 1997; 36 13: pg 2875-85.
- [167] GOM mbH. "ARAMIS". <http://www.gom.com/EN/measuring.systems/aramis/system/system.html>, GOM mbH, Accessed: 20th April, 2012.
- [168] Correlated Solutions, Inc. "Correlated solutions home". <http://www.correlatedsolutions.com/>, Correlated Solutions, Accessed: 20th April, 2016.
- [169] LaVision. "StrainMaster". <http://www.lavision.de/products/strainmaster.php>, LaVision, Accessed: 20th April, 2012.
- [170] Clocksin, W.F., Fonseca, J. Q., Withers, P. J., Torr, P. H. "Image processing issues in digital strain mapping". Seattle, WA, USA, 2002, 4790, pg 384-95.
- [171] Morton, J., HO, H., and Tsai, M. Y. An Evaluation of the Iosipescu Specimen for Composite Materials Shear Property Measurement. (1992) *Composites*, 23(6), p. 462. doi: 10.1016/0010-4361(92)90010-r.
- [172] JIANG, Y., LU F., GU Z. *Composite Material Mechanics*. Xi' an: Xi' an Jiaotong University, October 1993
- [173] Richard, R. M., Blacklock, J. R. Finite element analysis of inelastic structures. *AIAA Journal*, 1969,7:432-438.
- [174] Raimondo, L., Iannucci, L., Robinson, P., Curtis, P. T. A progressive failure model for mesh-size-independent FE analysis of composite laminates subject to low-velocity impact damage. *Composites Science and Technology*, 2012, 72(5): 624-632.
- [175] Donadon, M. V., Iannucci, L., Falzon, B. G., Hodgkinson, J. M., Almeida, S. F. M. A progressive failure model for composite laminates subjected to low velocity impact damage. *Computers & Structures*, 2008, 86(11): 1232-1252.

- [176] Faggiani, A., Falzon, B. G. Predicting low-velocity impact damage on a stiffened composite panel. *Composites Part A: Applied Science and Manufacturing*, 2010, 41(6): 737-749.
- [177] Pinho, S. T., Iannucci, L., Robinson, P. Physically based failure models and criteria for laminated fibre-reinforced composites with emphasis on fibre kinking. Part II: FE implementation. *Composites Part A: Applied Science and Manufacturing*, 2006, 37(5): 766-777.
- [178] Tserpes, K. I., Labeas, G., Papanikos, P., Kermanidis, T. Strength prediction of bolted joints in graphite/epoxy composite laminates. *Composites Part B: Engineering*, 2002, 33(7): 521-529.
- [179] Labeas, G, Belesis, S., Stamatelos, D. Interaction of damage failure and post-buckling behaviour of composite plates with cut-outs by progressive damage modelling. *Composites Part B: Engineering*, 2008, 39(2): 304-315.
- [180] Han, G., Guan., Z, Ji., Z, Du., S. Initial damage induced by thermal residual stress and microscopic failure analysis of carbon-fiber reinforced composite under shear loading. *Composite Interfaces*, 2015 (ahead-of-print): 1-15.
- [181] Private communication, AVIC Beijing Institute of Aeronautical Materials, December 2011.
- [182] Turon, A., Davila, C. G., Camanho, P. P., Costa, J. An engineering solution for mesh size effects in the simulation of delamination using cohesive zone models. *Engineering fracture mechanics*, 2007, 74(10): 1665-1682.

Deformation and damage mechanisms of laminated glass windows subjected to high velocity soft impact

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Abstract

Bird strike can cause serious risks to the safety of air travel. In this paper, the aim is to improve design by determining deformation and damage mechanisms of laminated glass windows when subjected to high velocity soft impacts. To achieve this, laboratory-scale impact experiments using bird substitute materials were performed in the velocity range of 100-180 m s⁻¹. An important step forward is that high-speed 3D Digital Image Correlation (DIC) has effectively been employed to extract the full-field deformation and strain on the back surface of the specimens during impact. The finite element simulations were performed in Abaqus/explicit using Eulerian approach and were able to successfully represent the experiments.

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For the laminated glass structures investigated, the damage inflicted is strongly sensitive to the nose shape of the projectile and most deleterious is a flat-fronted projectile. Two threshold velocities for impact damage have been identified associated with firstly the front-facing and secondly the rear-facing glass layer breaking. The order of the glass layers significantly influences the impact performance. The findings from this research study have led to a deeper and better-quantified understanding of soft impact damage on laminated glass windows and can lead to more effective design of aircraft windshields.

Keywords: *bird strike, laminated glass, hydrodynamics, soft impact, 3D DIC*

1-Introduction

Bird strike is a well-known safety concern in the aviation industry. However, more attention has been paid towards this problem recently as the number of the bird strikes has increased in the last two decades (Dolbeer et al., 2014). In the United States, for example, the number of wildlife strikes on civil aircraft was 6.1 times greater in 2013 compared to 1990 and 97% of the strikes are by birds. According to Dolbeer et al. (2014), 243 aircraft have been destroyed and 255 people have been killed globally since 1988 by wildlife strikes.

All front facing components of aircraft such as windshields, nose cones, wing leading edges and engine blades are vulnerable to bird strike during flight time, especially at the time of landing (Dolbeer et al., 2014). Although engine ingestion is recognised as the major threat to transport and executive jets (77% of all accidents are engine ingestion followed by 10% for windshields), for smaller aircraft, bird strike on the windshield is the main safety concern (52% of fatal accidents) (Thorpe, 2003). Similar figures have been reported by Dennis and Lyle (2008) where amongst the 51 fatal accidents identified between 1962 and 2009, caused by bird-

strike on the airframe, 27 accidents occurred on the windshield with the majority for smaller aircraft.

Despite its importance, there are not many experimental investigations available in the literature on the performance of the laminated glass windows against bird strike. This is due to the cost of full-scale experimental evaluations. Doubrava and Strnad (2010) investigated the performance of laminated glass windows with the thickness of 14, 18 and 20 mm against impact by a 1.81 kg bird at the velocity range of 300-450 km h⁻¹ (83-125 m s⁻¹). The details of the laminated glass configurations were not specified but the velocity at which the failure occurred increased linearly with the thickness of the windshield. Kangas and Pigman (1948) performed impact tests on various windshields using different materials and types of construction. The tests were conducted using birds at velocities up to 725 km h⁻¹ (208 m s⁻¹). Their study suggests that the primary factor influencing the impact strength of laminated glass window is the thickness of the plastic interlayer. Different methods of installation of the windshield to the cockpit were also investigated and were shown to have a strong effect on the impact strength of the windshield.

Due to the high cost of running full-scale experimental investigations, many researchers have used numerical analysis, e.g. finite elements (Grimaldi et al., 2013; Hedayati et al., 2014). Grimaldi et al. (2013) used the SPH method to parametrically investigate the response of a laminated glass window consisting of three layers of glass and two layers of PVB. They studied the effect of target geometry, impact angle and plate curvature on the response of the windshield against bird strike. The impact angle was found to have the strongest influence on the impact performance. Hedayati et al. (2014) also used the SPH method for the selection of the best material option for a helicopter windshield according to CS 29 certification for large helicopters. They suggested that the laminated glass with PVB interlayer performs the best.

67 ***Hydrodynamic loading***

68 Bird impact at high velocities can be considered as a “soft body impact”. The soft body impact
69 refers to an impact in which the strength of the projectile is much lower than that of the target
70 and consequently the projectile undergoes extensive deformation. The loading imposed on the
71 target is very different from normal hard impact (in which the projectile deformation is
72 negligible) and can be described well using hydrodynamic theory. In the hydrodynamic
73 approach, the loading mainly depends on the density and velocity of the projectile and
74 projectile strength and viscosity are neglected (Wilbeck, 1978). According to Wilbeck (1978)
75 and Barber et al. (1978), the bird impact loading on a rigid target can be divided into two stages:
76 stage i) in which the intensity of loading is high but the duration is very short (transient state)
77 and stage ii) which has the opposite characteristics, longer duration but less intensity (steady
78 state). This will be further discussed in Section 5-1. The loading can be affected by the response
79 of the target, as there is a coupling between loading and response. Factors such as shock
80 impedance, compliance and deformability of the target can significantly affect all aspects of
81 the loading including peak force and pressure, rise time, magnitude of impulse and duration of
82 decay and the steady state process (Barber et al., 1978).

83

84 ***Outline of the investigation***

85 In this paper, a combination of experimental and numerical analyses is employed to investigate
86 the impact performance of laminated glass windows. The deformation and failure mechanisms
87 of the laminated glass windows are studied at the velocity range between 100 to 180 m s⁻¹. The
88 effect of projectile nose shape, the glass front layer type and the order of glass layers on impact
89 damage of laminated glass windows are also investigated. The novel aspect of this research is
90 that high-speed 3D Digital Image Correlation (DIC) has effectively been employed to extract

the full-field deformation and strain on the back surface of the specimens during impact. This is a very useful method for checking the validity of the finite element simulations developed. The combined full field experimental/modelling approach provides useful insights for designing lightweight and impact resistant glazing against bird strike.

2-Materials

The laminated glass specimens used in this study are square plates with the dimension of 180 ×180 mm. The plates consist of two layers of glass and one layer of polymer which were laminated using an autoclave at Beijing Institute of Aeronautical Materials (BIAM). Two types of the strengthened alumina silicate glass were used for lamination: thermally and chemically strengthened. The chemically strengthened glass plates were manufactured by soaking float glasses in potassium salt solution for ion exchange at 420°C for 5 hours. This results in the formation of compressive layers with a depth of $38 \pm 5 \mu\text{m}$ and strength of $738 \pm 20 \text{ MPa}$ on both sides of the glass (air and tin sides), measured using Orihara surface stress meter model FSM-6000LE. The tin side of the glass was used for lamination. For the polymer interlayer, Thermoplastic Polyurethane (TPU) - KRYSTALFEX®PE499 from Huntsman was used.

Three configurations, including two laminated and one monolithic glass, were studied in this paper. The details of each configuration can be found in Table 1. For the laminated glass test specimens, Cases 1 and 2, two layers of strengthened glass with the thickness of 2.2 and 4.0 mm were used (the thinner glass layer normally faces the projectile except in one situation later which will be identified). Due to a limitation on the conventional polymer interlayer thickness available in the market, two layers of polymer were used to achieve the required thickness (Table 1). Case 3 in Table 1 represents an equivalent monolithic glass sample.

3-Experimental

To investigate the performance of laminated glass plates under soft impact, laboratory scale impact experiments were performed using a gas gun apparatus up to the velocity of 180 m s^{-1} (648 km h^{-1}). All impact tests were performed at 90° incidence angle (i.e. the target was orientated normal to the barrel).

3-1 Projectile

Using real birds in impact experiments is quite common in the aviation industry. However, it has several disadvantages including lack of repeatability and control on the orientation, homogeneity and isotropy of the projectile (Wilbeck and Rand, 1981). To overcome these shortcomings, gelatine and RTV rubber have been identified as two substitute materials and have been demonstrated to create a pressure profile similar to that of a real bird (Wilbeck and Rand, 1981).

In this study RTV rubber, Mold Max ® 10T, was used for the projectile which has a density of 1.09 g/cm^3 and shore hardness A of 10. Cylindrical projectiles were made by mixing two liquid components and casting into steel moulds. The moulds were then left in the vacuum chamber for curing and degassing. The final projectile has a diameter of $23.5 \pm 0.05 \text{ mm}$ and length of $50 \pm 0.3 \text{ mm}$. This gives an aspect ratio, projectile length over its diameter, of approximately two.

The projectile was accelerated to the required velocity using a light carrier. The carrier was made out of a thin layer of polystyrene film and a 2 mm PMMA backing disc. It has a wall thickness of $0.6 \pm 0.02 \text{ mm}$ and weight of $4.0 \pm 0.2 \text{ g}$ and is shown in Fig. 1a. The length of the carrier was chosen to be slightly less than that of the projectile. This is to ensure that no part of the carrier comes into contact with the target. The interaction of the rubber projectile with a 5

mm aluminium plate target, which was painted black prior to the experiment for better visualisation, is shown in Fig. 1b.

3-2 Gas gun set-up

As noted earlier, for achieving high velocity impacts, a gas gun apparatus was employed. The projectile was accelerated along a 3 m long barrel and its velocity was measured by two pairs of IR sensors located at the end of the barrel. The accuracy of the velocity measurements was confirmed by a series of calibration tests using a high-speed camera located perpendicular to the travel direction of the projectile. A transparent safety chamber, mainly made of thick polycarbonate panels, was used to confine the end of the barrel as well as the target area. This chamber helps to illuminate the target, observing the impact event and protecting the surrounding from the flying fragments caused by the impact. A schematic of the gas gun set-up is shown in Fig. 2a.

In order to measure the deformation of the target (i.e. the laminated glass windows), high-speed 3D digital image correlation (DIC) was employed. Two synchronised high-speed cameras (Phantom Miro M/R/ LC310) were located at the back of the target chamber (Fig. 2a). They were separated by 410 mm and had a distance of 925 mm from the centre point of the target. This gives an angle of approximately 25° between the two cameras which is the best recommended angle to do stereo vision measurements (Schreier et al., 2009). The cameras were recording at the rate of 40,000 frames per second. A pair of identical Nikon lenses with a fixed focal length of 50 mm was used for both cameras.

To monitor the interaction of the projectile with the target, another high-speed camera, Photron FASTCAM Mini UX50, was located on the impacted side (Fig. 2a). This camera was recording at a rate of 20,000 frames per second. All three cameras were triggered simultaneously using the signal generated by the IR sensors. Halogen lamps were used to illuminate the target. To

prevent any effect of heating from the halogen lamps, the lights were turned on just a few seconds before the test.

3-3 Sample preparation and boundary conditions

The laminated glass test samples with the size of 180×180 mm, were clamped around the edge to a metallic fixture by using twelve M8 bolts. The clamp was made of steel and had an opening of 150×150 mm. To avoid any direct contact between the glass and metallic clamp, which can lead to stress concentrations at the clamp edge and ultimately premature failure in the glass, rubber gaskets were used. The specimen and clamping system are shown in Figs. 2b and c. For all plates, rubber gaskets with the thickness of 4.1 ± 0.1 mm were used. The gaskets were compressed between the laminated glass plate and the clamp by tightening the bolts. The amount of the compression was controlled by a metallic spacer (Fig. 2b) with a thickness such that only a small amount of compression was present in the gaskets after tightening the bolts. This means that the sample can be assumed to be simply supported on an elastic foundation. The thickness of different test samples and their corresponding metallic spacer sizes are listed in Table 1.

In order to measure the deformation of the target by the digital image correlation technique (DIC), first a random speckle pattern was applied onto the surface of the specimen. The DIC algorithm then calculates the deformation by tracking each point through a pair of image sequences captured by the two high-speed cameras. The speckle pattern was made on the back surface of the specimen using a black marker on a white acrylic paint to generate the maximum contrast. The recommended size of the black speckles is between 3-5 pixels (Aramis, 2006). For the current experimental set-up, the optimum size of the speckles is between 0.7 to 1.0 mm (which can be best achieved by hand painting). Also, to prevent any shadow from the projectile affecting the DIC calculation of the back face, the front layer of the glass was painted black.

There is always a trade-off between the resolution and the number of photographs recorded by a high-speed camera. Therefore, for maximising the amount of information, which can be obtained from DIC in a relatively short impact event (duration less than 1 ms), the speckle pattern was only applied to the areas of most interest. Two configurations were chosen and are shown in Fig. 2c. In Configuration I, only the central part of the specimen, with the area of 70×70 mm was monitored. In Configuration II, the length of observation area was expanded to the whole free span of the plate (150 mm), but the width was narrowed down to 33 mm. This gives a similar total area as in Configuration I. In both configurations, two strain gauges were used. The strain gauge, FLA-2-8 from Techni Measure Ltd, has a 2 mm linear gauge and is thermally compensated for glass and ceramic. The surface of the glass was cleaned before attaching the strain gauge to it using a Cyanoacrylate adhesive. In Configuration I, a single strain gauge was located on the front glass face, whilst the second strain gauge was located exactly at the same position but on the back face. Both of the gauges were located 30 mm off-centre and measuring the strain along the y-axis (Fig. 2c). For Configuration II, both of the strain gauges were placed on the back glass face, one at the centre and the other at 30 mm off-centre. The strain gauges were attached to the glass surface before the sample was painted.

4-Numerical

In this section, the finite element method is used to simulate the mechanical response of the laminated glass windows under impact loading. The simulations were performed using Abaqus/explicit (“Abaqus Version 6.14,”). As a result of symmetry, only one quarter of the target was modelled (Fig. 3) with a symmetry boundary condition applied along the sectioned surfaces. The boundary of the target, shown in Fig. 2b, was modelled including the rubber gaskets (Fig. 3a). The two free surfaces of the rubber gaskets were constrained in the z

direction, simulating the presence of the two clamps in Fig. 2b. The target including glass, polymer interlayer and rubber gaskets were discretised using brick elements with eight nodes and reduced integration, C3D8R (in Abaqus notation). The mesh was refined near the central region of the plate (Fig. 3b) with a typical element size of $0.33 \times 0.33 \times 0.33$ mm. The glass was modelled as an elastic material with $\rho = 2440 \text{ kg m}^{-3}$, $E = 71.7 \text{ GPa}$ and $\nu = 0.21$ (Xue et al., 2013) where ρ , E and ν are density, elastic modulus and Poisson's ratio respectively. The rubber gaskets were modelled using a hyperelastic material model (Mooney-Rivlin) (Li et al., 2010) with density of 1060 kg m^{-3} and C_{10} and C_{01} (Mooney-Rivlin material model constants) of 0.69 and 0.173 MPa respectively.

For the polymer interlayer (TPU), a linear viscoelastic material model (generalised Maxwell model) was chosen as follows:

$$E(t) = E_{\infty} + \sum_{i=1}^n E_i e^{-\left(\frac{t}{\tau_i}\right)}, \quad (1)$$

where E_{∞} is the long-term modulus and E_i is elastic modulus associated to the relaxation time τ_i . Material parameters used for the generalised Maxwell model (Table 2) were extracted with a method similar to (Macaloney et al., 2007). The parameters were imported in Abaqus in the form of shear modulus (G_i), which has a value approximately one-third of E_i . The values of 1070 kg m^{-3} and 0.485 were chosen for the density and Poisson's ratio of the TPU respectively.

For modelling the soft impactor, the Eulerian method was used. In this approach, the mesh is fixed in space and the material flows through the elements. In comparison with the Lagrangian approach, the Eulerian method does not suffer from extensive mesh distortion. However, the method has its own disadvantages including mesh dependency of boundaries and relatively high computational cost (Heimbs, 2011). Depending on the duration of the simulations, two types of the Eulerian box were used (Figs. 3a and b). When the response of the target was of

231 interest and the simulation was performed over a long period (1 ms), the Eulerian box in Fig.
 232 3a was used as the box needed to cover the complete radial flow of the projectile. When the
 233 initial contact pressure and pressure distribution inside the projectile were of interest and the
 234 simulations were performed over the shorter period (the initial 50 μ s), the Eulerian box shown
 235 in Fig. 3b was used. The Eulerian box was discretised using 8-node brick elements with reduced
 236 integration (EC3D8R in Abaqus notation). The mesh size inside the Eulerian box had a typical
 237 size of 0.18 $\times$ 0.18 $\times$ 0.18 mm. It was found that the simulation results became insensitive to the
 238 size of the mesh on further refinement.

239 The rubber projectile was modelled using the Mie–Grüneisen equation of state (EOS) with an
 240 assumption of linear relationship between the velocity of the projectile (V_0) and the shock wave
 241 speed in the projectile material (V_s) as follows:

$$V_s = c + sV_0. \quad (2)$$

242 Therefore the relationship between the pressure (p) and nominal volumetric compressive strain
 243 (η) defined as $\eta = 1 - \rho_0/\rho$ is (Abaqus Version 6.14):

$$p = \frac{\rho_0 c^2 \eta}{(1-s\eta)^2} \left(1 - \frac{\Gamma_0 \eta}{2} \right) + \Gamma_0 \rho_0 E_m. \quad (3)$$

244 In the above equations ρ_0 , ρ , and E_m are reference density, current density and internal energy
 245 per unit mass respectively; c , s and Γ_0 are material constants with values of $c = 1869 \text{ m s}^{-1}$, $s =$
 246 0.5072 and $\Gamma_0 = 0$ (Iyama et al., 2009).

247 General frictionless explicit contact was used for modelling contact between all surfaces except
 248 the interface between the glass and the polymer interlayer for which a tie constraint was used.
 249 This is valid assumption as long as no debonding occurs at this interface, which was the case
 250 before the glass layers fractured.

251

252 **5-Results**

253 **5-1 Deformation and failure mechanisms**

254 As described earlier, soft impact by a silicon rubber projectile is used to generate hydrodynamic
255 loading similar to that which a windshield experiences during a bird strike. An example of the
256 results from an impact experiment is shown in Fig. 4, for a laminated glass sample, with a
257 thermally strengthened front face (Case 2), as often employed in the aircraft industry. This is
258 impacted at the velocity of $170 \pm 1 \text{ m s}^{-1}$ (using sample Configuration I shown in Fig. 2c). Fig.
259 4a displays the deformation of the projectile and its interaction with the target at intervals of
260 0.05 ms. The duration of the contact is short (less than 1 ms); the projectile flows radially as
261 expected and no part of the carrier hits the glass during the impact. At this velocity, only the
262 front glass layer breaks and the rear glass layer is still intact. Fig. 4b displays the out-of-plane
263 displacement of the target calculated by DIC. The centre of the plate is displaced by about 5.0
264 $\pm 0.1 \text{ mm}$. The time of the maximum deflection is coincident with the time when the projectile
265 completely loses its momentum and comes to rest (ca. $450 \text{ }\mu\text{s}$). After this time, the projectile
266 which is nearly flattened (Fig. 4a), starts to rebound. As noted earlier, the speed of the cameras
267 used for DIC is twice the speed of the one camera used for monitoring the projectile. Hence,
268 for each image in Fig. 4a, two images exist for DIC calculation. The extra images are excluded
269 from Figs. 4b and c.

270 The major principal strain calculated by DIC can be found in Fig. 4c. The strain reaches its
271 maximum of about 0.8 % at ca. $200 \text{ }\mu\text{s}$. As can be noted, the time of maximum strain is not
272 aligned with that of the maximum deflection (ca. $450 \text{ }\mu\text{s}$). To further investigate this, the test
273 was repeated using the sample Configuration II. This configuration allows observing the

274 deformation over the whole span of the plate and the effect of the boundary on the deformation
275 of the plate can be investigated. The results are shown in Fig. 5 for a laminated glass (Case 2)
276 at an impact velocity of $174 \pm 1 \text{ m s}^{-1}$. Similar to Fig. 4, the out-of-plane displacement and
277 major principal strain contours are plotted for the observed area. The maximum deflection at
278 the centre of the plate reaches approximately 5.5 mm and the major strain of nearly 0.8%. The
279 out-of-plane displacement and major strain history are plotted in Fig. 5c for the centre point of
280 the plate. Each data point shown in Fig. 5c, corresponds to one of the contours in Figs. 5a and
281 b. There is a gradual increase in central out-of-plane displacement until the maximum at 475
282 μs . A more rapid rise in central major strain can be seen, starting at the very early stages of the
283 deformation. The deformation of the plate in Fig. 5c can be divided into four phases: Phase 1
284 where both strain and displacement are increasing, Phase 2 in which the displacement is
285 increasing but there is not much change in the value of the strain, Phase 3 where the strain is
286 decreasing while the displacement is still increasing and finally Phase 4 where both
287 displacement and strain are decreasing.

288 The out-of-plane displacement profile of the plate is plotted over its whole span when the plate
289 is displaced (Fig. 5d) and then rebounds (Fig. 5e). Again, each profile corresponds to one of
290 the out-of-plane displacement plots in Fig. 5a. As can be observed, the deformation profile
291 does not cover the whole 150 mm of the plate length as some data (obtained from DIC) is lost
292 at points close to the edge of the clamp. At the early stage, Phase 1, the deformation is highly
293 localised under the point of impact and the boundaries are still not activated (Fig. 5d). During
294 this phase, flexural elastic waves travel from the point of contact towards the plate boundary.
295 This phase ends when these waves reach the boundary (at ca. 175 μs). During Phase 2, the level
296 of the strain at the centre of the plate does not change significantly. The out of the plane
297 displacement however, is still increasing. It can be noticed in Fig. 5d that the displacement at
298 the boundaries is not zero in this phase. This means that the rubber gasket is compressed and

its deformation can account for a part of the increase in the out-of-plane displacement. In Phase 3, the plate starts unloading, as indicated by a significant drop in the major strain at the centre of the plate (Fig. 5c). The profile of the plate in this phase is also different from that of Phase 2 (Fig. 5d). The rubber gasket is still being compressed and is responsible for a further increase in the out-of-plane displacement (Fig. 5c). At the end of Phase 3, the gasket, which had the original thickness of 4.1 mm, is compressed by around 50%. In Phase 4, the plate is rebounding back and both the out-of-plane displacement and major strain are decreasing. The profile of the plate in this phase is plotted in Fig. 5e.

The in-plane strain (strain in the y direction in Fig. 2c), calculated by DIC is compared in Fig. 6 with the results of the two strain gauges mounted on the back surface of the plate for a Configuration II specimen. As mentioned earlier, the top surface of the strain gauges was painted and speckled therefore, the deformation of the gauge can be monitored during the impact event using DIC. Overall, there is a very good agreement between the two measurements. The strain gauge at the centre only measured the strain up to 0.4 ms before the gauge terminals peeled off. This problem was observed for most of the strain gauges placed at the centre of the plate as the out-of-plane displacement was largest at this point. In most cases, only the terminals, which had a heavier solder connection, peeled off from the plate but the gauge was still attached. This allowed continuous measurement of gauge deformation using DIC.

The gauge at the centre of the specimen recorded a high initial rise in the strain at the very early stage of the deformation. This initial peak in strain can also be observed in the DIC results but it has a lower value. For capturing this peak more accurately using DIC, higher frame rates needed to be used for high-speed cameras. The strain at the centre of the sample reaches the maximum value of 0.8% around 0.2 ms and, as shown in Fig. 6, there is a good agreement between the strain gauge and DIC values.

A photograph of the failed sample, for Case 2, is shown in Fig. 7. The front impacted side of the specimen was painted black prior to the impact experiment. Therefore, the white area in Fig. 7, is an indicator of the regions where the glass fragments detached from the polymer interlayer. There is a black circular area, with the diameter equal to that of the projectile, at the centre of the plate where the glass fragments are still attached to the polymer. This type of damage pattern in Fig. 7 is similar to what has been reported for liquid jet impact (Bourne et al., 1997; Bowden and Field, 1964; Field, 1966; Hand and Field, 1990; Van Der Zwaag and Field, 1983; Walley et al., 2004).

It was apparent from the high-speed photographs that whilst the fracture in the front glass occurred during the loading phase of the deformation, the glass fragments mostly detached when the plate rebounded. Due to the presence of the black paint layer, the onset of the failure in the glass front layer was difficult to identify in high-speed photographs (Fig. 4a). From the signal of the strain gauge attached to the front layer (Configuration I, Fig. 2c), it can be inferred that the fracture started very early in the deformation process. However, determining the exact time of fracture initiation was difficult.

To further investigate this, a separate series of experiments was performed using clear targets (i.e. no painting was used on either side of the glass). Two high-speed cameras were used: one observing the impacted side and the other one monitoring the back side of the glass. The high-speed image sequences are shown in Fig. 8 for a monolithic (6.0 mm monolithic chemically strengthened plate – Fig. 8a) and a laminated glass (Case 1) plate (Fig. 8b) impacted at the velocity of $144 \pm 1 \text{ m s}^{-1}$ and $160 \pm 1 \text{ m s}^{-1}$ respectively. At the velocity of 144 m s^{-1} , the monolithic glass plate is completely broken and the projectile penetrates through it (Fig. 8a) whilst for the laminated glass plate, which is impacted at the higher speed of 160 m s^{-1} , only the front layer is broken (Fig. 8b). By looking more closely at the high-speed image sequence, it can be seen that the fracture is initiated as soon as the projectile comes into contact with the

plate (i.e. the damage is apparent in the second image which is only $25 \pm 1 \mu\text{s}$ after the initial contact). Both the time of the damage initiation (Fig. 8) and type of damage pattern (Fig. 7) indicate that the failure in the front glass layer is mainly controlled by high intensity water-hammer pressures developed in the initial phase of hydrodynamic loading.

The schematic in Fig. 9 can be used to explain the deformation and failure mechanisms of laminated glass windows under a high velocity soft impact. The hydrodynamic loading is well-known to have two distinct stages, illustrated in Fig. 9a. In Stage 1, as soon as the projectile comes into contact with the front glass layer, a shock wave is generated that then propagates along the projectile (Fig. 9b). The high intensity pressure behind the shock wave in the projectile is called “Hugoniot pressure” P_H and depends on the initial density (ρ_0) and velocity of the projectile (V_0) as well as the shock wave speed in the projectile material (V_s) (Equation 2) according to the following relationship (Wilbeck, 1978):

$$P_H = \rho_0 V_0 V_s . \quad (4)$$

At the time of impact, two types of stress waves are generated in the target: surface waves called “Rayleigh waves” and compression elastic waves (Field, 1966), which propagate inside the glass (Fig. 9b). Soon after the initial phase of the contact, the release waves are generated at the edges of the projectile causing formation of high velocity jets which travel with transverse velocity, $2V_0/\pi$, across the impact surface (Lesser, 1995). The release waves start propagating inside the shocked material, which leads to a significant drop in the pressure inside this region. The duration of Stage 1 depends on how fast these waves can reach the centre of the projectile which itself is a function of speed of sound in the shocked material and radius of the projectile (Field, 1966; Wilbeck, 1978). In cases when the duration of Stage 1 is short (e.g. impacts by small diameter projectiles with high initial velocity), the high intensity compressive waves can reach the interface between glass and polymer interlayer, turn to tensile waves and

reflect back (Fig. 9c-1). This is a result of the mismatch between the acoustic properties of the two layers. Field (1966) suggested that a combination of Rayleigh waves and reflected tensile waves are responsible for the damage initiation in liquid impact of thin glass plates. The damage is initiated in the form of a large number of circumferential cracks which form a ring with a diameter approximately equal to the initial diameter of the projectile.

Considering the diameter of the projectile here ($d = 23.5$ mm) and assuming the speed of release waves (c_r) to be the same as V_s (c_r is slightly greater than V_s (Wilbeck, 1978)) the duration of Stage 1 is calculated to be around $6.5 \mu\text{s}$. This is significantly longer than the time needed for compressive waves to reach the glass-polymer interface (only about $1 \mu\text{s}$ according to the speed of sound in the alumina silicate glass, $5,868 \text{ m s}^{-1}$ (Jin-Hyun, 1997)). It means that by the end of Stage 1, the elastic waves travel at least six times across the thin front glass whilst the material is still in a compressive state. Therefore, the reflected tensile waves cannot account for the failure of the front glass layer here.

For soft polymer interlayers (e.g. TPU) the compliance of the interlayer can cause more local bending in the front glass layer which can facilitate failure in this layer (Fig. 9c-2). Van der Zwaag and Field (1983) investigated the combined effect of bending stresses and stress wave reflection from the rear surface of a thin glass plate by supporting the glass with an acoustically-matched thick glass backing. Their results suggest that the bending stresses and stress wave reflection indeed contribute to the liquid jet impact damage. Therefore, we believe that a combination of Rayleigh waves and local bending stresses is more likely to be responsible for the impact damage in the thin front glass layer.

In the second stage of the deformation, known as “steady state”, the projectile flows radially. The steady state pressure (P_s) can be estimated using the Bernoulli equation:

$$P_s = \frac{1}{2} \rho_0 V_0^2. \quad (5)$$

During Stage 2, it is assumed that the broken front glass layer does not contribute significantly in carrying the load. The time scale in Stage 2 is sufficient for the development of flexural waves which initiate from the centre of the plate and move towards the boundary (Fig. 5d). If the impulse transferred to the target, defined as the area under the curve in Fig. 9a, becomes high enough, the rear glass layer will break normally at the centre of the plate (Fig. 9e) where the flexural stresses are maximum.

The strain development and impact damage as a function of impact initial velocity are shown in Fig. 10 for laminated glass windows (Case 1). Impact tests at different velocities were performed ranging between 120-180 m s⁻¹. The maximum strain at the centre and at a location 30 mm off-centre of the rear glass layer are used for evaluating the performance in Fig. 10b. The solid symbols correspond to the maximum major strain measured at the centre of the specimen using DIC. The open symbols correspond to the maximum strain measured using the off-centre strain gauge in the y-direction (location shown in the bottom right diagram of Fig. 10b). The strain gauge traces are shown in Fig. 10c for seven different impact velocities. Apart from the impact experiment at a velocity of 118 m s⁻¹, the maximum strain occurs around 0.2 ms for all experiments.

Three regions can be identified in Fig. 10b. For impact velocities below 131 m s⁻¹, no damage was observed in any of the glass layers. The intensity of Hugoniot pressure (Equation 4) is not high enough to cause any failure in the front glass layer. The level of Hugoniot pressure is however, strongly dependent on the projectile initial velocity: both directly with V_0 and indirectly with V_s (Equations 2 and 4). Therefore, the level of Hugoniot pressure rises steeply with increasing projectile velocity and becomes sufficient to break the glass front layer at the impact velocities between 146 and 168 m s⁻¹. The photographs of the failed samples at

velocities of 146, 157, 165 m s⁻¹ and 179 m s⁻¹, taken from the impacted side, are shown in Fig. 10a. For impact velocities of 146, 157 and 165 m s⁻¹, only the front layer is fractured and the rear glass layer is still intact. Since the exact impact damage threshold velocity, at which the failure is initiated in the front glass layer, is not precisely known, the region between 131 and 146 m s⁻¹ is shaded in Fig. 10b. Additional tests are needed to narrow down this area.

Although the rear glass layer is protected from failure by the polymer interlayer in the initial high intensity stage, it can break if the amount of impulse transferred to the target becomes high enough. This second impact damage threshold velocity, also displayed as a shaded area in Fig. 10b, is located between impact velocities of 168 and 179 m s⁻¹. At the velocity of 179 m s⁻¹ both layers of glass are broken. It should be noted that the maximum major strain in Fig. 10b for 179 m s⁻¹ (solid symbol) is chosen from one frame prior to the failure.

For visual guidance, a solid line is fitted through the off-centre strain gauge open square data points. A dashed line with a similar slope is fitted through the two solid square data points below the first threshold (where no damage in front glass layer occurred at these velocities). A good fit is observed in both cases. For all of the impact velocities at which the front glass layer breaks, a jump in the maximum strain in the centre of the specimen is observed. A dotted line is fitted through these solid square data points and is shown in Fig. 10b.

5-2 The effect of the nose shape of the projectile

The projectile nose shape and consequently the geometry of the contact area is known to significantly affect different aspects of hydrodynamic loading (Dear and Field, 1988; Field et al., 1985). In this section, the effect of projectile nose shape on the impact performance of the laminated glass windows is investigated by using two nose shapes: flat (as investigated in the

previous section) and hemi-spherical. Both experimental and numerical analyses are used for assessing the difference in the performance. In Fig. 11, the simulation results for the deformation and strain are compared with that from the experiment for a laminated glass plate (Case 1) impacted by a hemi-spherical nose projectile at the velocity of 158 m s^{-1} . The comparison is made for the deformation profile (Fig. 11a), central out-of-plane displacement and strain history (Fig. 11b), and strain in the y direction at the location of the strain gauge (Fig. 11c). In general, a very good agreement is observed which indicates the validity of the simulation results.

The experimental results obtained from impacts on the laminated glass windows (Case 1) by projectiles with the two nose shapes are compared in Fig. 12. For the range of impact velocities investigated, $100\text{-}169 \text{ m s}^{-1}$, no damage was observed in any of the glass layers for the hemi-spherical projectile. It should be noted that for a hemi-spherical nose, the maximum major strain at the centre (solid symbols), even at the higher velocities (e.g. $159 \text{ \& } 169 \text{ m s}^{-1}$), lies on the dashed line plotted for the flat-fronted nose shape (the linear fit for data points with no breakage) (Fig. 12).

The explanation for this dependency of failure to the projectile nose shape can be found in Fig. 13 where the numerical pressure development inside the projectile and the associated contact pressure imposed on the laminated glass are shown for flat and hemi-spherical nose shapes. Both of the projectiles have the same mass and diameter and are fired at the same initial velocity of 131 m s^{-1} . This is the velocity at which no fracture occurs for any of the nose shapes (Fig. 12).

The development of high intensity pressures is apparent at the very early stages of impact ($t = 0.75 \text{ }\mu\text{s}$) for a flat-ended projectile. This causes an approximately uniform contact pressure across the diameter of projectile. In contrast, for a hemi-spherical nose the level of pressure

inside the projectile is lower and the contact pressure is mainly localised over a small area at the centre of the plate (This area is growing as the contact area increases). As the time passes, the release waves are developed at the edges of the flat-fronted projectile ($t = 4 \mu\text{s}$) which then starts travelling towards its centre. This changes the shape of the contact pressure profile and reduces the pressure values around the edges. A second region of high intensity pressure is developed at the centre of the plate at $t = 6.5 \mu\text{s}$ for the flat-fronted projectile as soon as the release waves collide at the centre of the plate. The contact pressure now has its highest value (ca. 200 MPa) at the centre of the plate. In contrast, for the hemi-spherical nose, the peak pressure normally occurs at the edge of the contact (Field et al., 1985) and moving away from the centre of the plate as the time passes (Fig. 13). At $t = 8.5 \mu\text{s}$ a negative pressure is developed at the centre of the plate for the flat nose shape. The development of these tensile stresses and the resulting cavitation are also mentioned by Field et al. (1985). At this time, the contact pressure is nearly zero all over the plate except at a small area with ($x/r \sim 0.2$) where x is the distance from the centre of the plate and r is the radius of the projectile.

The level of maximum principal strain is compared for the two nose shapes in Fig. 14 at two locations inside the laminated glass target: location 1 and 2, which are the central points at the distal side of the front and rear glass layers respectively. In Figs. 14a and b the focus is on the early stage of the impact (the initial $50 \mu\text{s}$) while in Fig. 14c the performance is compared over a longer period ($500 \mu\text{s}$). As can be seen in Fig. 14a, the level of strain in the front glass layer significantly increases over a short period of time and reaches the value of approximately 1%. The initial negative value of strain is because of the elastic compression wave reaching the back of the plate before the plate starts bending. As can be observed the level of strain in location 1 is higher than location 2 and is more likely to be the fracture initiation point. This reflects what has been seen experimentally in Fig. 10, where the fracture always occurred earlier in the front glass layer. It should be mentioned that although the plate does not fail at

this impact velocity (131 m s^{-1}), the level of calculated strain already exceeds that of measured under quasi-static loading (ca. 0.8% (Mohagheghian et al., 2016)). This can be due to the higher failure strain of glass under dynamic loading. Nie et al. (2010) found that depending on the surface condition of the glass, dynamic failure strain can be five times greater than its quasi-static value. The strain at location 2 is compared in Fig. 14c between the two nose shapes. The existence of initial high intensity strains is apparent for the flat nose shape. The level of strain becomes more similar later on the deformation stage for the two nose shapes.

In addition to intensity of pressure in Stage 1, the other important factor which can have a significant influence on the growth of the surface micro-cracks is the duration of Stage 1 (Van Der Zwaag and Field, 1983). This itself is nose shape dependent and is shorter for a hemi-spherical nose (Wilbeck, 1978). In summary, the low intensity and short duration of Stage 1 for a hemi-spherical nose prevent the growth of micro-cracks and therefore the onset of failure in the front glass layer.

5-3 The effect of glass front type

In this section, the glass front layer is changed from a chemically to a thermally strengthened glass. Thermally strengthened glasses are manufactured using rapid cooling of glass above its glass transition temperature (Gardon, 1980). This generates residual stresses across the thickness, which have a lower compressive stress on the surface but higher tensile stress in the centre compared to a chemically strengthened glass. In general, thermally strengthened glasses are cheaper but have lower strength than chemically strengthened glasses (Gy, 2008).

The comparison between the two laminates is shown in Fig. 15 (Case 2 with thermally strengthened front glass layer compared with Case 1 with chemically strengthened front glass).

All other parameters, including the rear glass layer type, are kept the same. For the three velocities tested, the damage occurred only in the front glass layer. The value of the maximum strain in the rear glass layer in Fig. 15 is nearly the same for both cases. Therefore, the choice of glass type for the front layer has little influence on the threshold velocity at which the rear glass layer fails (second damage threshold in Fig. 10b). The front glass layer here acts as a sacrificial layer; breaks in the very early stage of the loading (Stage 1 in Fig. 9a) and protects the rest of the structure from premature failure, which is what happens for the thick monolithic glass in Fig. 8a.

5-4 The effect of the order of the glass layers

In the previous sections, a 2.2 mm thick glass layer was employed as the front glass layer. The effect of the order of the glass layers is investigated in this section by changing the layer orientation of Case 1 and placing the 4.0 mm glass layer facing the projectile. Fig. 16a shows a comparison between the damage caused by an impact at the velocity of 146 m s^{-1} for both cases. In the case of a thicker glass layer in the front, both glass layers are broken. At the same speed however, when the thinner glass layer is facing the projectile, fracture only appears in the front layer with no damage in the thick rear glass layer. This again supports the argument that the front layer is susceptible to failure by high intensity stresses generated early in the impact event (Stage I in Fig. 9). When the 2.2 mm glass layer is at the front, the glass fails almost instantaneously with the remainder of the impulse carried by a 4.0 mm rear glass layer. The same mechanism happens when the 4.0 mm glass is located in the front. The main difference is that in the latter case, the remaining load should be carried by a 2.2 mm rear glass layer which has a much lower load carrying capacity.

The strain development at the back of the front glass layer, obtained from simulation, is compared in Fig. 16b at the impact velocity of 131 m s^{-1} for these two configurations, thin glass layer in the front and at the back. The level of maximum strain in the initial phase of deformation is similar despite the difference in their front glass layer thickness. This confirms that positioning a thinner glass layer in front is more beneficial.

6-Conclusions

Deformation and damage mechanisms of laminated glass windows were investigated experimentally and numerically under high velocity soft impacts. Impact tests were performed using silicon rubber projectiles at a velocity range of $100\text{-}180 \text{ m s}^{-1}$. High-speed 3D digital image correlation was employed to monitor the deformation and strain at the back surface of the target and its results were validated using strain gauges. The simulations were performed in Abaqus/explicit using an Eulerian approach. The simulations were validated with the experimental results and good agreement was observed. From these research findings, the following conclusions are drawn:

- There are different phases identified for the deformation of a laminated glass window under a high velocity soft impact. Unlike the central out-of-plane displacement, the maximum strain in the centre of the rear glass layer occurs early in the impact as a result of highly localised deformation.
- The damage inflicted is sensitive to the nose shape of the projectile with a flat-fronted nose soft projectile being more damaging than a projectile with a hemi-spherical nose.
- Two impact velocity thresholds for damage are identified for a flat-fronted nose projectile. When the impact velocity exceeds the first threshold, the glass front layer

breaks. This damage occurs in the early stages of the hydrodynamic loading and has similar characteristics to that observed for liquid jet impact (Field, 1966). A combination of Rayleigh surface waves and localised bending stresses is believed to be responsible for the damage in this layer. The rear glass layer breaks when the impact velocity and the associated impulse transferred to the target is high enough to exceed a second higher threshold velocity. The fracture is initiated from the point of maximum flexural stress, which normally occurs at the centre of the specimen.

- The front glass layer in a laminated glass window acts as a sacrificial layer and protects the rest of the structure from premature failure. In contrast, for a thick monolithic glass, the damage made in the early stage of hydrodynamic loading causes the structure to lose its load carrying capacity at velocities much lower than that for the laminated glass.
- The order of the glass layers has a significant effect on the impact performance. Laminated glass with a thinner glass layer in the front outperforms the case when this layer is located at the back. In the former case, the thicker rear glass is protected from failure in the early stages and is able to carry the remainder of the load.
- The choice of glass front layer type (chemically versus thermally strengthened glass) is found to have no significant effect on the maximum strain in the rear-glass layer for soft impacts in the range of impact velocities investigated.
- Finally, the model developed represents well the experimentally determined deformation and strain response using 3D DIC. Knowledge of these deformations and associated strains is key in determining the onset of failure and so the model developed can provide the basis of a viable design tool for aircraft windshields in the future.

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References

- Abaqus Version 6.14, Dassault Systèmes Simulia Corp.
- Aramis, G.O.M., 2006. Theory and user manual.
- Barber, J., Taylor, H., Wilbeck, J., 1978. Bird impact forces and pressures on rigid and compliant targets. Tech. Rep. AFFDL-TR-77-60.
- Bourne, N.K., Obara, T., Field, J.E., 1997. High-speed photography and stress gauge studies of jet impact upon surfaces. *Philos. Trans. R. Soc. A Math. Phys. Eng. Sci.* 355, 607–623. doi:10.1098/rsta.1997.0028
- Bowden, F.P., Field, J.E., 1964. The Brittle Fracture of Solids by Liquid Impact, by Solid Impact, and by Shock. *Proc. R. Soc. A Math. Phys. Eng. Sci.* doi:10.1098/rspa.1964.0236
- Dear, J.P., Field, J.E., 1988. High-speed photography of surface geometry effects in liquid/solid impact. *J. Appl. Phys.* 63, 1015–1021. doi:10.1063/1.340000
- Dennis, N., Lyle, D., 2008. Bird Strike Damage & Windshield Bird Strike Final Report.
- Dolbeer, R., Wright, S., Weller, J., Begier, M., 2014. Wildlife strikes to civil aircraft in the United States 1990–2013, Federal Aviation Administration National Wildlife Strike Database.

609 Doubrava, R., Strnad, V., 2010. Bird strike analyses on the parts of aircraft structure, in:
610 Proceedings of the 27th Congress of the Aeronautical Sciences.

611 Field, J.E., 1966. Stress Waves, Deformation and Fracture Caused by Liquid Impact. Philos.
612 Trans. R. Soc. A Math. Phys. Eng. Sci. doi:10.1098/rsta.1966.0032

613 Field, J.E., Lesser, M.B., Dear, J.P., 1985. Studies of Two-Dimensional Liquid-Wedge Impact
614 and Their Relevance to Liquid-Drop Impact Problems. Proc. R. Soc. A Math. Phys. Eng.
615 Sci. doi:10.1098/rspa.1985.0096

616 Gardon, R., 1980. Elasticity and Strength in Glasses, in: Glass: Science and Technology Edited
617 by D. R. Uhlmann and N. J. Kreidl, Pp. pp. 146–216.

618 Grimaldi, A., Sollo, A., Guida, M., Marulo, F., 2013. Parametric study of a SPH high velocity
619 impact analysis – A birdstrike windshield application. Compos. Struct. 96, 616–630.
620 doi:10.1016/j.compstruct.2012.09.037

621 Gy, R., 2008. Ion exchange for glass strengthening. Mater. Sci. Eng. B Solid-State Mater. Adv.
622 Technol. 149, 159–165. doi:10.1016/j.mseb.2007.11.029

623 Hand, R.J., Field, J.E., 1990. Liquid impact on toughened glasses. Eng. Fract. Mech. 37, 293–
624 311. doi:10.1016/0013-7944(90)90042-F

625 Hedayati, R., Ziaei-Rad, S., Eyvazian, A., Hamouda, A.M., 2014. Bird strike analysis on a
626 typical helicopter windshield with different lay-ups. J. Mech. Sci. Technol. 28, 1381–
627 1392. doi:10.1007/s12206-014-0125-3

628 Heimbs, S., 2011. Computational methods for bird strike simulations: A review. Comput.
629 Struct. 89, 2093–2112. doi:10.1016/j.compstruc.2011.08.007

630 Iyama, H., Hamashima, H., Nishi, K., Itoh, S., 2009. Study on Expansion of a Silicon Tube by
631 Underwater Shock Wave. ASME 2009 Press. Vessel. Pip. Div. Conf. 1–5.
632 doi:10.1115/PVP2009-77502

633 Jin-Hyun, S., 1997. Ultrasonic properties of aluminosilicate glass and fluorosilicate glass-
634 ceramic.

635 Kangas, P., Pigman, G.L., 1948. Development of aircraft windshields to resist impact with
636 birds in flight part II~Investigation of windshield materials and methods of windshield
637 mounting, No. 4580001. SAE Technical paper. doi:10.4271/480001

638 Lesser, M., 1995. Thirty years of liquid impact research: a tutorial review. *Wear* 186-187, 28–
639 34. doi:10.1016/0043-1648(95)07190-3

640 Li, L., Yang, S.H., Hwang, C.S., Kim, Y.S., 2010. Effects of string tension and impact location
641 on tennis playing. *J. Mech. Sci. Technol.* 23, 2990–2997. doi:10.1007/s12206-009-0903-
642 5

643 Macaloney, N., Bujanda, A., Jensen, R., Goulbourne, N., 2007. Viscoelastic Characterization
644 of Aliphatic Polyurethane Interlayers. Army Res. Lab. Reprt No.; ARL-TR-4296.

645 Mohagheghian, I., Wang, Y., Jiang, L., Zhang, X., Yan, Y., Kinloch, A.J., Dear, J.P., 2016.
646 Structural performance of monolithic and laminated windows made of chemically
647 strengthened glass, submitted for publication.

648 Nie, X., Chen, W.W., Templeton, D.W., 2010. Dynamic ring-on-ring equibiaxial flexural
649 strength of borosilicate glass. *Int. J. Appl. Ceram. Technol.* 7, 616–624.
650 doi:10.1111/j.1744-7402.2010.02508.x

651 Schreier, H., Orteu, J.-J., Sutton, M. a., 2009. Image Correlation for Shape, Motion and
652 Deformation Measurements. Springer. doi:10.1007/978-0-387-78747-3

653 Thorpe, J., 2003. Fatalities and destroyed civil aircraft due to bird strikes, 1912-2002, in:
654 International Bird Strike Committee, 26th Meeting. pp. 1–28.

655 Van Der Zwaag, S., Field, J.E., 1983. Rain erosion damage in brittle materials. *Eng. Fract.*
656 *Mech.* 17, 367–379. doi:10.1016/0013-7944(83)90087-5

657 Walley, S.M., Field, J.E., Blair, P.W., Milford, a. J., 2004. The effect of temperature on the
658 impact behaviour of glass/polycarbonate laminates. *Int. J. Impact Eng.* 30, 31–53.
659 doi:10.1016/S0734-743X(03)00046-0

660 Wilbeck, J., 1978. Impact behavior of low strength projectiles. Tech. Rep. AFML-TR-77-134.

661 Wilbeck, J.S., Rand, J.L., 1981. The Development of a Substitute Bird Model. *J. Eng. Power*
662 103, 725. doi:10.1115/1.3230795

663 Xue, L., Coble, C.R., Lee, H., Yu, D., Chaparala, S., Park, S., 2013. Dynamic Analysis of Thin
664 Glass Under Ball Drop Impact With New Metrics, in: Volume 1: Advanced Packaging;
665 Emerging Technologies; Modeling and Simulation; Multi-Physics Based Reliability;
666 MEMS and NEMS; Materials and Processes. ASME, p. V001T03A006.
667 doi:10.1115/IPACK2013-73291

668

669 **Figure Captions:**

670 Figure 1: (a) Manufacture of a thin-walled, light weight carrier for a silicon rubber projectile
671 (diameter of 23.5 mm and length of 50 mm). (b) Interaction of the projectile and its carrier with
672 a 5 mm aluminium alloy plate. The plate is painted black to visualise the deformation of the
673 projectile.

674 Figure 2: Schematic of (a) Gas gun and 3D Digital Image Correlation (DIC) setup; (b) clamping
675 used for gas gun experiments and (c) two types of sample configuration prepared.

676 Figure 3: Finite element models used for (a) long-period (1 ms) and (b) short-period (50 μ s)
677 simulations.

678 Figure 4: Soft impact results of a laminated glass window (Case 2-Configuration I) at the
679 velocity of 170 m s^{-1} : (a) shows the projectile deformation; (b) and (c) display the out-of-plane
680 displacement and major principal strain contours over the observation area, calculated using
681 DIC.

682 Figure 5: Soft impact results of a laminated glass window (Case 2-Configuration II) at the
683 velocity of 174 m s^{-1} : (a) and (b) display the out-of-plane displacement and major principal
684 strain contours over the observation area, calculated using DIC; (c) shows the history of central
685 out-of-plane displacement and major principal strain (Markers in (c) are 25 μ s apart and each
686 corresponds to a contour plot in (a) and (b)); (d) and (e) are the plate profile displacing and
687 rebounding respectively; each profile in (d) and (e) corresponds to a contour plot in (a).

688 Figure 6: Comparison between the strain obtained from strain gauges and that calculated by
689 DIC for a laminated glass plate (Case 2-Configuration II) impacted at velocity of 174 m s^{-1} .

690 Figure 7: Damage in a laminated glass specimen (Case 2) impacted at velocity of 174 m s^{-1} .
691 The impacted side of the specimen was painted black prior to the impact. White in the left
692 photograph (front view) is an indicator for area in which glass fragment detached from the
693 plate.

694 Figure 8: High-speed image sequences for deformation and failure of: (a) 6.0 mm monolithic
695 chemically strengthened glass impacted at the velocity of 144 m s^{-1} ; (b) laminated glass plate
696 (Case 1) impacted at the velocity of 160 m s^{-1} .

697 Figure 9: Schematic of: (a) typical high velocity soft impact loading and (b-e) different
698 mechanisms and stages of damage in a laminated glass window.

699 Figure 10: Impact performance of laminated glass (Case 1) at various velocities (a)
700 Photographs of damaged laminated glass, taken from impacted side; (b) Maximum strain
701 obtained from the distal side of rear glass layer at the centre of the specimen, calculated by DIC
702 (solid symbols), and at 30 mm off-centre, from strain gauge (open symbols), against projectile
703 initial velocity; (c) Strain traces of the off-centre gauge at various impact velocities.

704 Figure 11: Comparison between experimental and simulation results for an impact by a hemi-
705 spherical projectile on a laminated glass window (Case 1) at the velocity of 158 ms^{-1} . The
706 comparison is made for (a) plate deformation profile, (b) central out-of-plane displacement and
707 major strain and (c) strain in y-direction at the location of the gauge (30 mm off-centre).

708 Figure 12: Maximum strain at the distal side of rear glass layer against projectile initial velocity
709 for two projectile nose shapes: flat and hemi-spherical. Solid symbols are the maximum major
710 strain at the centre of the specimen calculated by DIC and open symbols are the maximum
711 strain at 30 mm off-centre obtained from strain gauge.

Figure 13: Finite element simulation result of impact by a flat and hemi-spherical nose projectile on a laminated glass window (Case 1) at the velocity of 131 ms^{-1} . (Left) displays the pressure development inside the projectile and (Right) displays the imposed contact pressure on the front glass layer.

Figure 14: Strain development in two central locations: the back side of the front glass layer (location 1) and the back side of the rear glass layer (location 2) for an impact on a laminated glass window (Case 1) at the velocity of 131 ms^{-1} . (a) and (b) shows the strain development in the first $50 \mu\text{s}$ of impact at the two locations for a flat and hemi-spherical nose respectively. (c) compares the strain development at location 2 for the two nose shapes over the longer period ($500 \mu\text{s}$).

Figure 15: Maximum strain at the distal side of rear glass layer against projectile initial velocity for two laminates with thermally and chemically strengthened glass in the front. Solid symbols are the maximum major strain at the centre of the specimen calculated by DIC and open symbols are the maximum strain at 30 mm off-centre obtained from strain gauge.

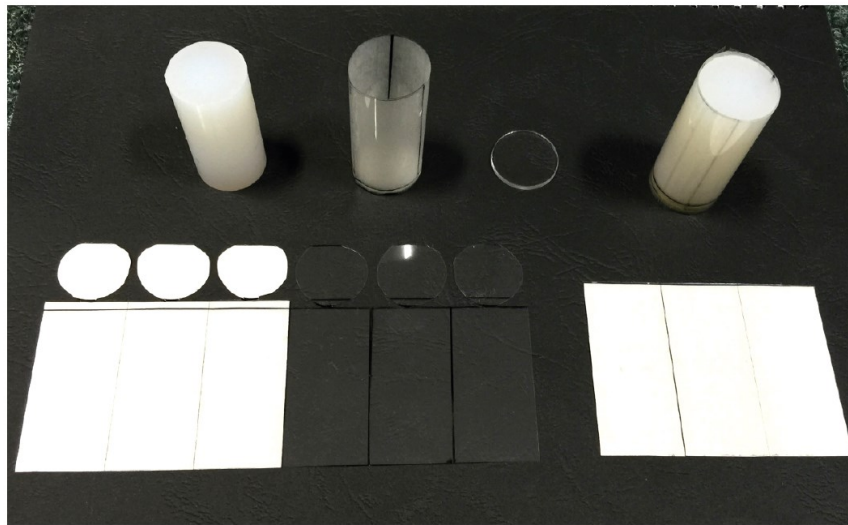
Figure 16: The effect of glass layer orientation on (a) impact damage of a laminated glass window (Case 1) at the velocity of 146 m s^{-1} and (b) strain development in the centre point at the back-side of the front glass layer. The results are obtained from finite element simulations.

Table 1: Different configurations of samples used in this investigation.

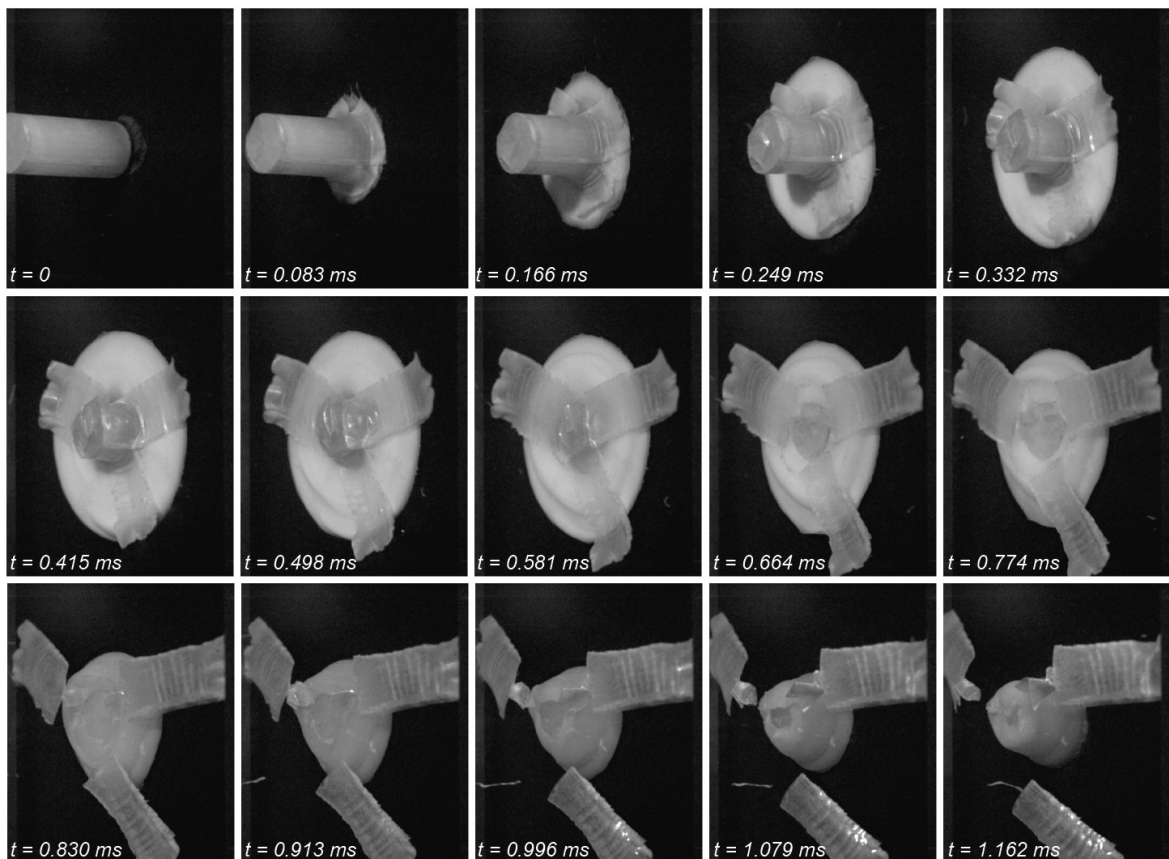
Table 2: Prony series material constants extracted for TPU (KRYSTALFEX®PE499).

733 **Figure 1:**

(a)



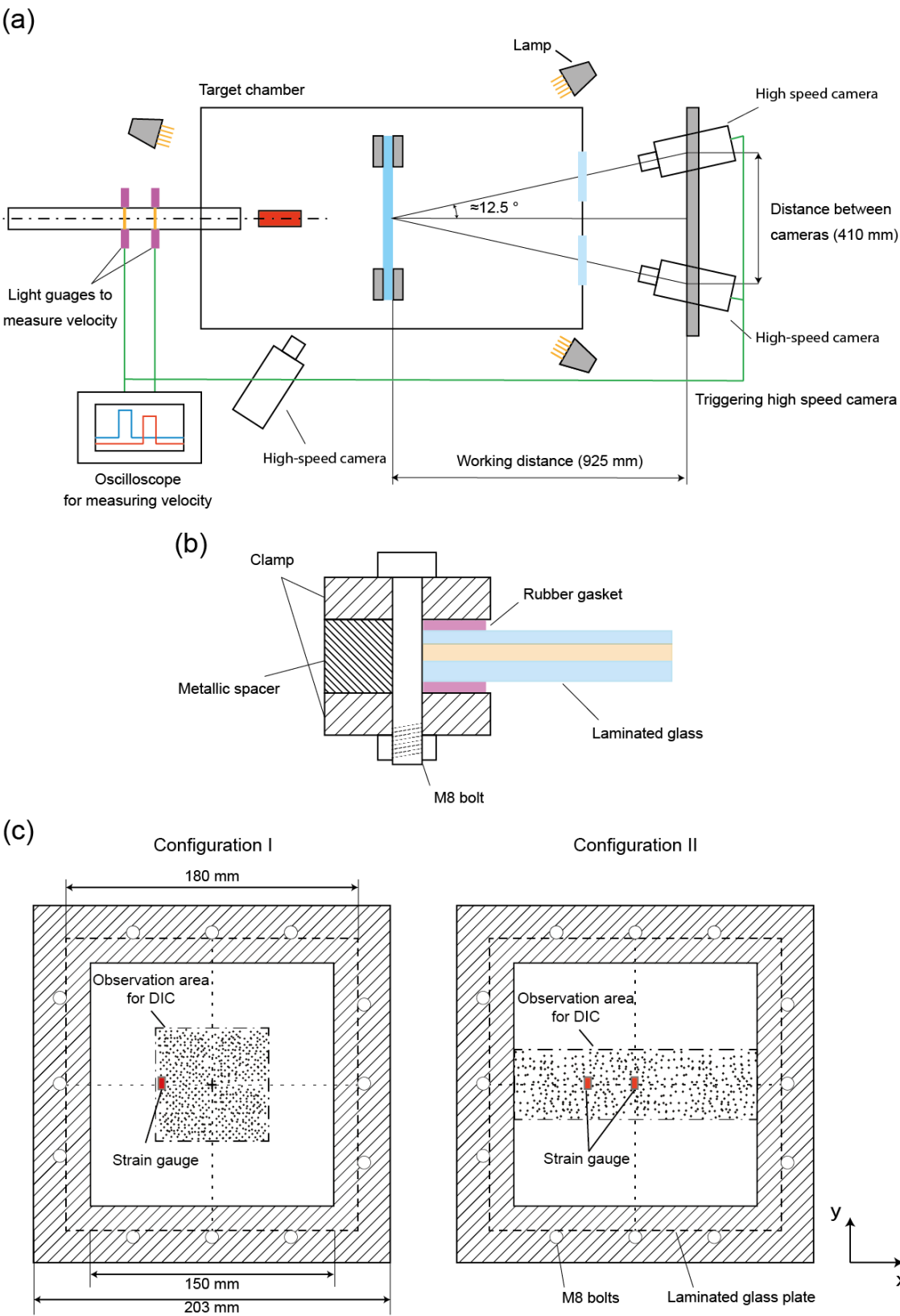
(b)



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736 **Figure 2:**



737

738

739 **Figure 3:**

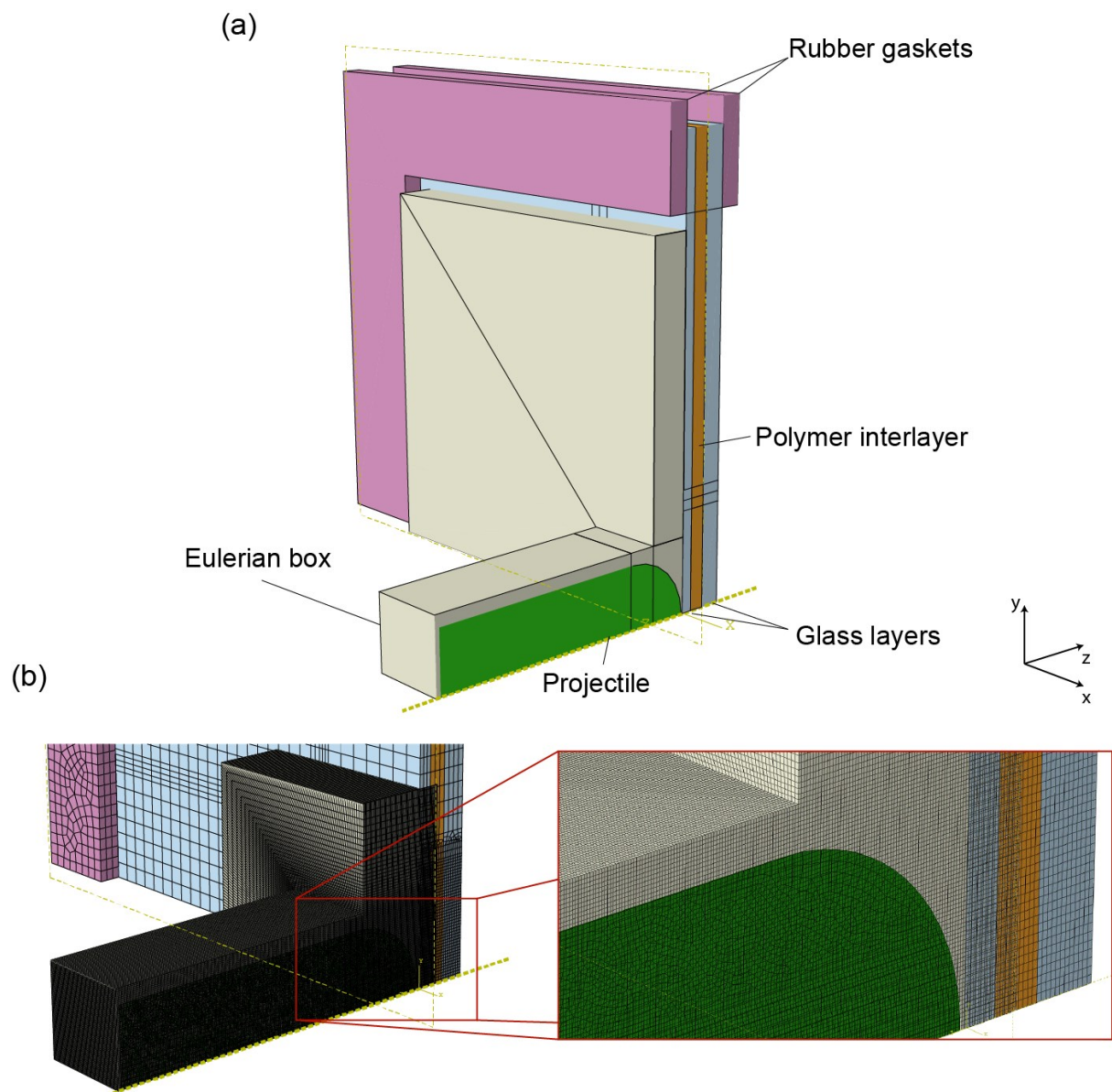
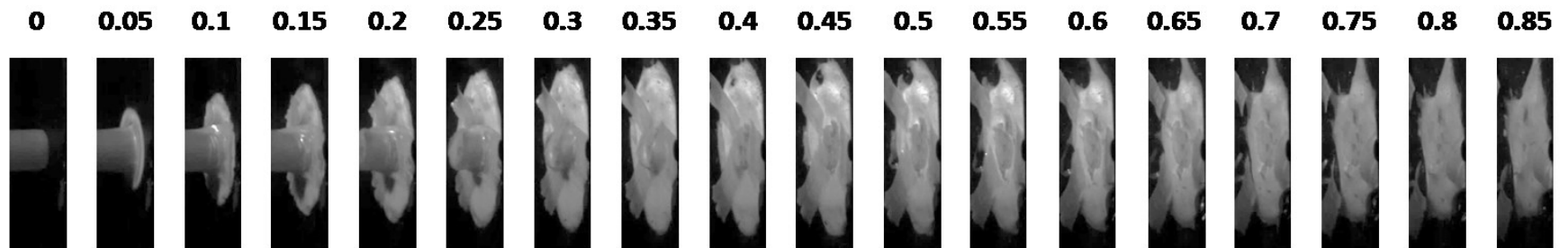
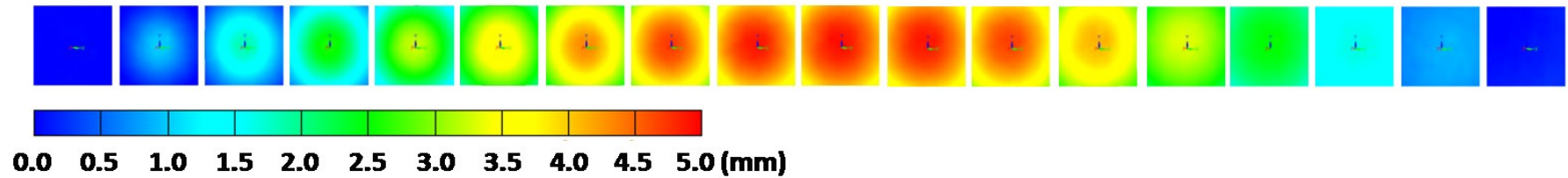


Figure 4:

(a) Time (ms)



(b) Out of plane displacement (mm)



(c) Major principal strain (%)

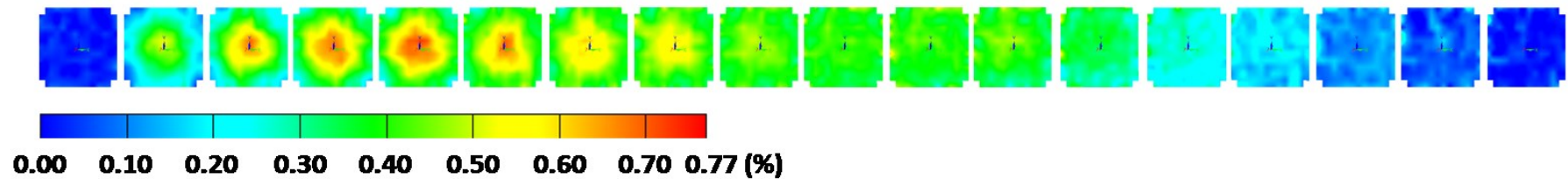


Figure 5:

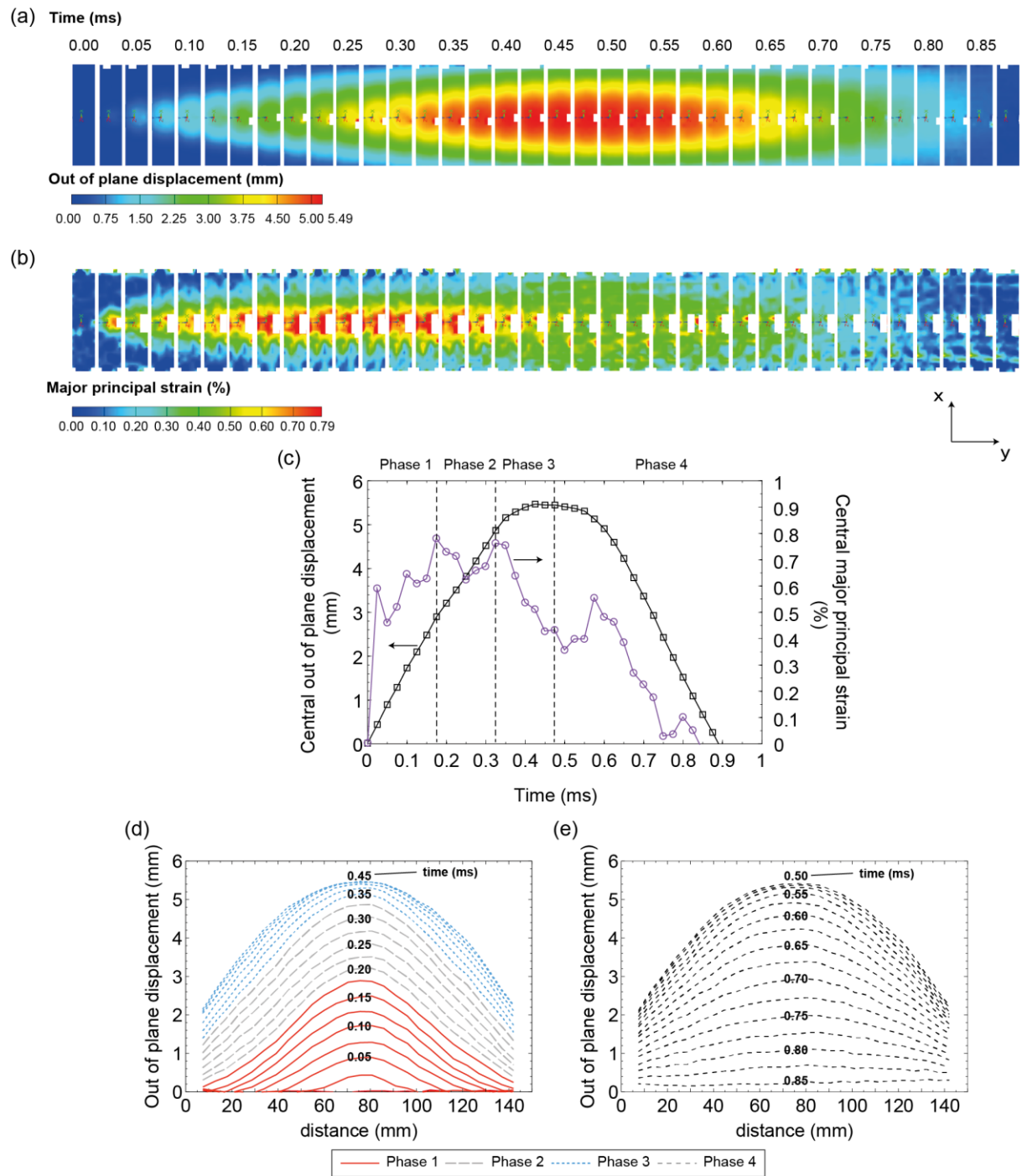


Figure 6:

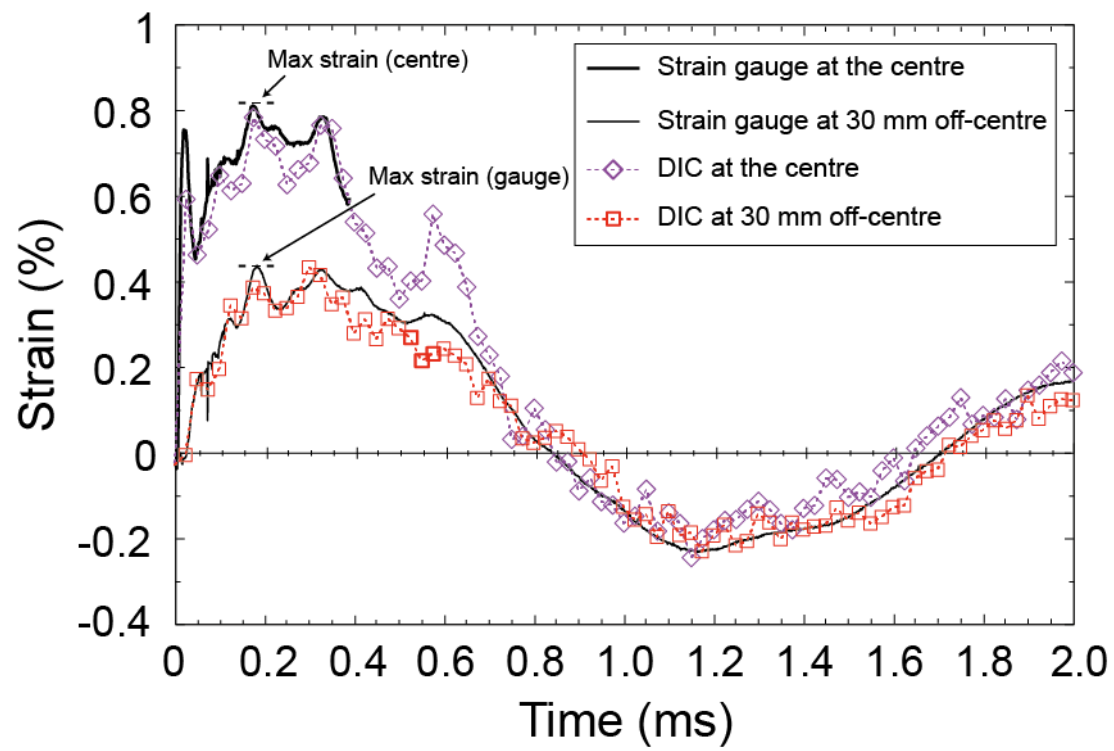


Figure 7:

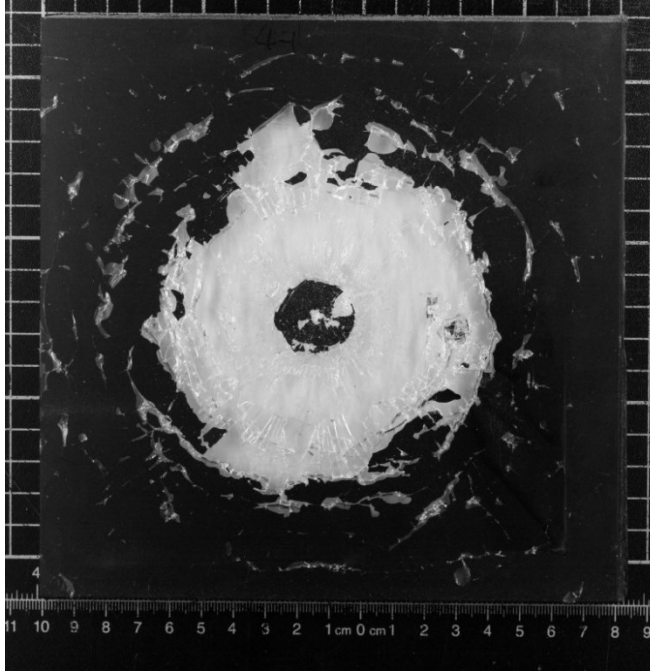
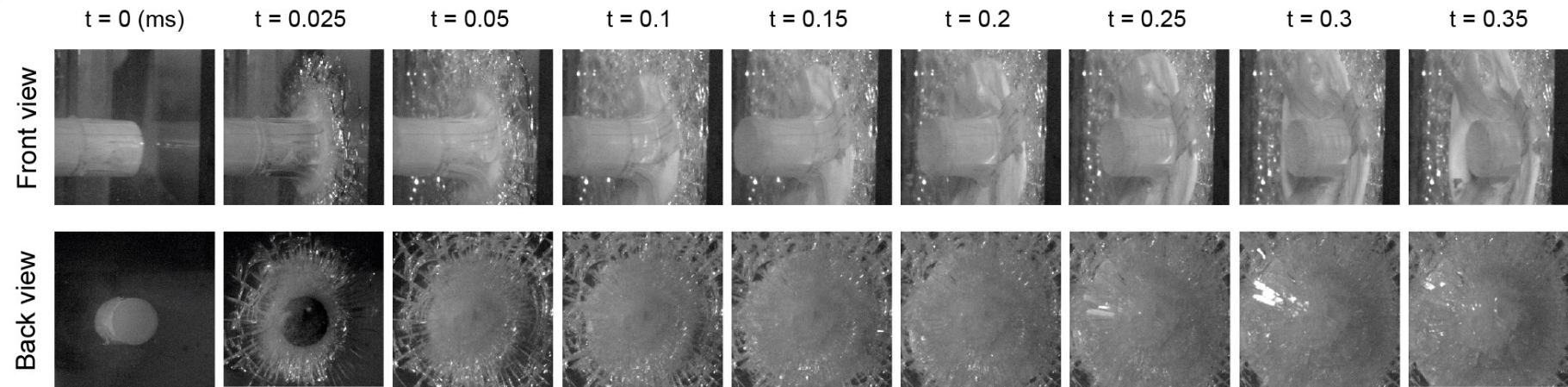


Figure 8:

(a)



(b)

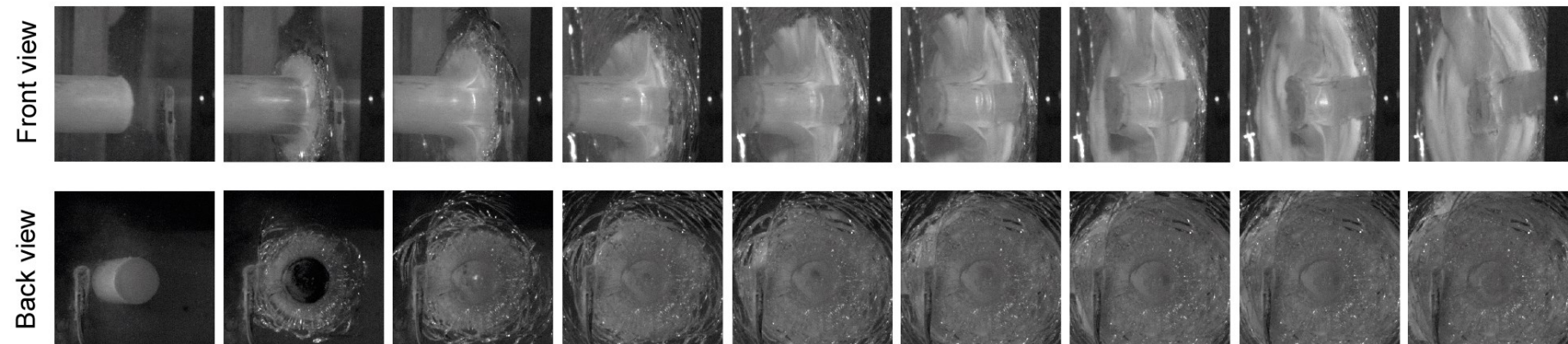


Figure 9:

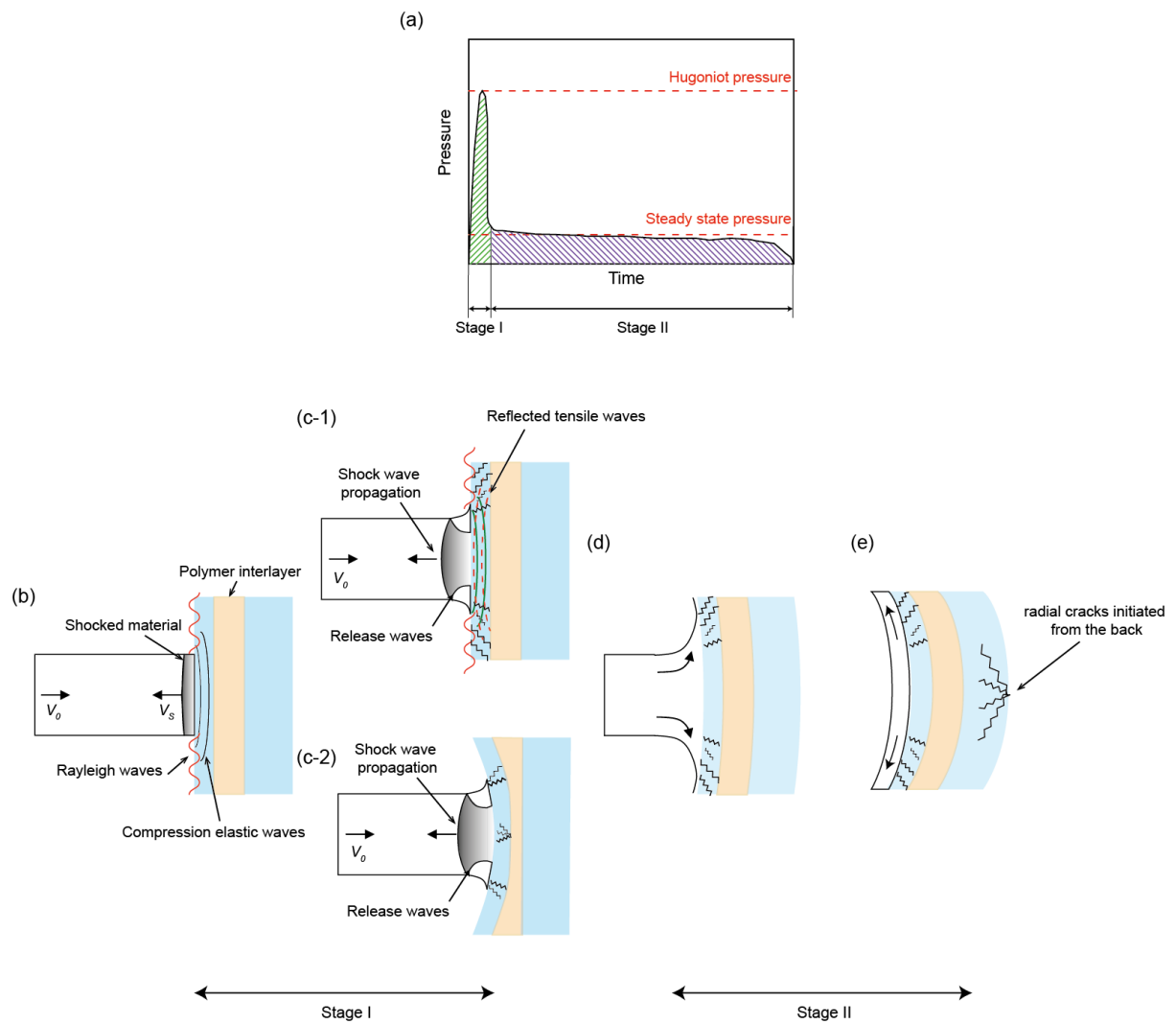


Figure 10

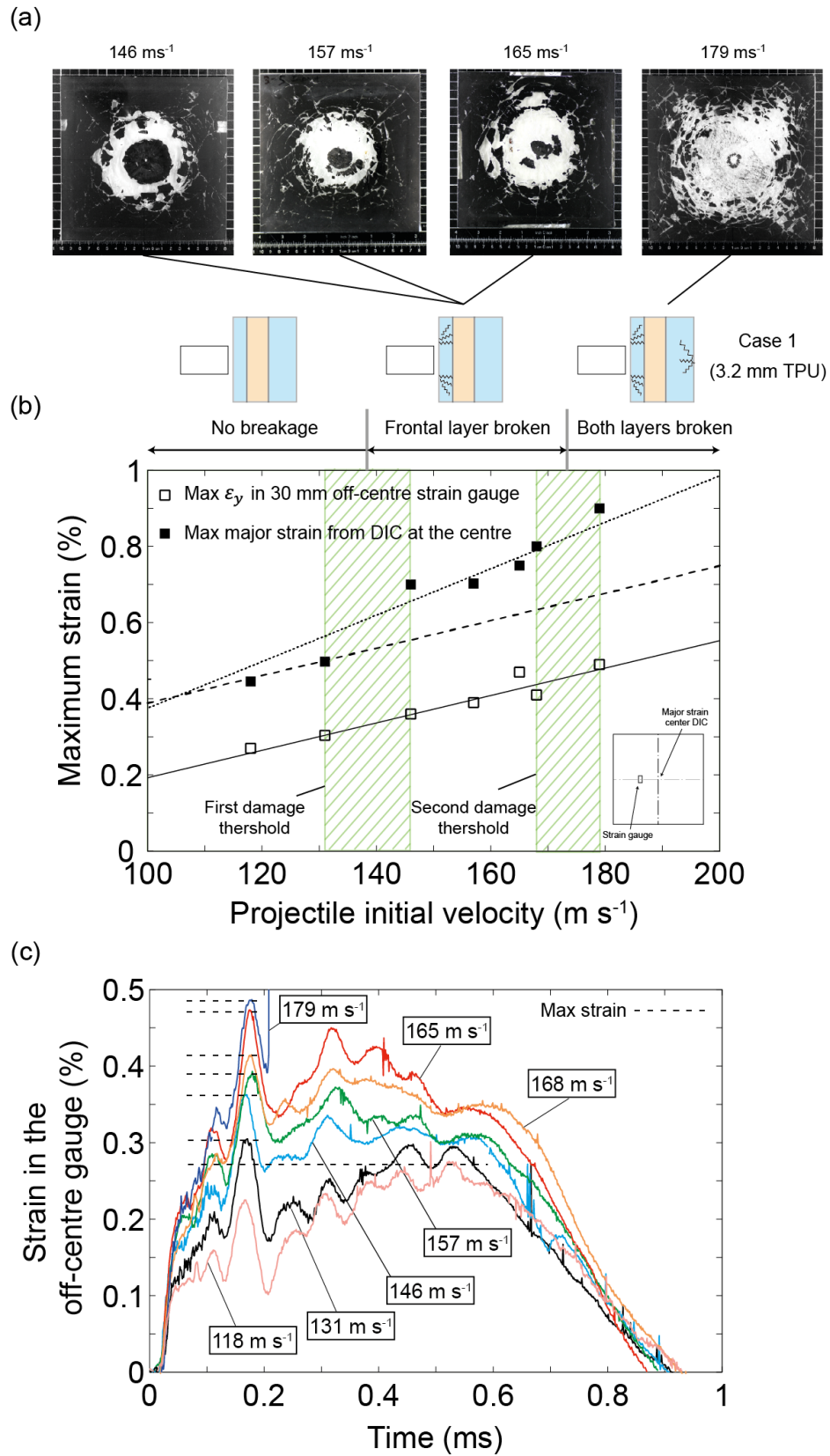


Figure 11:

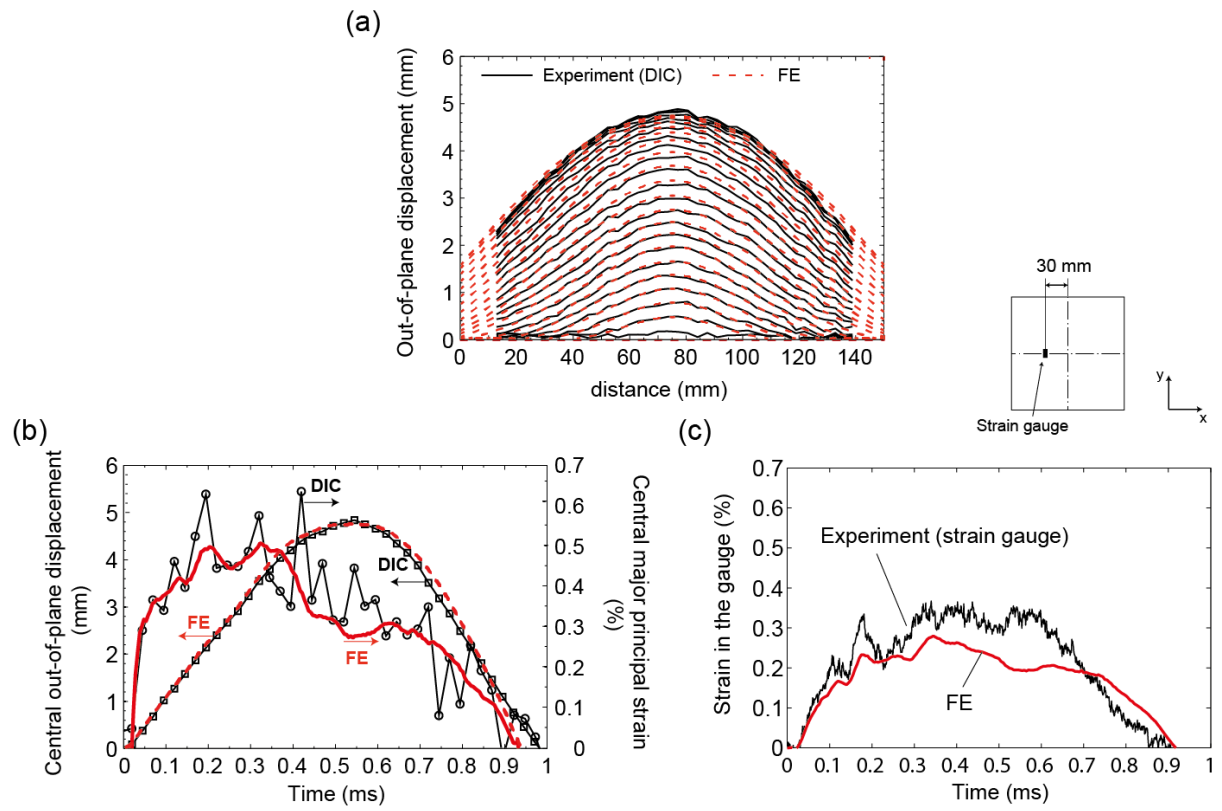


Figure 12:

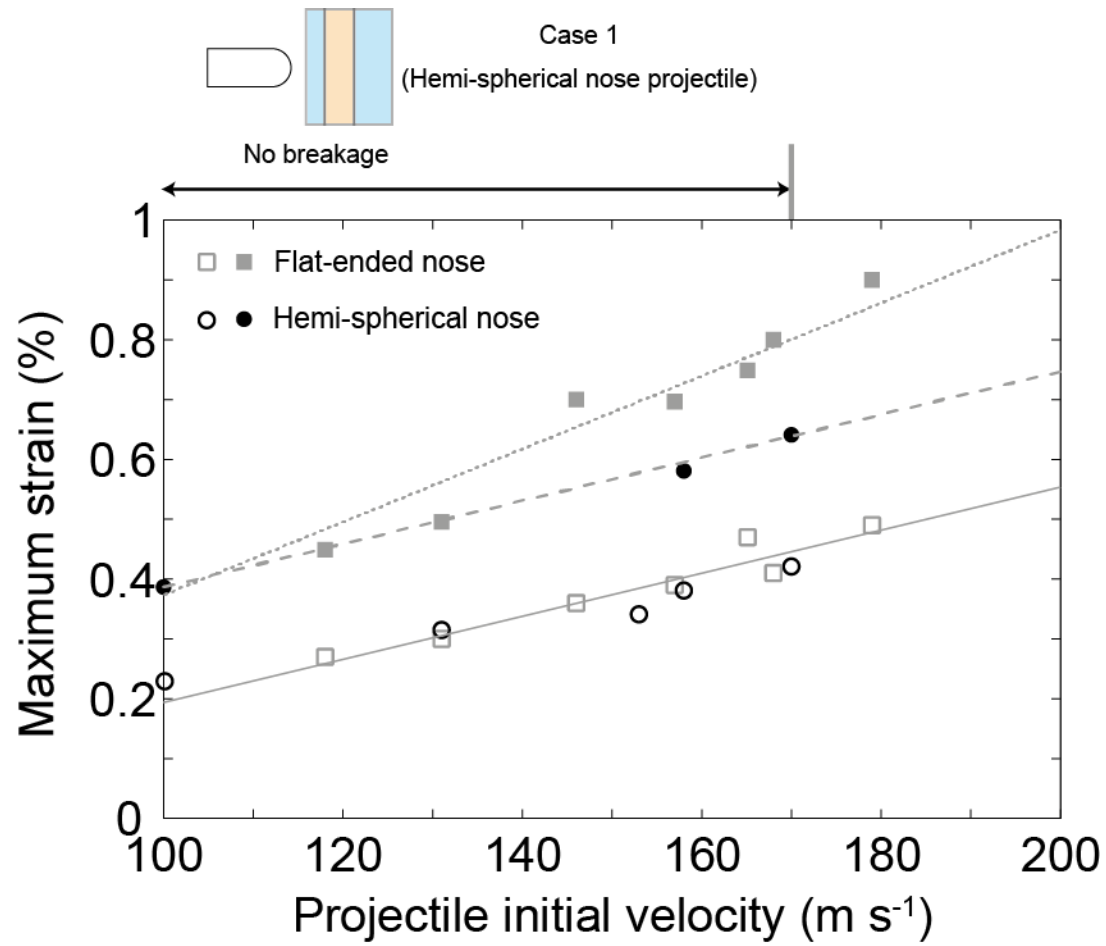


Figure 13:

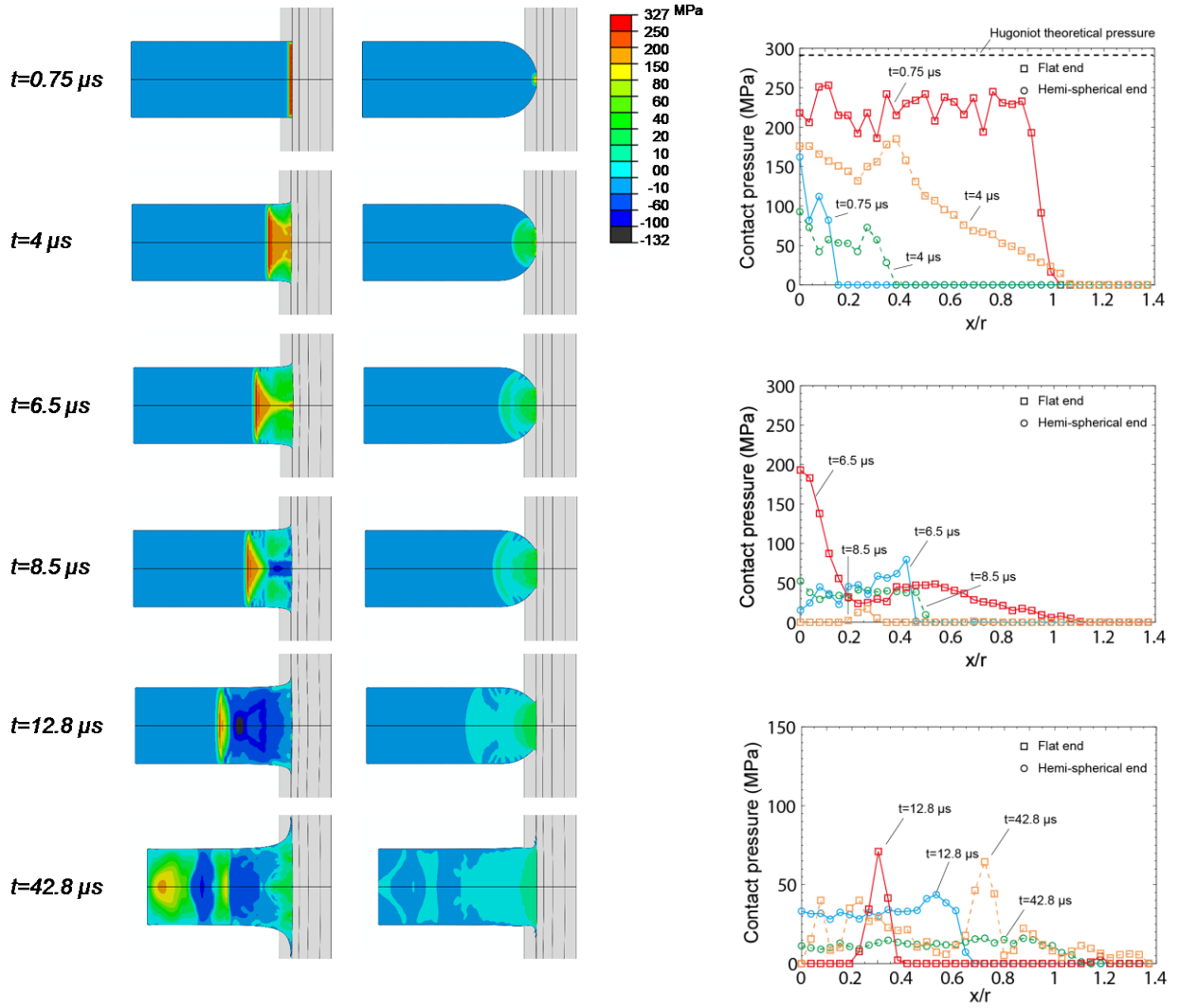


Figure 14:

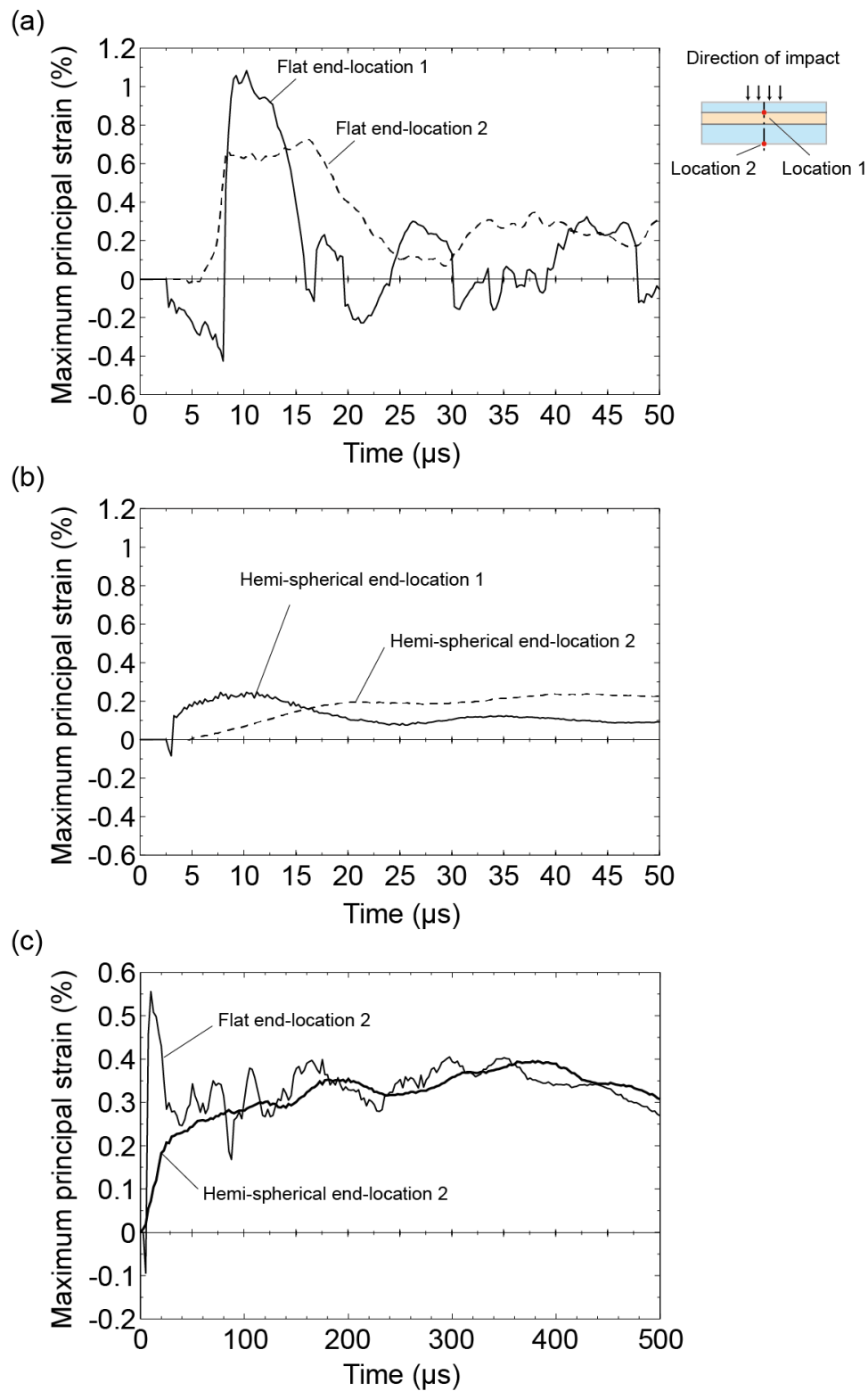


Figure 15:

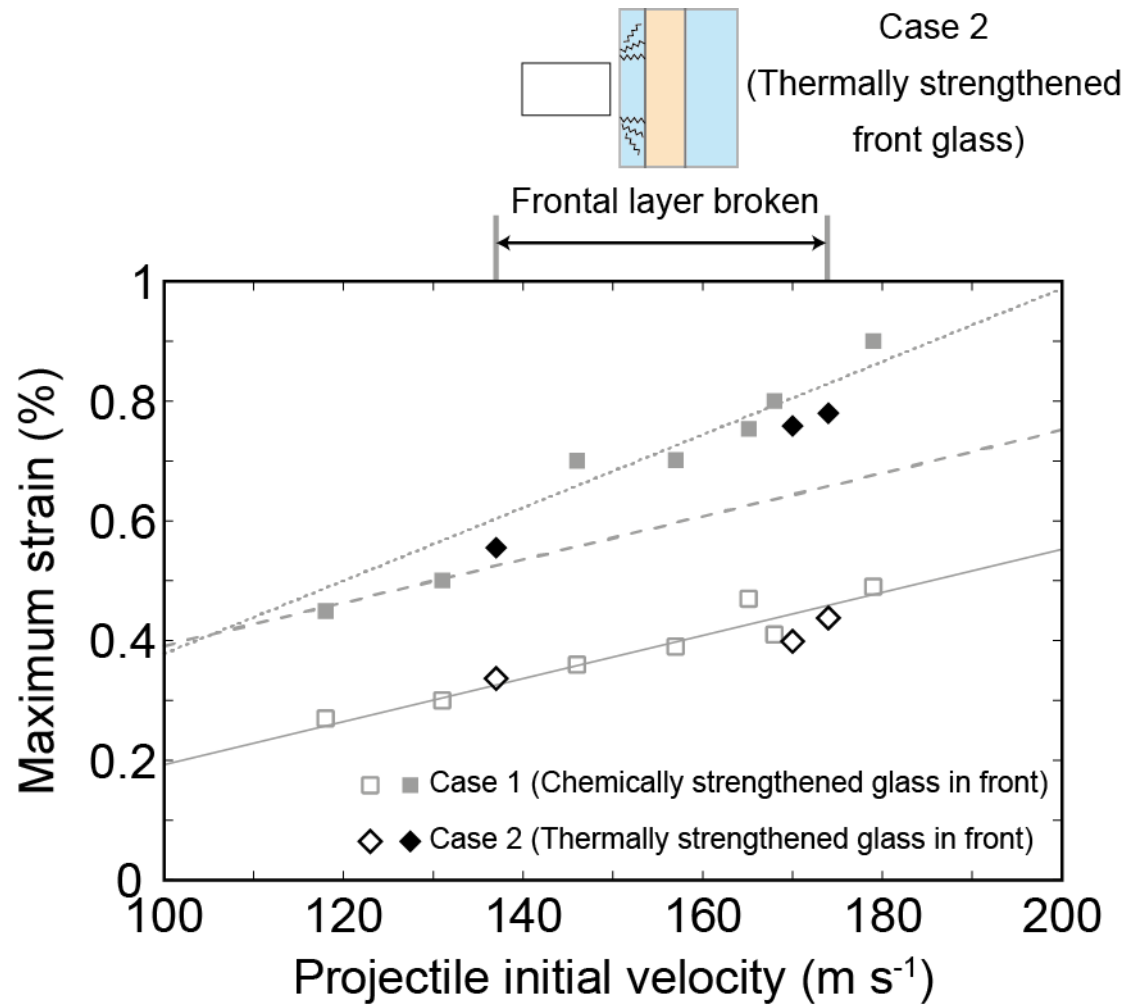


Figure 16:

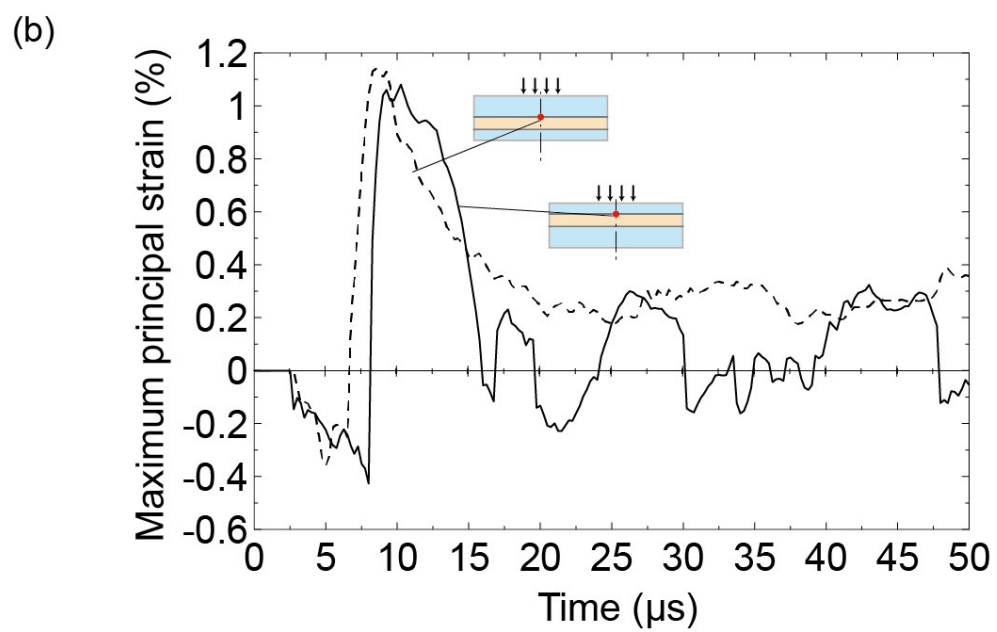
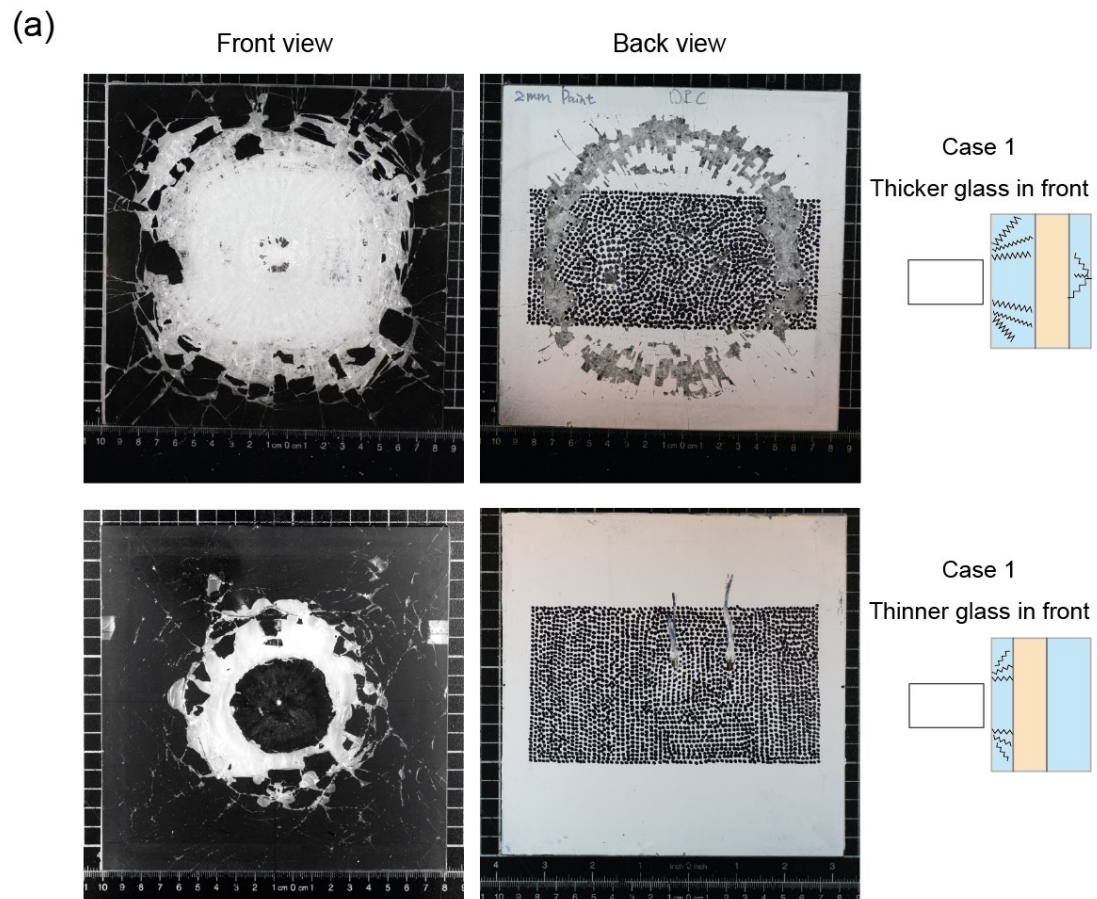


Table 1

Configuration	Glass and polymer layers	Average plate thickness (mm)	Metallic spacer size (mm)
Case 1	2.2 mm CT ⁽ⁱ⁾ /1.27+1.91 mm TPU ⁽ⁱⁱ⁾ /4.0 mm CT	9.2	17.1
Case 2	2.2 mm TT ⁽ⁱⁱⁱ⁾ /1.27+1.91mm TPU/4.0 mm CT	9.4	17.1
Case 3	6.0 mm CT	6.0	13.8

- (i) Chemically toughened glass
- (ii) Thermoplastic Polyurethane (KRYSTALFEX®PE499)
- (iii) Thermally toughened glass

Table 2

	G_i / G_o^*	$\tau_i (s)$
1	0.44231	10^{-10}
2	0.11511	10^{-9}
3	0.17258	10^{-8}
4	0.08917	10^{-7}
5	0.07606	10^{-6}
6	0.04828	10^{-5}
7	0.02867	10^{-4}
8	0.01444	10^{-30}
9	0.00611	10^{-2}
10	0.00249	10^{-1}

11	0.00097	10^0
12	0.00058	10^{+1}
13	0.00050	10^{+2}
14	0.00041	10^{+3}
15	0.00036	10^{+4}
16	0.00029	10^{+5}
17	0.00016	10^{+6}
Long-term	0.001404	Infinity

* G_o is the instantaneous shear modulus and its value is equal to: $G_{\infty} + \sum_{i=1}^n G_i$



Characterisation of composite-titanium alloy hybrid joints using digital image correlation



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ABSTRACT

An advanced Comeld hybrid joining technology for joining Ti6Al4V and carbon fibre reinforced polymer (CFRP) is presented. The Comeld joining method utilises protrusions formed on the surface of the metal by electron beam surface treatment which are then embedded into composite layers to form an integrated joint by co-curing. In this paper, joints were formed with wedge profiled protrusions on the metal surface and during tensile testing the protrusions resisted fracture in all tests, leading to failure in the composite part. A digital image correlation (DIC) technique was used to monitor full field, in-plane strain during the tests and to also identify the development of failure modes. The results from specimens with protrusions exposed on the side surface by grinding showed that the embedded protrusions are effective in reducing stress concentration near the metal composite interface and therefore contributed to improved mechanical properties of the hybrid joints.

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1. Introduction

Key design requirements for aircraft structures including a high strength to weight ratio, the need to tailor properties in specific directions, durability and corrosion resistance ensure that carbon fibre reinforced polymer (CFRP) composites will continue to be used in increasing quantities. An example of this trend is the Airbus A380 where 25% of the aircraft is manufactured using composite materials and this figure will increase to over 50% for the Airbus A350, including elements such as the wings and fuselage [1].

One of the most important reasons for the increased use of composites is that integrated structures can be manufactured with the potential benefits of reduced manufacturing costs and time for the assembly process. In some cases, due to the requirement of high stiffness and ductility or impact resistance, composites are combined with metal alloys to meet the requirements. This has led to the design of metal to composite hybrid structures. As a result, it has become necessary to understand how to match dissimilar materials and reduce local stress concentrations for various geometries and material discontinuities [2].

Traditional joining methods such as adhesive bonding and mechanical joining (riveting and bolting) have been researched

for many years [3–6], including stress and fracture mechanics analyses. Now traditional joints are widely used in many different applications. The development of metal processing technologies and an increase in advanced surface treatment technologies have increased the potential for hybrid joining technologies between composite to metal. For example, highly efficient electron beam micro-processing can tailor different surface patterns in many materials, known as Surfi-Sculpt processing by the Welding Institute (TWI) [7–9], and its application in joining metal to composite, also well known as Comeld™ [10–12]. Other research has already been conducted by using arc welding to manufacture protrusions on stainless steel (cold-metal transfer, CMT) and to realise hybrid joining [13], or by using additive manufacturing for the technology of hybrid penetrative reinforcement (HYPER) [14,15]. Hybrid joining research has also become a significant research topic recently for further applications in the aircraft industry. Compared to other hybrid joining technologies, there are several advantages of Comeld hybrid joining. Different patterns and protrusion sizes can be generated efficiently on the surface of the metal by the electron beam Surfi-Sculpt process. The mechanical properties of the protrusions are often improved after the thermal process of melting and solidification. The vacuum environment also helps to further improve the surface quality of the work-piece and the requirement for the formation of surface oxide layers in titanium alloys can be avoided. The effect of processing parameters and

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the resulting mechanical properties of Comeld structures have quickly become the key issue in order to develop this technology. Some work has been reported with preliminary results from experiments and from numerical simulations [10–12,16]. DIC is currently one of the most effective methods of measuring strain, and it was found to be very effective in monitoring the damage development in composite structures [17]. In the present work, DIC has been used to measure the full field strain distributions across hybrid joints and detailed strain maps have been measured across the composite-metal interface and around protrusions exposed by grinding.

2. Materials and procedures

The hybrid joints investigated here consist of a metal part, Ti6Al4V titanium alloy, and a composite part made of carbon fibre reinforced polymer (CFRP). The CFRP used in this study was Hex-Ply® 8552/33%/UD268/IM7 (12 K) unidirectional carbon fibre prepreg plies manufactured by Hexcel. This included a commercial epoxy 8552, which is a high performance, tough epoxy matrix for use in primary aerospace structures, and IM7 carbon fibre, which is a continuous, high performance, intermediate modulus, PAN based fibre in the form of 12,000 (12 K) filament count tows. The material data is given in Table 1 [18].

The hybrid joint was tailored as shown in Fig. 1, which was a single step double-lap. Fig. 1 shows the dimensions of the metal part, with two overlapped areas of $33 \times 30 \text{ mm}^2$. The metal part was surface treated by Surfi-Sculpt, as is shown in Fig. 2. The surfaces were manufactured using an electron beam processing machine (ZD150-15MH), with an accelerating voltage of 150 kV. Beam currents of 1.5 mA, 3.5 mA and 5 mA were investigated as were scanning frequencies between 500 and 4500 Hz as was reported in [12]. From that study, the parameters of 3 mA, a scanning frequency of 1500 Hz with a processing time of 11 s were selected as those produced protrusions with greatest height at optimal speed. The scanning waveform produced protrusions in a linear array of (23×8) . The resulting protrusions were wedge shaped and their dimensions were: height, $h = 1.2 \text{ mm}$, angle, $\theta_L = 90^\circ$, $\theta_R = 45^\circ$, top width, $w_t = 0.4 \text{ mm}$, base width, $w_b = 1.2 \text{ mm}$, thickness, $\delta = 0.4 \text{ mm}$, distribution density, $\sigma = 18 \text{ pins/cm}^2$, $\Delta x = 3.6 \text{ mm}$ (the nearest distance between two protrusions along x direction); $\Delta y = 1.2 \text{ mm}$ (the nearest distance between two protrusions along y direction). An important feature of these wedge shaped protrusions is that they have greater resistance to fracture at their base due to their greater base width.

Fig. 3 shows a schematic configuration of the joint. The composite parts were comprised of three sections, i.e. the upper (3.5 mm), middle (4 mm), and lower (3.5 mm) sections. The whole joint was symmetrical around the overlapped area and along the mid-section. According to the cured ply thickness (0.25 mm) in Table 1, the upper and lower sections consist of 14 plies with the following layup $[-45/45/0/45/90/-45/0]_{\text{sym}}$, whilst the middle section was designed with 16 plies with the layup $[0/-45/45/90/45/0/-45/0]_{\text{sym}}$. Fig. 4 shows the joints ready to be cured in the autoclave after laying up and joining. The curing of the joint was performed in an autoclave at a pressure of 7 bar and a temperature of 180°C with control vacuum of 0.2 bar. A water jet machine was used to cut

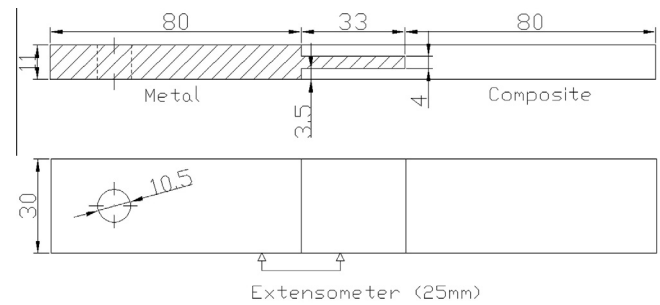


Fig. 1. Dimension of Comeld hybrid joint design.

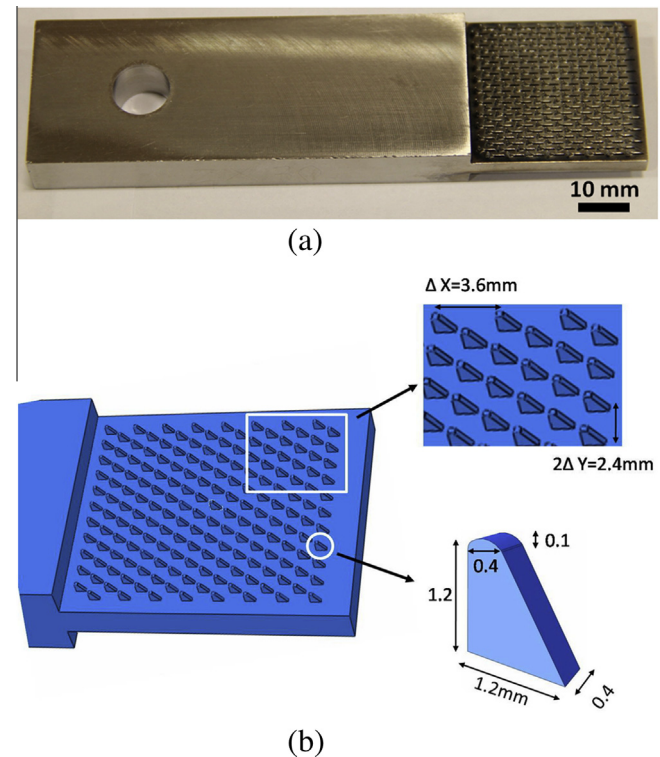


Fig. 2. Surfi-Sculpted metal part specification (a) photograph and (b) detail of protrusions (Note: The protrusions have a left side angle $\theta_L = 90^\circ$, and a right side angle $\theta_R = 45^\circ$).

out the specimens from the cured plate and a diamond impregnated grinding wheel was used to grind the specimens into the final dimension (Fig. 1), including standard specimens (Fig. 5a) and ground specimens (Fig. 5b), which had a row of protrusions exposed on the side of the specimen.

The tensile testing of the joints was conducted under quasi-static conditions on a 250 kN tensile testing machine. All the samples were tested at a constant cross-head displacement (extension) rate of 1 mm/min at ambient temperature and humidity (23 °C, 50%RH). An ARAMISv6.3.0 Digital image correlation system (GOM mbH, Germany) was used to obtain real time full field surface

Table 1
Specification and properties of 8552/IM7 composite.

Resin content (%)	Fibre weight (g/m ²)	Resin weight (g/m ²)	Cured ply thickness (mm)	Tensile modulus GPa (msi) E_{11} $E_{22} = E_{33}$	Shear modulus GPa (msi) $G_{12} = G_{13}$ G_{23}	Poisson's ratio $\nu_{12} = \nu_{13}$ ν_{23}
33	268	132	0.251	154 (22.3) 8.96 (1.3)	5.31 (0.77) 2.96 (0.43)	0.32 0.5

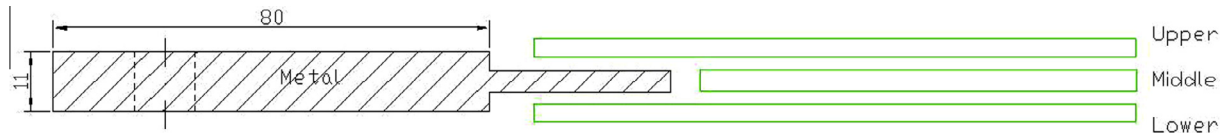


Fig. 3. Schematic of the Comeld joining method.



Fig. 4. Specimens ready to be cured after laying up.

strain data, which uses digital images recorded at set time intervals during tensile testing to determine the surface deformation and strain of a target over time. The speed of image recording was one image per second.

To ensure the accuracy of computation, the facet size and facet step were set up according to the configuration with the average speckle diameter (aerosol spray) and the resolution of the image. Validation of the ARAMIS system was performed by comparing the accuracy of the ARAMIS strain evaluation against strain measured using an extensometer. The DIC strain measurement is based on the distribution of random speckles on a specimen surface [19]. The random speckles are generated using black and white spray-paints. Fig. 6 shows the specimen surface after speckling with black and white paints. The average speckle diameter (aerosol spray) was about 0.1 mm. The extensometer (Instron 2630-107, with a gauge length of 25 mm and maximum strain in the range between -10% and $+100\%$) was also used for recording the strain. One of the Comeld hybrid joint specimens was loaded to failure to perform this validation.

3. Results and discussion

3.1. Tensile testing results

Seven Comeld joint specimens were tested and the results are shown in Table 2. The first five specimens were standard

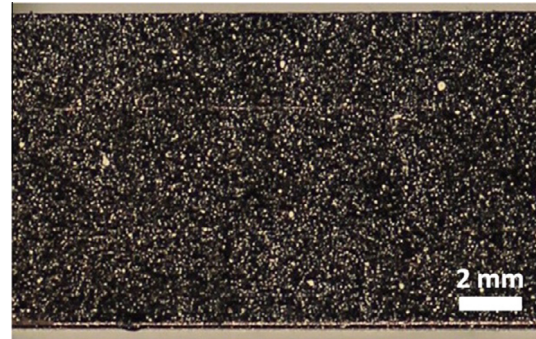


Fig. 6. The surface of the specimen after application of the speckle pattern.

specimens, as shown in Fig. 5(a), whilst the last two specimens (GR1, GR2) were ground on the sides to monitor the strain field around the area with exposed protrusions directly when testing. The final width of these ground specimens was 27.4 mm; about 1.3 mm was ground from each side compared to the standard samples. The tensile stress, σ is given by F/A_0 where F denotes force, and A_0 denotes cross sectional area (i.e. $30 \text{ mm} \times 11 \text{ mm}$), σ_f is the failure stress. The shear stress, τ is given by F/A_i , where A_i denotes overlapped area, consisting of a width of 30 mm and overlapped length of 33 mm on double sides. Compared with the ground specimens the standard specimens had higher values of σ_f and τ . This is because the exposed protrusions led to somewhat earlier crack initiation on the surface of the specimens as will be discussed in more detail later.

Fig. 7 shows the loading history for the Comeld joints up to failure. The curve shows a linear trend, similar to CFRP laminate. The final failure mode in the joints, shown in Fig. 8, it is clear that failure occurred within the CFRP laminate, and some plies remained on the overlapped area where the protrusions mechanically interlocked with the composite. Hence, the joints showed a similar mechanical behaviour to bulk CFRP laminate. The optimised protrusion shape and dimension greatly improved the properties of the joining interface between the metal and the composite. As titanium alloy (Ti6Al4V) has higher tensile strength than, for example, stainless steel, the joints investigated in the present work have a greater scope for improvement as the stronger protrusions can carry a greater load [13]. Further analysis of failure is discussed in Section 3.3. Both the ARAMIS DIC system and an extensometer

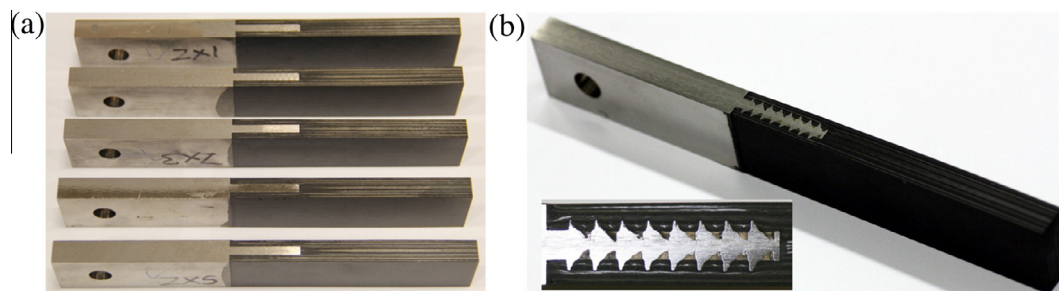
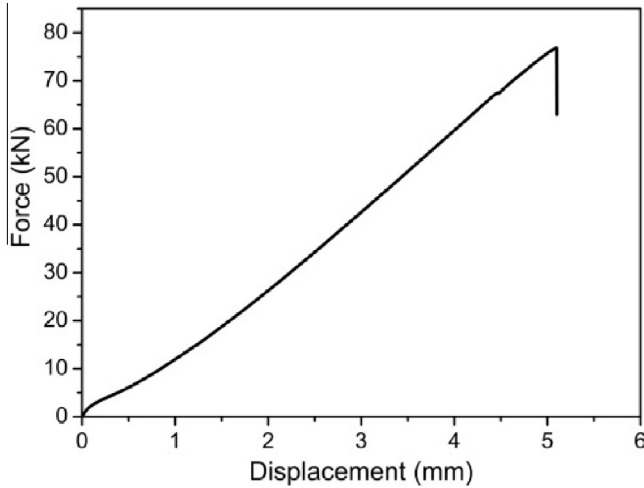
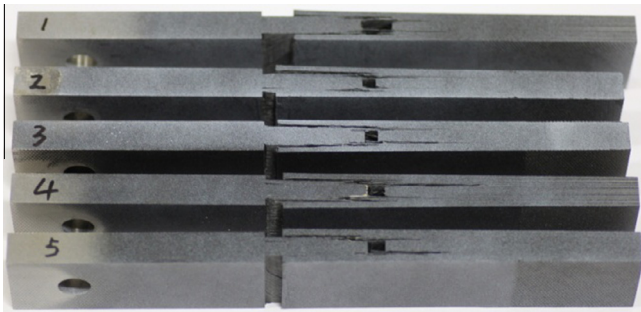


Fig. 5. Final specimens of Ti6Al4V/CFRP after manufacturing (a) standard specimens (b) ground specimen (with cross-sectional view of a row of exposed protrusions on the side of the specimen).

Table 2

Results of tensile testing of Comeld joints between Ti6Al4V and CFRP.

No.	F (kN)	Displacement (mm)	σ_f (MPa)	τ (MPa)
1	65.97	4.73	200.0	33.3
2	73.10	5.60	221.5	36.9
3	75.12	5.87	227.6	37.9
4	72.91	5.49	220.9	36.8
5	76.85	5.10	232.9	38.8
Avg	72.79	5.36	220.6	36.8
STD	4.14	0.45	12.54	2.09
GR1	59.98	6.13	199.4	33.2
GR2	58.55	4.07	194.6	32.4

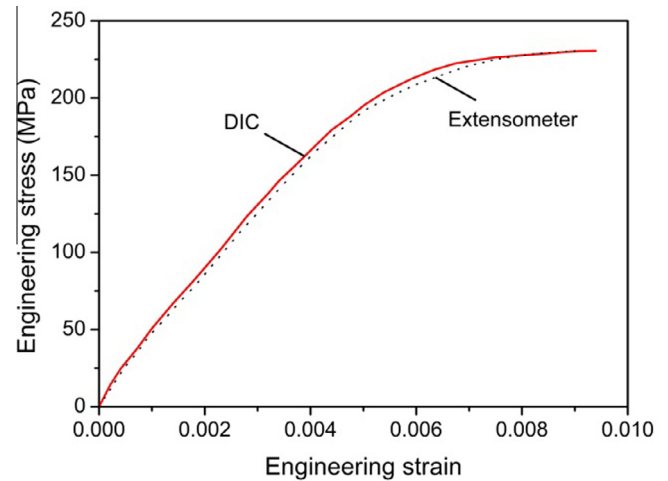
**Fig. 7.** Results of tensile testing of a Comeld joint (Ti6Al4V/CFRP).**Fig. 8.** Fractured Comeld standard specimens after tensile testing.

were used for recording the strain during testing. Fig. 9 shows engineering stress versus the longitudinal strain (ϵ_{11}) across a gauge length of 25 mm and the DIC strain output curves obtained on the front view of the specimen. The results showed excellent agreement between the ARAMIS DIC strain values and the extensometer values.

3.2. DIC results analysis

3.2.1. Front view results for the standard specimen

Fig. 10(a) illustrates the locations of the analysis lines chosen for the DIC performed on two sides of the joint (front and side views). Line 2 was at the central position of the front view along the longitudinal direction, and lines 1 and 3 were symmetrical about line 2. The length of lines 1, 2 and 3 was equal to the extensometer gauge length, i.e. 25 mm. Lines 4, 5 and 6 were the centre lines of each section on the side view along the longitudinal

**Fig. 9.** Tensile engineering stress–strain curves obtained using DIC and extensometer.

direction. The length of lines 4, 5 and 6 was 20 mm. Line 7 was a cross-sectional line on the side view (i.e. in the transverse direction), with a length equal to the specimen thickness of 11 mm.

Fig. 10(b) shows the computed full field longitudinal strain (ϵ_{11}) distribution at 99% of the failure load along the gauge length from the front view. The highest strain region was distributed near the interface of composite and metal, whereas, there were no strain changes along the remaining gauge length. When the loading force nearly reached the peak value during testing, the strain concentrated near the interface region and when the load exceeded the interface bonding strength, a crack appeared on the interface. Fig. 10(c)–(e) further illustrate the strain evolutions observed in the specimens. Prior to 75% of the failure load being reached, there was no obvious difference in the strain distribution along the three section lines (1–3) and the strain values were very low. However, just prior to failure the strain value reached a peak, and a distinct difference between the three lines was observed. Line 1 located on the left side of the front view showed the highest strain value with $\epsilon_{11} \approx 0.11$ compared to line 2 with $\epsilon_{11} \approx 0.10$ and line 3 with $\epsilon_{11} \approx 0.09$ which indicates that the failure initially took place on the left side of the joint and then extended into the middle and right sections.

3.2.2. Side view results of the standard specimen

Fig. 11(a) shows the computed full field longitudinal strain (ϵ_{11}) distribution at 99% of the failure load along the gauge length on the side view and the locations of lines 4–6. The position of the composite–metal interface is superimposed onto the strain map together with the position of the protrusions (beneath the surface). Fig. 11(b)–(d) show different longitudinal strain distributions at different proportions of the final failure load (i.e. 25%, 50%, 75%, 99%). Lines 4–6 were symmetrical about the axial centre line of the specimen, so when loaded longitudinally, the strain fields along lines 4 and 6 are very similar just prior to failure, as is shown in Fig. 11(b) and (d). Fig. 11(c) shows larger strain changes compared to Fig. 11(b) and (d). Indeed, the strains along line 5 are greater than the strains along lines 4 or 6 throughout the loading and are significant as these strains contribute to the sequence of failure finally culminating in the delamination mechanism evident in Fig. 8.

Fig. 12(a) shows the computed transverse strain (ϵ_{22}) full field distribution at 99% of the failure load on the side view. Section line 7 is along the transverse direction. Fig. 12(b) shows the value of the transverse strain, ϵ_{22} , along line 7, and the location of the

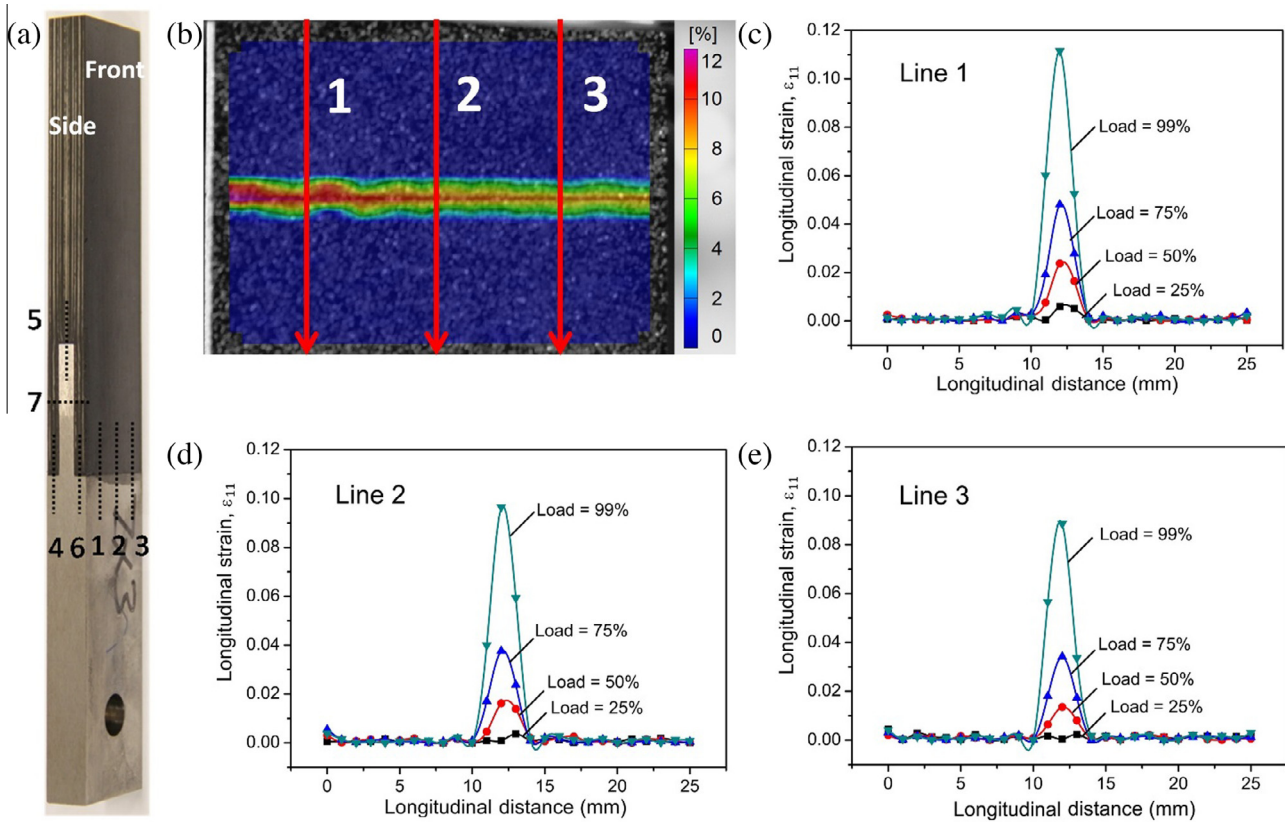


Fig. 10. (a) lines for DIC analysis (1–3) on the front surface of specimen, (b) enlarged DIC results on front view and longitudinal strain ϵ_{11} at 25%, 50%, 75% and 99% of failure load along: (c) line 1; (d) line 2 and (e) line 3.

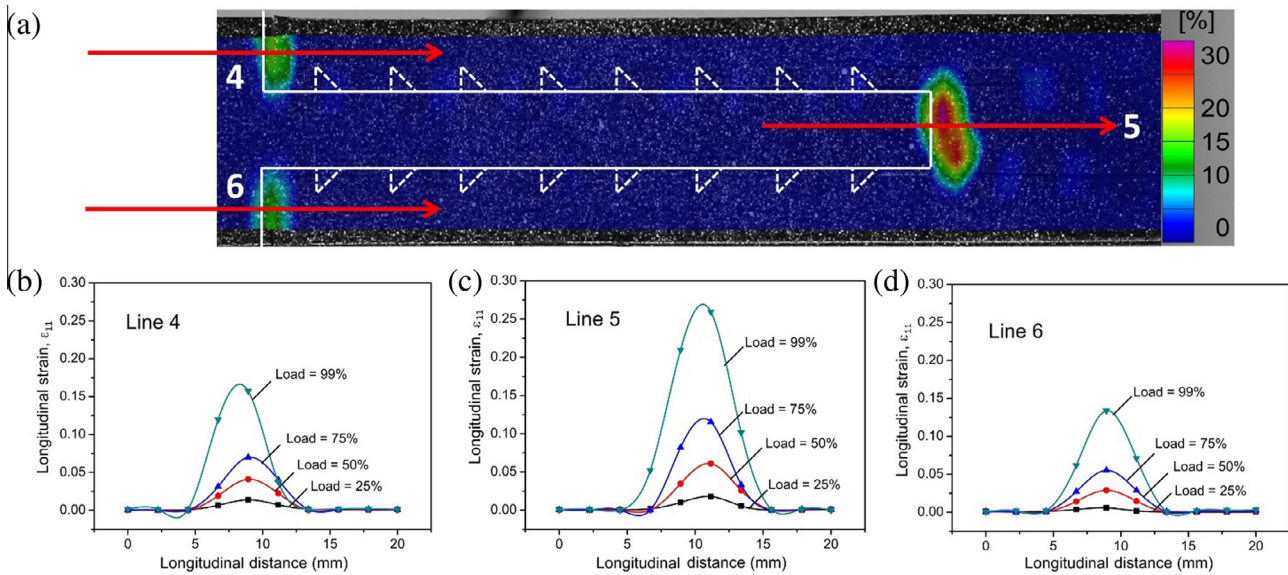


Fig. 11. Full field longitudinal strain distribution on the side of the specimen at 99% of failure load and results at 25%, 50%, 75% and 99% of failure load along (b) line 4, (c) line 5 and (d) line 6. The position of the composite-metal interface is superimposed onto the image in (a) together with the position of the protrusions beneath the surface.

composite and metal parts of the joint. The two strain peaks along line 7 correspond to the approximate location of the ends of the protrusion cut by line 7, i.e. they occur about 1 mm into the composite measured from the interface. However, it is the concentrations of transverse strain shown at the end of the overlap in Fig. 12(a) that are most significant and it is these strains that contribute to triggering the delamination of the composite at these locations, as shown in Fig. 8, and is discussed further below.

3.2.3. DIC results of ground specimen

The ground specimens were manufactured and tested to obtain the full field strain field in close proximity to the protrusions on the side view. The surface strain field results from the front view were similar to that of the standard specimen for the interface between metal and composite, whereas, from the side view, there was a significant difference because the interface changed considerably when the protrusions were exposed by grinding. Fig. 13(a) shows

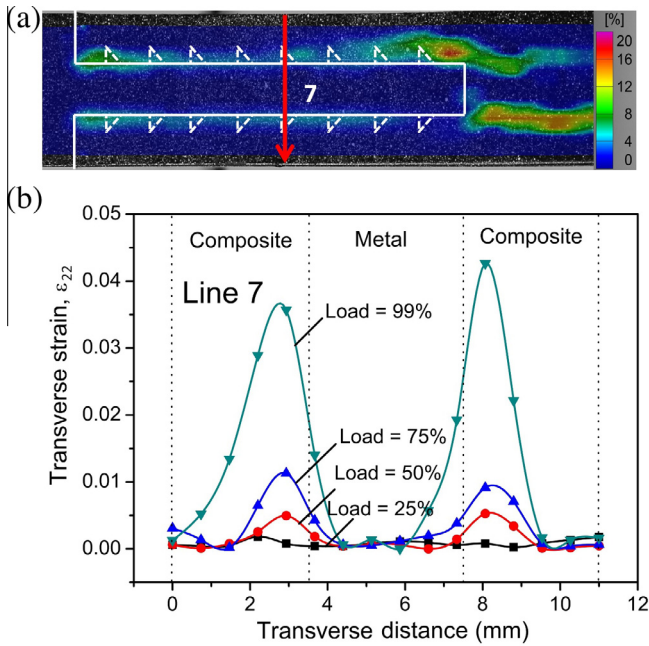


Fig. 12. (a) Full field transverse strain (ϵ_{22}) distribution at 99% of the failure load from the side view. The section line 7 is shown along the transverse direction. (b) Transverse strain (ϵ_{22}) distribution on the section line 7 at different loads (25%, 50%, 75%, 99%).

the computed longitudinal strain (ϵ_{11}) full field distribution at 99% of the failure load on the side view of the ground specimen. Compared with Fig. 11(a), apart from the similar higher ϵ_{11} strain distributed on the interfaces between metal and composite (near lines 4, 5 and 6), i.e. at the ends of the overlap, the ϵ_{11} strain near the protrusions on the interface of the ground specimen became smaller than for the standard specimen (Fig. 11). Also shown on Fig. 13(a) is the location of the DIC analysis line 8, passing longitudinally along a plane cut by a row of protrusions. As is shown in Fig. 13(b), mapping the strain across each composite-metal-composite interface along line 8 resulted in two strain peaks. Thus, the number of strain peaks shown in Fig. 13(b) is twice the number of protrusions cut by line 8, discounting the main peaks at each end of line 8 associated with the major strain concentrations. It can be seen that the protrusions on the left side of the overlap induce the higher strain concentration and this diminishes moving towards the right end of the overlap.

Fig. 14(a) shows the computed transverse strain (ϵ_{22}) full field distribution at 99% of the failure load on the side view on the ground specimen. The strain field variation matched the profile of the protrusions along the interface. Fig. 14(b) shows the strain distribution along the middle transverse section (line 9). Comparing Fig. 14(b) with Fig. 12(b), the value of ϵ_{22} is much lower for the ground specimen, i.e. the maximum strain for line 9 is less than the maximum strain for line 7. Also the value of the maximum strains in Fig. 14(a) are only a fifth of the maximum strain values in Fig. 12(a). This indicates that, on the plane through a row of protrusions, both the longitudinal and transverse strains are reduced by the proximity to the protrusions.

Figs. 15 and 16 show the distribution of the full field major strains across the entire side view at 99% of the failure load for the standard specimen (Fig. 15) and ground specimen (Fig. 16). Comparing Figs. 15 and 16, the following observations can be made. Firstly, the values of the major strain are always significantly lower for the ground specimens than for the standard specimens. This shows how effective the protrusions are (in a plane through a row of protrusions) of reducing the principal strain and hence

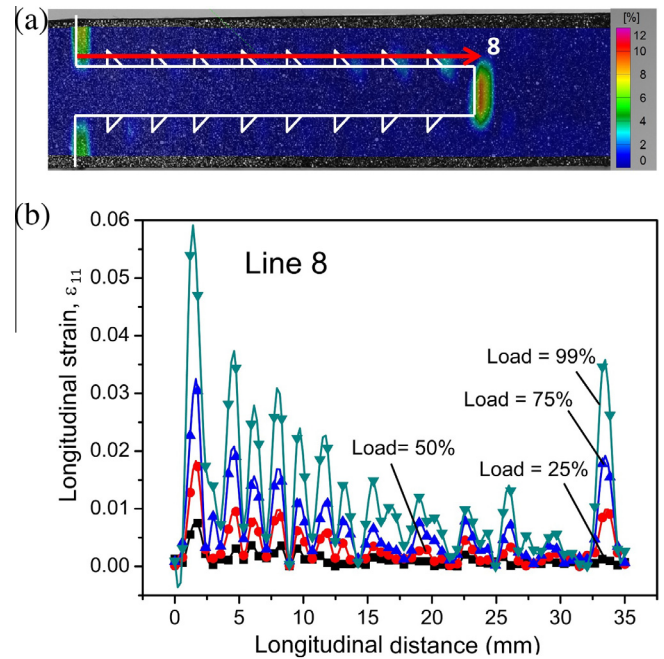


Fig. 13. (a) Computed longitudinal strain (ϵ_{11}) full field distribution at 99% of the failure load from the side view of the ground specimen. (b) Strain values along section line 8 near protrusions. The position of the composite-metal interface is superimposed onto the image in (a) together with the position of the protrusions exposed on the surface.

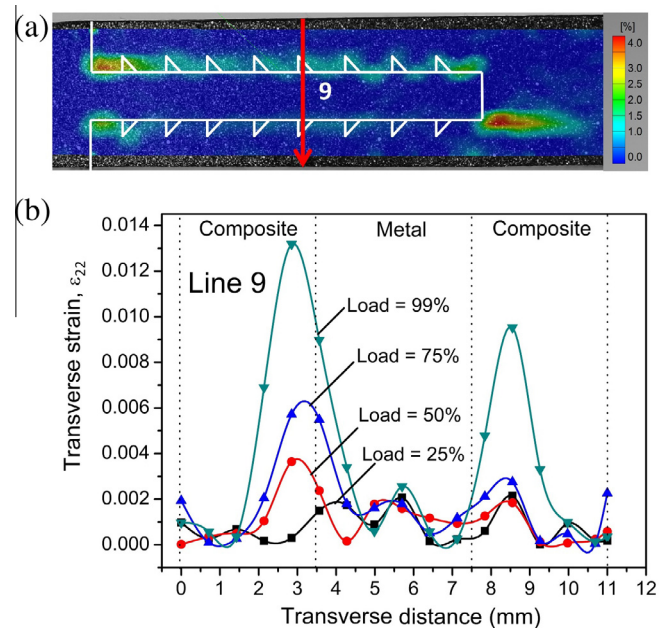


Fig. 14. (a) computed transverse strain (ϵ_{22}) full field distribution at 99% of the failure load from the side view. (b) strain values along section line 9 near protrusions.

delaying failure in the joint (compared to a joint without any protrusions). Secondly, the metal core of the hybrid joint overlap experiences low strain with the major strain peaking in the composite at the end of the overlap on the joint mid-section. The strain peaks in the composite in line with the protrusion ends were about 50% of the maximum strain experienced for both the standard and the ground specimens. Finally, the ground specimens clearly show the presence of the composite-metal-composite interfaces along

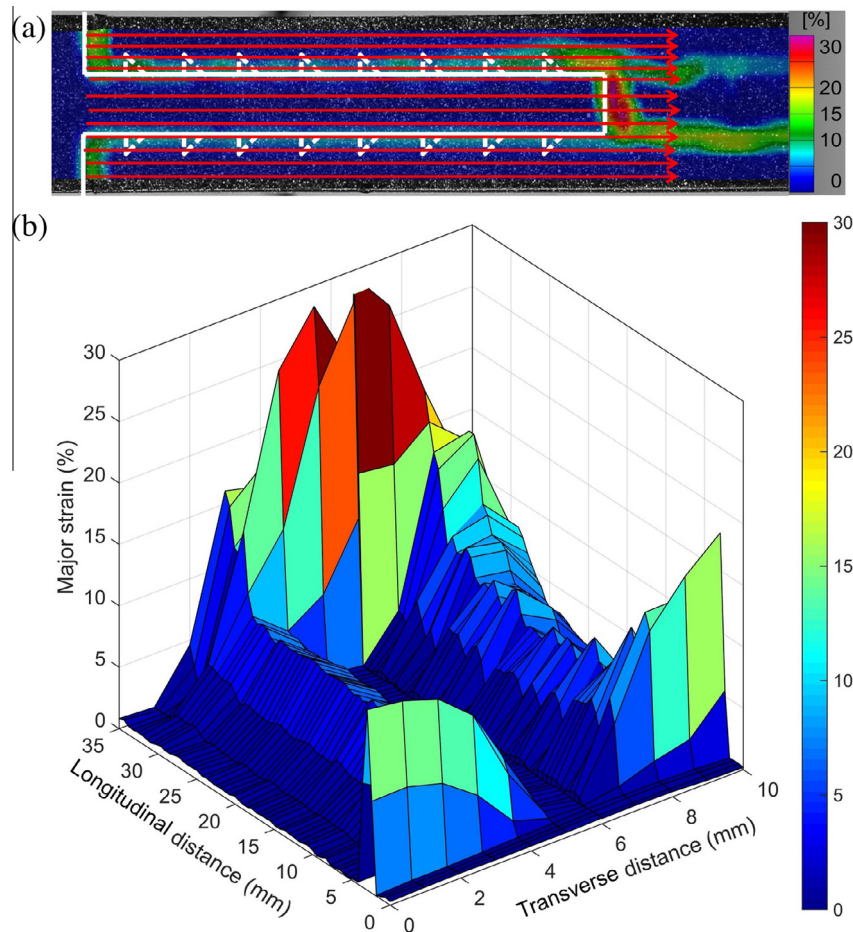


Fig. 15. (a) computed major strain, ϵ_{11} , full field distribution at 99% of the failure load from the side view of the standard specimen. (b) 3D plot of local major strain field distribution at 99% of the failure load from the side view of the standard specimen.

the longitudinal length. This demonstrates the sensitivity of the DIC technique and the local influence of the protrusions.

3.3. Failure mode analysis

According to the above full field strain distributions measured during the tests, a better understanding has been gained of the failure process in the joints. From the strain field result at 99% of the failure load and from observations during the test, it is shown that the failure process consists of two main stages: the first stage involves the separation of the interfaces (debonding) normal to the loading direction. For example, in the areas indicated by positions A, B and C in Fig. 17, the failure initiated between the composite-metal interfaces, and then the whole interface (near positions A and B) separated quickly. With further displacement, the interface near position C sustained the load until failure. Once all three interfaces normal to the axial load had failed, the second failure stage then began which consisted of failure in the composite. This damage consists of both delamination and fibre fracture and some intra-ply matrix cracking. In this type of failure mode, several laminate plies remained bonded to the sculpted metal surface after failure, as is shown in Fig. 18(a). From the surface layer direction (-45° on both sides, shown in Fig. 18a), 6 plies remained attached to the metal part, where each layer is about 0.25 mm thick, (Table 1), i.e. the total thickness of the composite left attached to the metal was 1.5 mm which is just greater to the height of the protrusions at 1.2 mm. The maximum load resulted from some damage (including fibre fracture) to the retained layers

and delamination extending past the end of the overlap into the composite as shown in photograph of the failure composite part in Fig. 18(b).

As discussed in Section 2, the Surfi-Sculpt protrusions employed in this study had a wedge shape pattern with a significantly wider base than head. The processing parameters employed were optimised in the sense that the protrusions were manufactured of sufficient quality in the shortest time. The wedge shape was produced to avoid the protrusion failing by fracture during loading of the Comeld joint. Indeed, studies reported in the literature show that where the protrusions are not strong enough to support the load, once the maximum load was reached, the failure resulted in the complete fracture of the protrusions [13,15]. Studies also have indicated that the height and angle of protrusions are important factors. Tu et al. [11] used finite element analysis to study these effects in titanium alloy-composite Comeld joints in tension. They showed that, for cylindrical protrusions with hemispherical heads, the optimum angle for the protrusions was between 20° and 30° with the protrusions leaning towards the metallic end of the joint to resist shear. They also found that longer protrusions were better but it is known that if they penetrate right through the composite part then this can lead to other problems such as a loss of aerodynamic performance or a reduction in environmental durability. The wedge shape protrusions used in the present work offer a normal face to resist the imposed shear stress on the interface whilst the 45° linear taper on the opposite face reduces the magnitude of the bending stress imposed at the protrusion base. The height of the protrusions was selected to combine manufacturing speed

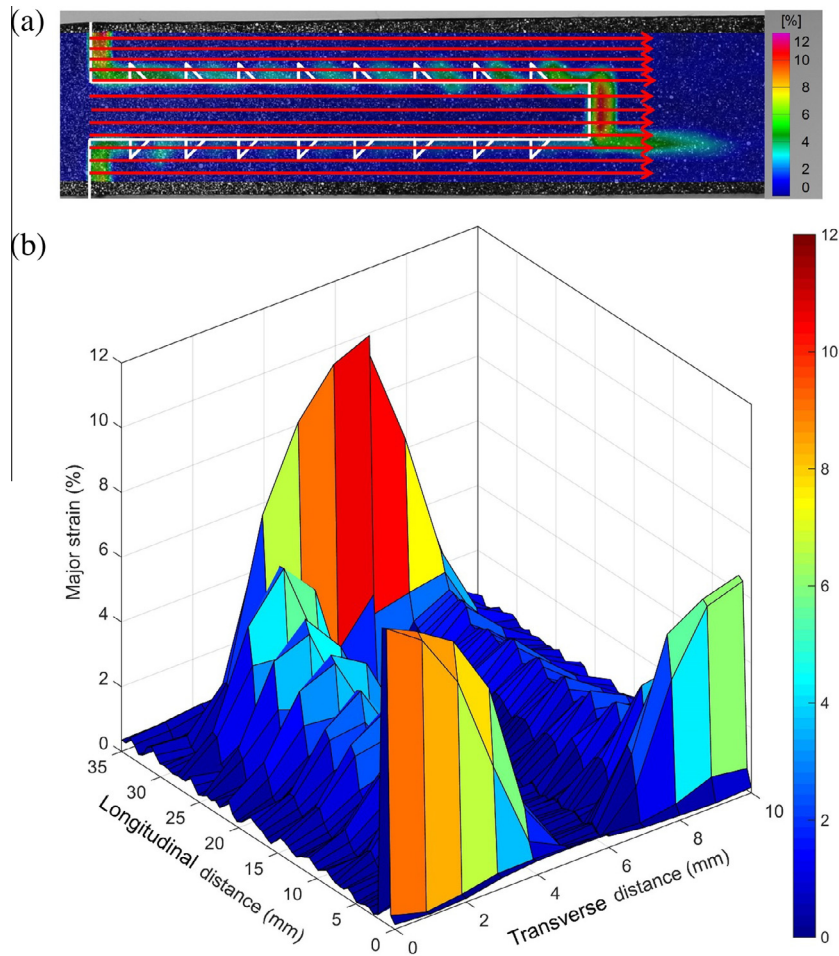


Fig. 16. (a) computed major strain full field distribution at 99% of the failure load from the side view of the ground specimen. (b) 3D plot of local major strain field distribution at 99% of the failure load from the side view of the ground specimen.

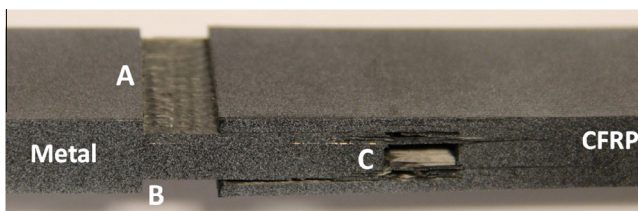


Fig. 17. Failure of the Comeld specimen with linear patterned protrusions.

with sufficient joint performance. In a subsequent paper [21] we show that, in tensile testing, Comeld joints formed using the wedge shaped protrusions (linear profile) significantly outperform joints formed using cylindrically shaped protrusions.

The DIC technique has clearly proved valuable in measuring the detailed full field strain distributions in hybrid joints and its

sensitivity and resolution has been sufficient to identify individual strain concentrations around individual protrusions when exposed by grinding on the specimen side view. It should be noted that in the comparison of the standard and ground specimens, it is the effect of the strains local to the protrusions which is being compared to strains somewhat removed from the protrusions as both standard and ground specimens contain protrusions. The use of the ground specimens was simply to expose the protrusions on the specimen side view.

The concentration of the longitudinal strain on the specimen front view (Fig. 10b) would appear to trigger the first step in the failure process of the joint, i.e. the failure of the composite-metal interface normal to the applied load. However, the combination of the concentration of longitudinal strain at the end of the overlap (Fig. 11a) and the concentration of transverse strain in the composite at the end of, and beyond the overlap, (Fig. 12a) appear to trigger the second, ultimate step in the joint failure

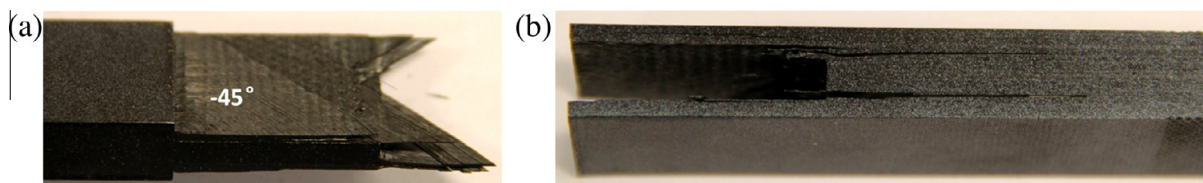


Fig. 18. Delamination failure on the interface between composite layers. (a) the failed metal section, (b) the failed CFRP section.

which is the delamination in the composite (with some associated fibre fracture and intra-ply matrix cracking), extending across the overlap length on a plane in close proximity to the ends of the protrusions. It is not thought that the debonding of the metal-composite interfaces normal to the applied load, which we describe at stage 1, represents the critical damage mode. However, once these interfaces have failed then the load is fully transferred through the protrusions and to a lesser extent, across the horizontal metal-composite interface. In these joints, the critical damage mode appears to be the development of a critical transverse strain in the composite which initiates an unstable delamination fracture back through the laminate in the region of the protrusion ends. The flat horizontal interfaces and those on the surface of the protrusions therefore do not debond and the strength of the joint is limited by the toughness of the composite. The relative role of the diameter of surface pins and the adhesion attained across the metal-composite interface was investigated by Parkes et al. [15]. These workers studied joints formed between titanium alloy and composite using surface pins formed via additive manufacturing, i.e. using hybrid penetrative reinforcement (HYPER) joints. They found that the most significant improvements in joint performance were obtained by increasing the diameter of the pins (to delay pin fracture) and that the effects of inserting a disbond on the horizontal metal-composite interface and the use of laser treatment of the titanium prior to bonding both were relatively minor. In their tests, following debonding of the horizontal interface, the load was fully transferred to the metal pins which ultimately fractured, leaving an almost undamaged composite. By contrast, the results reported on the present study fail ultimately within the composite, and attempts to further improve the strength of the joint must be aimed towards reducing the stress concentrations within the composite, or by improving the ability of the composite to withstand the applied stresses.

The strain mapping clearly provides a technique to evaluate the performance of a Comeld joint and to compare different designs. In a recent paper from some of the present authors Xiong et al. [20] investigated the effect of the composite layup on the performance of a Comeld joint. It was found that the optimum strength was achieved for a joint where some damage to the protrusions had occurred but this required enhancement of the composite lay-up. There is the possibility that the joints in the present work could be further enhanced and this forms part of our planned future research. In addition, it is planned to investigate the fatigue loading of Surfi-Sculpt Comeld joints and in this loading regime, the performance of the joints is expected to be very sensitive to the detailed specification of the protrusions and their effects on the strain distribution across the interface.

4. Conclusions

An advanced Comeld hybrid joining technique was employed to manufacture double lap joints between an aerospace titanium alloy (Ti6Al4V) and a carbon fibre reinforced epoxy composite. The metal parts had been surface treated by Surfi-Sculpt, in which an electron beam had been used to create surface protrusions of 1.2 mm in height and with a surface density of 18 pins/cm². The protrusions were wedge shaped with a much wider base than head. The joints were tested to failure in tension and a detailed study of the strain variation during loading was undertaken using both a contact extensometer and full field digital image correlation (DIC) technique.

The longitudinal strain distributions along the front view of the specimen showed significant local concentrations at the metal-

composite interface. The longitudinal strain distributions on the side view showed a peak strain at the end of the overlap at the composite mid-section. The transverse strain distributions along the side view of the specimens showed peak tensile strains close to, and beyond the end of the overlap in the composite.

Using the DIC technique to measure the strain distribution along a side view (where the specimen had been ground to expose the protrusions onto the surface), periodic strain peaks associated with each individual composite-metal interface of each individual protrusion were measured. The protrusions reduced the values of the in-plane strains compared to planes where no protrusions were locally present. The test observations, together with the measured strains, were used to explain the failure sequence exhibited by the joints. This was shown to involve (i) failure of the tensile composite-metal interfaces followed by (ii) delamination of the composite on planes just above the protrusion ends, with additional fibre fracture and intra-ply matrix cracking occurring. For the tests reported on here, no damage (yielding or fracture) to the protrusions was observed. The DIC techniques described here have provided additional insight into the failure mechanisms pertaining to Comeld joints and opens the way for further optimisation of the joints.

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References

- [1] www.airbus.com.
- [2] Hart-Smith LJ. Adhesive bonded double-lap joints. NASA report CR112235; 1973.
- [3] Moutrille MP, Derrien K, Baptiste D, Balandraud X, Grédiac M. Through-thickness strain field measurement in a composite/aluminum adhesive joint. *Composites Part A* 2009;40:985–96.
- [4] Camanho PP, Matthews FL. Stress analysis and strength prediction of mechanically fastened joints in FRP: a review. *Composites Part A* 1997;28A:529–47.
- [5] Kweon J, Jung J, Kim T, Choi J, Kim D. Failure of carbon composite-to-aluminum joints with combined mechanical fastening and adhesive bonding. *Compos Struct* 2006;75:192–8.
- [6] Dano M, Gendron G, Picard A. Stress and failure analysis of mechanically fastened joints in composite laminates. *Compos Struct* 2000;50:287–96.
- [7] Dance BGI, Kellar EJC. Workpiece structure modification. International Patent Publication Number WO 2004028731 A1.
- [8] Buxton AL, Dance BGI. Surfi-Sculpt – revolutionary surface processing with an electron beam. In: Proc. ISEC, Minnesota; August 2005.
- [9] Wang XC, Guo EM, Gong SL, Li B. Realization and experimental analysis of electron beam Surfi-Sculpt on Ti-6Al-4V alloy. *Rare Met Mater Eng* 2014;43(4):819–22.
- [10] Mouring SE, Janowski M, Louca LA, Brambleby RJ. Structural performance of Comeld hybrid metal-to-composite joints. In: Proceedings of the twenty-second (2012) international offshore and polar engineering conference, Rhodes; June 2012. p. 117–122.
- [11] Tu W, Wen PH, Hogg PJ, Guild FJ. Optimisation of the protrusion geometry in Comeld™ joints. *Compos Sci Technol* 2011;71:868–76.
- [12] Wang X, Ahn J, Bai Q, Lu W, Lin J. Effect of forming parameters on electron beam Surfi-Sculpt protrusion for Ti-6Al-4V. *Mater Des* 2015;76:202–6.
- [13] Ucsnik S, Scheerer M, Zaremba S, Pahr DH. Experimental investigation of a novel hybrid metal-composite joining technology. *Composites Part A* 2010;41:369–74.
- [14] Meyer J, Johns D. Hybrid component. International Patent Number WO 2010/112904A1; 2010.
- [15] Parkes PN, Butler R, Meyer J, de Oliveira A. Static strength of metal-composite joints with penetrative reinforcement. *Compos Struct* 2014;118:250–6.
- [16] Tu W, Wen PH, Guild FJ. Multi-region mesh free method for Comeld™ joints. *Comput Mater Sci* 2010;48:481–9.

- [17] Comer AJ, Katnam KB, Stanley WF, Young TM. Characterising the behaviour of composite single lap bonded joints using digital image correlation. *Int J Adhes Adhes* 2013;40:215–23.
- [18] Makeev A, Seon G, Lee E. Failure predictions for carbon/epoxy tape laminates with wavy plies. *J Compos Mater* 2010;44(1):95–112.
- [19] Cheng P, Sutton MA, Schreier HW, McNeill SR. Full-field speckle pattern image correlation with b-spline deformation function. *Exp Mech* 2002;42(3):344–52.
- [20] Xiong W, Blackman BRK, Dear JP, Wang X. The effect of composite orientation on the mechanical properties of hybrid joints strengthened by Surfi-Sculpt. *Compos Struct* 2015;134:587–92.
- [21] Wang X, Ahn J, Lee J, Blackman BRK. Investigation of the failure modes and mechanical properties of hybrid joints with different interface patterns using digital image correlation. *Mater Des* 2015.