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 represent successfully 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 take-off and 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.

Despite the lack of experimental as well as numerical studies on high velocity soft impact response of laminated glass windows, research in this field can benefit from the rich literature on low velocity impact response (Chen et al., 2013, 2015; Grant et al., 1998; Kaiser et al., 2000; Peng et al., 2013; Pyttel et al., 2011; Saxe et al., 2002; Zhang et al., 2013) as well as ballistic testing of glass at high rates (Bourne et al., 1994; Chocron et al., 2016, 2010, 2007b; Holmquist and Johnson, 2011; Walley, 2013).

Grant et al., (1998) investigated the damage threshold of laminated glass structures using granite projectiles up to impact velocities of 20 m s^{-1} . Critical impact velocity was defined as the lowest velocity at which damage occurred during a set of 30 impact tests. The thickness of the outer glass layer was found to be the primary parameter affecting this critical velocity. The performance of laminated glass windows was investigated against the windborne debris for hard (Kaiser et al., 2000; Saxe et al., 2002) as well as soft (Zhang et al., 2013) impactors. Kaiser et al., (2000) proposed a “sacrificial ply” design concept for protection of architectural glazing against the windborne debris. In this approach, the exterior-facing, outer glass ply, is allowed to fracture during impact. This however, prevents the fracture in the inner glass ply and retains the structural integrity of the whole structure. A statistical approach, mean minimum breakage velocity (MMBV), was used by Kaiser et al., (2000) to compare various laminated glass constructions. Cumulative probability of inner glass failure was also assessed. The “sacrificial ply” design concept was explored further by Saxe et al., (2002). The effect of glass type on the impact performance was studied using annealed, heat strengthened, and fully tempered glass. Regardless of outer glass layer type, using heat-strengthened or fully tempered inner glass layer instead of annealed glass significantly improved the MMBV. Chen et al., (2013) investigated the radial and circular crack propagation in a laminated glass window subjected to low velocity hard impact. The speed of radial and circular cracks were measured using high speed photography. A Weibull statistical approach was used to analyse the macroscopic cracking

morphology over 100 repeated experiments. Increasing impact velocity and polymer interlayer thickness were found to have an opposite effect on the radial and circular crack numbers. Chocron et al., (2016) studied the damage threshold of borosilicate glass under plate impact at velocities ranging from 116 to 351 m s⁻¹. The damage in the glass was observed to occur behind the shock wave at velocities as low as 130 m s⁻¹.

Numerical simulations were also employed to predict the response of laminated glass windows subjected to pedestrian head impact (Peng et al., 2013; Pyttel et al., 2011), windborne debris (Shetty et al., 2012; Zhang et al., 2013) and low velocity hard impact (Behr et al., 1999; Chen et al., 2015). Chen et al., (2015) developed a three-dimensional computational framework for modelling impact fracture in laminated automotive glazing. Good agreement was observed between the experimental and numerical results in terms of fracture patterns and peak force. Pyttel et al., (2011) proposed a failure criterion for laminated glasses subjected to impact loading. The success of the failure criterion was then assessed by comparing numerical and experimental results for pedestrian head impact on flat and curved windows. Pedestrian head impact was also simulated by Peng et al., (2013) using different combinations of glass and PVB. A critical fracture stress criterion was used to model the failure in the glass. Zhang et al., (2013) investigated the response of laminated glass windows against large windborne wooden blocks. The glass was modelled using a Johnson Holmquist Ceramic constitutive model (JH2) (Holmquist and Johnson, 2011). The developed numerical model reliably simulated the window deflection, maximum strain, debris penetration and glass cracking shape. Chocron et al., (2007a, 2007b) developed a constitutive model for pre-damaged borosilicate glass under confinement. The model was employed to simulate the penetration of projectile into the glass targets (Chocron et al., 2007a).

Hydrodynamic loading

Bird impact at high velocities can be considered as a “soft body impact”. The soft body impact refers to an impact in which the strength of the projectile is much lower than that of the target and consequently the projectile undergoes extensive deformation. The loading imposed on the target is very different from normal hard impact (in which the projectile deformation is negligible) and can be described well using hydrodynamic theory. In the hydrodynamic approach, the loading mainly depends on the density and velocity of the projectile and projectile strength and viscosity are neglected (Wilbeck, 1978). Although, the bird body consists of various parts with different densities (head, neck, wings, torso shell, etc.), in most theoretical and numerical studies, the bird is normally treated as a homogenous material with a uniform density (average density of all parts). McCallum et al., (2013) developed a numerical multi-material bird model with a more accurate representation of bird anatomy. However, comparison with standard projectile shapes (cylindrical, hemi-spherical and ellipsoid) with a single homogenous density showed similar results for Hugoniot pressure, maximum impact force and impact duration for bird-strike certifications tests.

According to Wilbeck (1978) and Barber et al. (1978), the bird impact loading on a rigid target can be divided into two stages: stage i) in which the intensity of loading is high but the duration is very short (transient state) and stage ii) which has the opposite characteristics, longer duration but less intensity (steady state). This will be further discussed in Section 5-1. The loading can be affected by the response of the target, as there is a coupling between loading and response. Factors such as shock impedance, compliance and deformability of the target can significantly affect all aspects of the loading including peak force and pressure, rise time, magnitude of impulse and duration of decay and the steady state process (Barber et al., 1978).

Outline of the investigation

In this paper, a combination of experimental and numerical analyses is employed to investigate the impact performance of laminated glass windows. The deformation and failure mechanisms of the laminated glass windows are studied at the velocity range between 100 to 180 m s⁻¹. The effect of projectile nose shape, the glass front layer type and the order of glass layers on impact damage of laminated glass windows are also investigated. The novel aspect of this research 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. 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 (only the strength and depth of compressive layer was measured and the distribution of through-thickness residual stress was not measured). Therefore, the variation in the strength and depth of compressive layer is about 3% and 13% respectively. These variations

in addition to other factors including flaw size and distribution can contribute to the variation in the strength of glass plates. In order to quantify these variations, quasi-static failure strain of chemically strengthened glasses was measured using ring-on-ring experiments. The failure probability was then assessed using a two-parameter Weibull statistical distribution. The Weibull probability of failure $\ln(-\ln(1 - P_f))$ is plotted against failure strain for 2.2 and 4.0 mm chemically strengthened glass plates in Fig. 1. The curves are fitted using a non-linear least-squares method through at least 12 repeat tests. Further details on the experimental procedure can be found in a separate publication (Mohagheghian et al., 2016).

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). The tin side of the glass was used for lamination. For the polymer interlayer, Thermoplastic Polyurethane (TPU) - KRYSTALFEX®PE499 from Huntsman was used. 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. 42a. 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. 2b.

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. 3a.

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. 3a). 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. 3a). 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. 3b) 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. 3c. 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. 3c). 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. 4) with a symmetry boundary condition applied along the sectioned surfaces. The boundary of the target, shown in Fig. 3b, was modelled including the rubber gaskets (Fig. 4a). The two free surfaces of the rubber gaskets were constrained in the z direction, simulating the presence of the two clamps in Fig. 3b. 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. 4b) with a typical element size of $0.33 \times 0.33 \times 0.33$ mm. The simulation results became insensitive to the size of the mesh on further refinement. 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. Modelling fracture in the glass plates is not considered in the present study and therefore no failure model is employed for the glass in the FE model.

In this study, the chemically strengthened glass is treated as an isotropic material without considering the initial through-thickness residual stress distribution. The same approach has also been used for finite element simulation of quasi-static and low velocity impact response of chemically strengthened glass plates (Hu et al., 2014; Shetty et al., 1980; Singh et al., 2016; Westbrook et al., 2010; Xue et al., 2013). As shown by Jiang et al., (2016), chemically strengthening does not affect the quasi-static flexural stiffness of the glass plates but only increases the strength of the glass by postponing the failure to larger deformations. In this paper, the response of laminated glass windows is investigated numerically only at impact velocities for which no fracture occurs in the glass layers. Therefore, it is believed that not considering the residual stress in the FE model has no effect on the simulation results. It should be noted however, that when the failure of the glass plates needs to be modelled, considering the residual stress would be essential as it has a significant effect especially on the crack propagation and on the shape of the fragments.

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 (Heimbbs, 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 interest and the simulation was performed over a long period (1 ms), the Eulerian box in Fig. 4a was used as the box needed to cover the complete radial flow of the projectile. When the initial contact pressure and pressure distribution inside the projectile were of interest and the simulations were performed over the shorter period (the initial 50 μ s), the Eulerian box shown in Fig. 4b was used. The Eulerian box was discretised using 8-node brick elements with reduced integration (EC3D8R in Abaqus notation). The mesh size inside the Eulerian box had a typical size of 0.18×0.18×0.18 mm. It was found that the simulation results became insensitive to the size of the mesh on further refinement.

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

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

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

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

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

General frictionless explicit contact was used for modelling contact between all surfaces except the interface between the glass and the polymer interlayer for which a tie constraint was used. This is valid assumption as long as no debonding occurs at this interface, which was the case before the glass layers fractured. The general contact algorithm in Abaqus enforces contact between Eulerian materials and Lagrangian surfaces (Abaqus Version 6.14). The contact constraints are enforced with the penalty method (Abaqus Version 6.14).

5-Results

5-1 Deformation and failure mechanisms

As described earlier, soft impact by a silicon rubber projectile is used to generate hydrodynamic loading similar to that which a windshield experiences during a bird strike. An example of the results from an impact experiment is shown in Fig. 5, for a laminated glass sample, with a thermally strengthened front face (Case 2), as often employed in the aircraft industry. This is impacted at the velocity of $170 \pm 1 \text{ m s}^{-1}$ (using sample Configuration I shown in Fig. 3c). Fig. 5a displays the deformation of the projectile and its interaction with the target at intervals of 0.05 ms. The duration of the contact is short (less than 1 ms); the projectile flows radially as expected and no part of the carrier hits the glass during the impact. At this velocity, only the front glass layer breaks and the rear glass layer is still intact. Fig. 5b displays the out-of-plane

displacement of the target calculated by DIC. The centre of the plate is displaced by about 5.0 ± 0.1 mm. The time of the maximum deflection is coincident with the time when the projectile completely loses its momentum and comes to rest (ca. 450 μ s). After this time, the projectile which is nearly flattened (Fig. 5a), starts to rebound. As noted earlier, the speed of the cameras used for DIC is twice the speed of the one camera used for monitoring the projectile. Hence, for each image in Fig. 5a, two images exist for DIC calculation. The extra images are excluded from Figs. 4b and c.

The major principal strain calculated by DIC can be found in Fig. 5c. The strain reaches its maximum of about 0.8 % at ca. 200 μ s. As can be noted, the time of maximum strain is not aligned with that of the maximum deflection (ca. 450 μ s). To further investigate this, the test was repeated using the sample Configuration II. This configuration allows observing the deformation over the whole span of the plate and the effect of the boundary on the deformation of the plate can be investigated. The results are shown in Fig. 6 for a laminated glass (Case 2) at an impact velocity of 174 ± 1 m s⁻¹. Similar to Fig. 5, the out-of-plane displacement and major principal strain contours are plotted for the observed area. The maximum deflection at the centre of the plate reaches approximately 5.5 mm and the major strain of nearly 0.8%. The out-of-plane displacement and major strain history are plotted in Fig. 6c for the centre point of the plate. Each data point shown in Fig. 6c, corresponds to one of the contours in Figs. 5a and b. There is a gradual increase in central out-of-plane displacement until the maximum at 475 μ s. A more rapid rise in central major strain can be seen, starting at the very early stages of the deformation. The deformation of the plate in Fig. 6c can be divided into four phases: Phase 1 where both strain and displacement are increasing, Phase 2 in which the displacement is increasing but there is not much change in the value of the strain, Phase 3 where the strain is decreasing while the displacement is still increasing and finally Phase 4 where both displacement and strain are decreasing.

373 The out-of-plane displacement profile of the plate is plotted over its whole span when the plate
374 is displaced (Fig. 6d) and then rebounds (Fig. 6e). Again, each profile corresponds to one of
375 the out-of-plane displacement plots in Fig. 6a. As can be observed, the deformation profile
376 does not cover the whole 150 mm of the plate length as some data (obtained from DIC) is lost
377 at points close to the edge of the clamp. At the early stage, Phase 1, the deformation is highly
378 localised under the point of impact and the boundaries are still not activated (Fig. 6d). During
379 this phase, flexural elastic waves travel from the point of contact towards the plate boundary.
380 This phase ends when these waves reach the boundary (at ca. 175 μ s). During Phase 2, the level
381 of the strain at the centre of the plate does not change significantly. The out of the plane
382 displacement however, is still increasing. It can be noticed in Fig. 6d that the displacement at
383 the boundaries is not zero in this phase. This means that the rubber gasket is compressed and
384 its deformation can account for a part of the increase in the out-of-plane displacement. In Phase
385 3, the plate starts unloading, as indicated by a significant drop in the major strain at the centre
386 of the plate (Fig. 6c). The profile of the plate in this phase is also different from that of Phase
387 2 (Fig. 6d). The rubber gasket is still being compressed and is responsible for a further increase
388 in the out-of-plane displacement (Fig. 6c). At the end of Phase 3, the gasket, which had the
389 original thickness of 4.1 mm, is compressed by around 50%. In Phase 4, the plate is rebounding
390 back and both the out-of-plane displacement and major strain are decreasing. The profile of the
391 plate in this phase is plotted in Fig. 6e.

392 The in-plane strain (strain in the y direction in Fig. 3c), calculated by DIC is compared in Fig.
393 7 with the results of the two strain gauges mounted on the back surface of the plate for a
394 Configuration II specimen. As mentioned earlier, the top surface of the strain gauges was
395 painted and speckled therefore, the deformation of the gauge can be monitored during the
396 impact event using DIC. Overall, there is a very good agreement between the two
397 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. 7, 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. 8. The front impacted side of the specimen was painted black prior to the impact experiment. Therefore, the white area in Fig. 8, 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. 8 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. 5a). From the signal of the strain gauge attached to the front layer (Configuration I, Fig. 3c), 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. 9 for a monolithic (6.0 mm monolithic chemically strengthened plate – Fig. 9a) and a laminated glass (Case 1) plate (Fig. 9b) 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. 9a) 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. 9b). 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. 9) and type of damage pattern (Fig. 8) indicate that the failure in the front glass layer is mainly controlled by high intensity water-hammer-type pressures developed in the initial phase of hydrodynamic loading.

The schematic in Fig. 10 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. 10a. 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. 10b). 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. 10b). 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. 10c-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. 10c-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. 6d). If the impulse transferred to the target, defined as the area under the curve in Fig. 10a, becomes high enough, the rear glass layer will break normally at the centre of the plate (Fig. 10e) where the flexural stresses are maximum.

The strain development and impact damage as a function of impact initial velocity are shown in Fig. 11 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. 11b. 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.

11b). The strain gauge traces are shown in Fig. 11c 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. 11b. 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. 11a. 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. 11b. Additional tests are needed to narrow down this area. However, because of variations in the strength of glass plates (Fig. 1), determination of exact impact damage threshold velocity is not possible without performing large number of experiments and using statistical approaches (Kaiser et al., 2000).

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. 11b, 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. 11b 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. 11b.

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. 12, 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. 12a), central out-of-plane displacement and strain history (Fig. 12b), and strain in the y direction at the location of the strain gauge (Fig. 12c). 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. 13. 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

541 strain at the centre (solid symbols), even at the higher velocities (e.g. 159 & 169 m s⁻¹), lies on
542 the dashed line plotted for the flat-fronted nose shape (the linear fit for data points with no
543 breakage) (Fig. 13).

544 The explanation for this dependency of failure to the projectile nose shape can be found in Fig.
545 14 where the numerical pressure development inside the projectile and the associated contact
546 pressure imposed on the laminated glass are shown for flat and hemi-spherical nose shapes.
547 Both of the projectiles have the same mass and diameter and are fired at the same initial velocity
548 of 131 m s⁻¹. This is the velocity at which no fracture occurs for any of the nose shapes (Fig.
549 13).

550 The development of high intensity pressures is apparent at the very early stages of impact ($t =$
551 $0.75 \mu\text{s}$) for a flat-ended projectile. This causes an approximately uniform contact pressure
552 across the diameter of projectile. In contrast, for a hemi-spherical nose the level of pressure
553 inside the projectile is lower and the contact pressure is mainly localised over a small area at
554 the centre of the plate (This area is growing as the contact area increases). As the time passes,
555 the release waves are developed at the edges of the flat-fronted projectile ($t = 4 \mu\text{s}$) which then
556 start travelling towards its centre. This changes the shape of the contact pressure profile and
557 reduces the pressure values around the edges. A second region of high intensity pressure is
558 developed at the centre of the plate at $t = 6.5 \mu\text{s}$ for the flat-fronted projectile as soon as the
559 release waves collide at the centre of the plate. The contact pressure now has its highest value
560 (ca. 200 MPa) at the centre of the plate. In contrast, for the hemi-spherical nose, the peak
561 pressure normally occurs at the edge of the contact (Field et al., 1985) and moving away from
562 the centre of the plate as the time passes (Fig. 14). At $t = 8.5 \mu\text{s}$ a negative pressure is developed
563 at the centre of the plate for the flat nose shape. The development of these tensile stresses and
564 the resulting cavitation are also mentioned by Field et al. (1985). At this time, the contact

565 pressure is nearly zero all over the plate except at a small area with ($x/r \sim 0.2$) where x is the
566 distance from the centre of the plate and r is the radius of the projectile.

567 The level of maximum principal strain is compared for the two nose shapes in Fig. 15 at two
568 locations inside the laminated glass target: location 1 and 2, which are the central points at the
569 distal side of the front and rear glass layers respectively. In Figs. 14a and b the focus is on the
570 early stage of the impact (the initial 50 μ s) while in Fig. 15c the performance is compared over
571 a longer period (500 μ s). As can be seen in Fig. 15a, the level of strain in the front glass layer
572 significantly increases over a short period of time and reaches the value of approximately 1%.
573 The initial negative value of strain is because of the elastic compression wave reaching the
574 back of the plate before the plate starts bending. As can be observed the level of strain in
575 location 1 is higher than location 2 and is more likely to be the fracture initiation point. This
576 reflects what has been seen experimentally in Fig. 11, where the fracture always occurred
577 earlier in the front glass layer. It should be mentioned that although the plate does not fail at
578 this impact velocity (131 m s⁻¹), the level of calculated strain already exceeds that of measured
579 under quasi-static loading (ca. 0.8% (Mohagheghian et al., 2016)). This can be due to the higher
580 failure strain of glass under dynamic loading. Nie et al. (2010) found that depending on the
581 surface condition of the glass, dynamic failure strain can be five times greater than its quasi-
582 static value. The strain at location 2 is compared in Fig. 15c between the two nose shapes. The
583 existence of initial high intensity strains is apparent for the flat nose shape. The level of strain
584 becomes more similar later on the deformation stage for the two nose shapes.

585 In addition to intensity of pressure in Stage 1, the other important factor which can have a
586 significant influence on the growth of the surface micro-cracks is the duration of Stage 1 (Van
587 Der Zwaag and Field, 1983). This itself is nose shape dependent and is shorter for a hemi-
588 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. 16 (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. 16 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. 11b). The front glass layer here acts as a sacrificial layer; breaks in the very early stage of the loading (Stage 1 in Fig. 10a) and protects the rest of the structure from premature failure, which is what happens for the thick monolithic glass in Fig. 9a.

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. 17a 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. 10). 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. 17b 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 100-180 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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828 **Figure Captions:**

829 Figure 1: Cumulative probability of failure for 2.2 and 4.0 mm chemically strengthened glass
830 tested using ring-on-ring flexural experiment.

831 Figure 2: (a) Manufacture of a thin-walled, light weight carrier for a silicon rubber projectile
832 (diameter of 23.5 mm and length of 50 mm). (b) Interaction of the projectile and its carrier with
833 a 5 mm aluminium alloy plate. The plate is painted black to visualise the deformation of the
834 projectile.

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

837 Figure 4: Finite element models used for (a) long-period (1 ms) and (b) short-period (50 μ s)
838 simulations.

839 Figure 5: Soft impact results of a laminated glass window (Case 2-Configuration I) at the
840 velocity of 170 m s⁻¹: (a) shows the projectile deformation; (b) and (c) display the out-of-plane
841 displacement and major principal strain contours over the observation area, calculated using
842 DIC.

843 Figure 6: Soft impact results of a laminated glass window (Case 2-Configuration II) at the
844 velocity of 174 m s⁻¹: (a) and (b) display the out-of-plane displacement and major principal
845 strain contours over the observation area, calculated using DIC; (c) shows the history of central
846 out-of-plane displacement and major principal strain (Markers in (c) are 25 μ s apart and each
847 corresponds to a contour plot in (a) and (b)); (d) and (e) are the plate profile displacing and
848 rebounding respectively; each profile in (d) and (e) corresponds to a contour plot in (a).

849 Figure 7: Comparison between the strain obtained from strain gauges and that calculated by
850 DIC for a laminated glass plate (Case 2-Configuration II) impacted at velocity of 174 m s⁻¹.

851 Figure 8: Damage in a laminated glass specimen (Case 2) impacted at velocity of 174 m s^{-1} .
852 The impacted side of the specimen was painted black prior to the impact. White in the left
853 photograph (front view) is an indicator for area in which glass fragment detached from the
854 plate.

855 Figure 9: High-speed image sequences for deformation and failure of: (a) 6.0 mm monolithic
856 chemically strengthened glass impacted at the velocity of 144 m s^{-1} ; (b) laminated glass plate
857 (Case 1) impacted at the velocity of 160 m s^{-1} .

858 Figure 10: Schematic of: (a) typical high velocity soft impact loading and (b-e) different
859 mechanisms and stages of damage in a laminated glass window.

860 Figure 11: Impact performance of laminated glass (Case 1) at various velocities (a)
861 Photographs of damaged laminated glass, taken from impacted side; (b) Maximum strain
862 obtained from the distal side of rear glass layer at the centre of the specimen, calculated by DIC
863 (solid symbols), and at 30 mm off-centre, from strain gauge (open symbols), against projectile
864 initial velocity; (c) Strain traces of the off-centre gauge at various impact velocities.

865 Figure 12: Comparison between experimental and simulation results for an impact by a hemi-
866 spherical projectile on a laminated glass window (Case 1) at the velocity of 158 ms^{-1} . The
867 comparison is made for (a) plate deformation profile, (b) central out-of-plane displacement and
868 major strain and (c) strain in y-direction at the location of the gauge (30 mm off-centre).

869 Figure 13: Maximum strain at the distal side of rear glass layer against projectile initial velocity
870 for two projectile nose shapes: flat and hemi-spherical. Solid symbols are the maximum major
871 strain at the centre of the specimen calculated by DIC and open symbols are the maximum
872 strain at 30 mm off-centre obtained from strain gauge.

Figure 14: 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 15: 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 16: 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 17: 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 for strain are obtained from finite element simulations at the impact velocity of 131 m s^{-1} .

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

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

895 **Figure 1:**

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899 **Figure 2:**

(a)

(b)

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902 **Figure 3:**

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905 **Figure 4:**

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Figure 5:

Figure 6:

Figure 7:

Figure 8:

Figure 9:

Figure 10:

Figure 11

Figure 12:

Figure 13:

Figure 14:

Figure 15:

Figure 16:

Figure 17:

(a)

(b)

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) Themoplastic Polturethane (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: $G_{\infty} + \sum_{i=1}^n G_i$