



Failure mode transitions in asymmetric laminated safety glass under concentrated loading: analytical and experimental study

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Abstract

Objectives: This study investigates the mechanical response and failure characteristics of asymmetric laminated safety glass with varying thickness ratios while maintaining a constant total thickness of 6 mm and using an EVA interlayer.

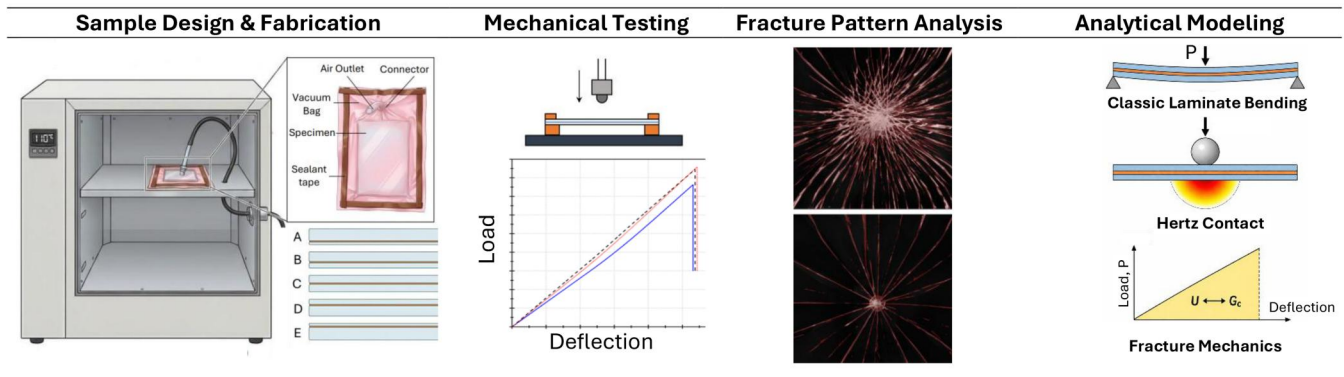
Methods: A hybrid methodology combining quasi-static concentrated loading experiments with analytical modelling based on Classical Laminate Theory and Hertzian contact mechanics was employed. Beyond measuring peak loads and displacements, a semi-empirical system identification approach was developed to quantify the interlayer bonding efficiency (I') and effective bending stiffness. Fracture patterns were analysed using digital image processing to correlate stored elastic strain energy with post-failure crack density.

Results: The results reveal a clear transition in failure behaviour depending on the outer glass layer thickness. Specimens with a thicker outer layer exhibited a global flexural failure mode, where the ultimate load scaled approximately linearly with laminate bending stiffness. In contrast, specimens with a thin outer layer under the applied load transitioned to a localized contact-dominated regime, where stress concentrations exceeded the nominal tensile strength of glass, causing premature failure below the global structural capacity. The specimen with the thickest outer layer achieved an ultimate failure load nearly twice that of the thinnest configuration and demonstrated approximately four times greater strain energy absorption before fracture.

Conclusion: These findings show that increasing outer layer thickness enhances load-bearing capacity by shielding the interlayer and delaying localized fracture, although it also produces a denser crack network due to higher pre-failure energy release. The proposed physics-informed scaling laws provide a framework for optimizing asymmetric laminated glass in safety-critical applications.

Keywords laminated safety glass, structural integrity, fracture mechanics, asymmetric layout, failure mode transition, stress shielding.

Graphical abstract



Highlights

- Asymmetric layup significantly influences the bending-extension coupling.
- A transition from global flexural to localized contact failure mode is identified.
- Thicker outer layers provide stress shielding, doubling the ultimate failure load.
- Strain energy storage in global bending is four times higher than in local modes.
- Fracture density correlates linearly with the pre-failure elastic strain energy.

1. Introduction

Glass is extensively used in architecture and automotive engineering due to its remarkable transparency, allowing clear, unobstructed views of the external environment from within the structure. However, compared to metallic, brick, concrete, or composite materials, glass typically represents the most vulnerable component in a structure [1, 2]. Sudden internal pressurisation often occurs within buildings, such as when wind-borne debris shatters a window, or doors are forcibly opened. This can significantly increase the load on interior walls and apply a greater upward force on the roof structure. Furthermore, fragments generated by shattered glass can pose an additional hazard to individuals [3–5]. For similar reasons, shattered glass poses a significant hazard to passengers in traffic accidents [6].

Since the last century, the methodology used to strengthen glass involves placing a flexible polymer sheet between two or more layers of glass, typically made of polyvinyl butyral (PVB), ethylene-vinyl acetate (EVA), or thermoplastic polyurethane (TPU) [7]. A range of researches have shown that lamination enhances the performance of glass in various mechanical aspects [8–10]. Since 1914, two-layer laminated glass has been used in the automotive industry [11].

Over the past decades, laminated glass has become an irreplaceable component of structural engineering, commonly applied to increase the strength of glass and to reduce the risk posed by glass fragments [12]. A substantial body of research has been

conducted to study the mechanical properties and dynamic response of laminated glass. In 1999, the fracture of glass-PVB laminates in biaxial flexure was studied by Bennison [13]. Then the static and dynamic performance of laminated glass beams is described by a mathematical model established by Aşık and Tezcan [11]. Subsequently, Ivanov [14] developed a model for the shearing effect of the interlayer in laminated glass. Galuppi and Royer-Carfagni [15] derived a new simple expression for the effective thickness of laminated glass, based on an approximate shape function for laminated plate deformation and by minimising the corresponding strain energy, addressing the shortcomings of traditional equivalent thickness definitions. Afterward, Xu *et al.* [16] established an experimental study to investigate the mechanical properties of laminated glass with PVB polymer under quasi-static and dynamic loads. Split Hopkinson Pressure Bar (SHPB) compression experiments with different loading cases were used for quasi-static and dynamic compression tests. The results demonstrate a strong rate-dependency of laminated glass, with nonlinearity shown in both quasi-static and dynamic loading tests. Based on these findings, a constitutive model was established, and the mechanism of crack generation is discussed. This study provides important data and models for further research. In 2014, Huang *et al.* [17] conducted an experimental investigation aimed at understanding the creep behaviour of laminated glass under prolonged loading at different temperature conditions. Their findings revealed that the PVB laminated glass exhibited significant creep behaviour. In contrast, the creep behaviour of Ionoplast laminated glass was negligible within a 24-h period. This research provides insights into the performance of laminated glass under long-term loading scenarios, emphasising the importance of considering temperature effects. In 2019, the bonding properties between glass and PVB were investigated by Samieian, and a FEA model was developed for analysis [18].

Dynamic loads such as impact and blast are included among the loading conditions that could potentially be applied to the glass structure [19]. Laminated glass consistently garners attention due to its superior dynamic load-bearing capacity, attributed to its enhanced energy absorption characteristics [20]. Therefore, apart from theoretical and static mechanical studies, extensive research has focused on the behaviour of laminated glass under impact, impulsive, and blast loads within the academic realm. Flocker and

Dharani [21] conducted a theoretical study on the stress details in laminated glass under low velocity impact in 1997. Zhang *et al.* [22] initiated numerical and laboratory tests on laminated glass windows under debris impact. Later, Zhang *et al.* [23] studied laminated glass under blast and impulsive loads. Nevertheless, the strength of PVB, EVA, or TPU is limited after the structure breaks [24]. To address this issue, a new type of polymer, Sentry Glass Plus (SGP), was developed to provide excellent post-breakage performance with higher strength and stiffness [25]. Wang *et al.* [26] conducted an experimental investigation into the low-velocity impact properties of SGP laminated glass. The Mean Minimum Breakage Velocity (MMBV) test method was applied to determine the energy of the breakage. However, a laminated glass panel can withstand 44% higher MMBV with clamped edges. Zhang *et al.* [7] designed a series of tests to research the influence of varying interlayers on the impact response of laminated glass. The investigation shows that the laminate materials significantly influence the impact behaviour of laminated glass. Under lower impact energy (3J and 5J), laminate materials with a low modulus exhibited better impact performance. Under higher impact energy (10J and 15J), laminate materials with superior dynamic mechanical properties demonstrated better impact performance. Additionally, Vedrtam and Pawar [27] conducted a comprehensive study on the critical thicknesses of laminated glass due to impact load through destructive ball drop tests. Their findings revealed that laminated glass with a PVB interlayer exhibited superior impact resistance, making it particularly suitable for structures prone to impact. Furthermore,

laminated glass with a higher critical thickness of EVA interlayer also demonstrated competitive impact resistance capabilities.

Despite extensive research on the mechanical behaviour of laminated glass, a critical knowledge gap persists regarding the influence of the thickness ratio between individual glass plies, specifically the asymmetry between outer and inner layers, on the overall structural performance under concentrated loading. While total laminate thickness is often a fixed design constraint, the strategic distribution of layer thicknesses offers a potent, yet under-explored, pathway for performance optimization. This study addresses this gap through a synergistic approach that integrates quasi-static experiments with analytical modelling based on Classical Laminate Theory (CLT) and Hertzian contact mechanics. We systematically investigate asymmetric configurations with outer-to-inner thickness ratios ranging from 5:1 to 1:5, while maintaining a constant total glass thickness of 6 mm and a uniform EVA interlayer of 0.38 mm. Beyond conventional strength metrics, a semi-empirical system identification framework is developed to quantify interlayer bonding efficiency and effective bending stiffness. Furthermore, digital image processing of fracture morphologies is employed to establish a fundamental correlation between stored elastic strain energy and post-failure crack density. By identifying the transition from global flexural failure to localized contact-dominated fracture, this work provides physics-informed scaling laws for the structural optimization of asymmetric safety glass laminates.

Table 1 Properties of glass sheets and EVA used in laminated glass specimens [28, 29].

	Density (kg/m^3)	Poisson's ratio	Young's modulus (MPa)	Tensile strength (MPa)	Shear strength (MPa)
Glass sheet	2500	0.24	75 000	125	6.9
EVA polymer	930	0.3	25	1.6	5.0

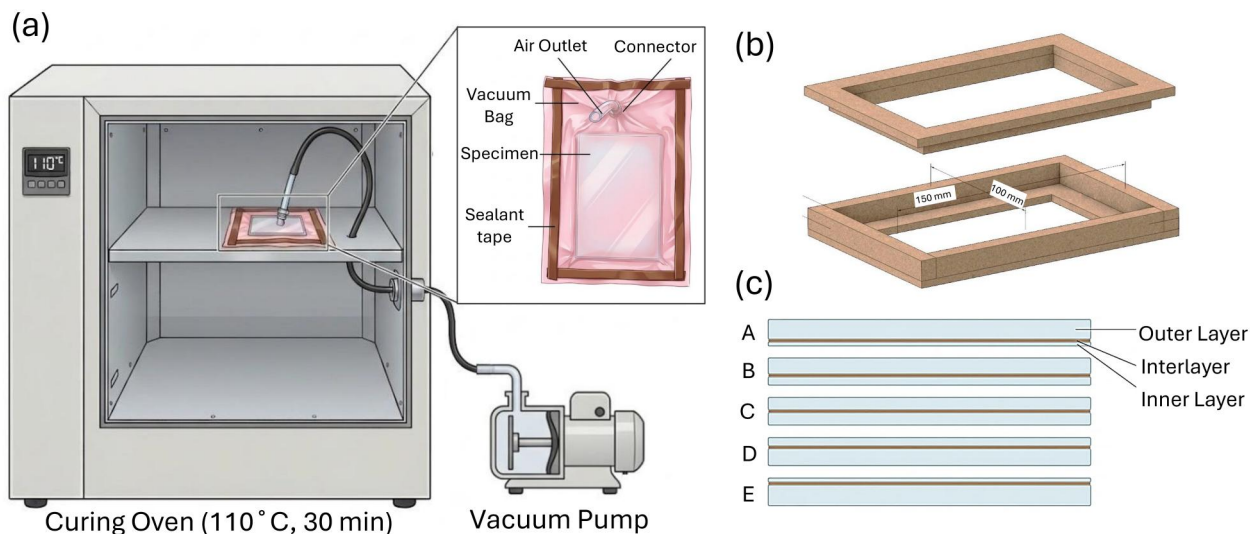


Figure 1 Schematic representation of the experiments. (a) Setup for the preparation of laminated glass specimens in a vacuum bagging system and curing oven, (b) configuration of the wooden fixture used to secure samples during mechanical testing, and (c) detailed geometric distribution of the outer glass, inner glass, and EVA interlayers for specimens A through E.

2. Materials and Methods

2.1 Material properties and specimen fabrication

The tempered glass sheets, which were purchased from EGW®, and the EVA film that was purchased from UNILAM® were used to manufacture the laminated glass specimens used in this research. The properties of these materials are listed in Table 1.

As shown in Fig. 1a, the EVA interlayers were placed between the glass sheets before they were packed into the vacuum bag. The vacuum bag consisted of two pieces of vacuum bagging film and was sealed with sealant tape. A connector, attached to a pump, was used to extract the air from the vacuum bag. The pump was connected to the vacuum bag through the wall of the Precision Composites Curing Oven, allowing for air extraction during heating and maintaining as complete a vacuum as possible throughout the entire process. All the equipment used was provided by EasyComposite. An illustration of the specimen preparation setup is provided below. The vacuum bag, containing the specimen, was heated in the curing oven to 100°C for 30 min, followed by naturally cooling it to room temperature.

Five distinct combinations of laminated glass structures were prepared, as given in Fig. 1c. The specimens, each with a total thickness of 6.38 mm, varied in terms of outer glass layer thickness (from 1 to 5 mm) and inner glass layer thickness (from 5 to 1 mm), with a consistent interlayer of 0.38 mm. The in-plane dimensions for all specimens are 150 mm × 100 mm.

The loads were applied to the outer layer in the tests. Additionally, a fixture was designed to maintain the specimen in an appropriate position for testing. This fixture, comprised of a support and a cap, allows ample space for specimen deformation during the experiment. It is made of wood and shaped via laser cutting. The fixture is equipped with rubber strips lining the contact position with the specimen to provide cushioning. The design of the fixture is demonstrated in Fig. 1b. The wooden rig defines an unsupported area of 125 × 75 mm², ensuring that

the central region of the specimen can undergo out-of-plane deformation during loading, while all four edges are restricted to rotate (clamped condition).

2.2 Quasi-static flexure test

A quasi-static flexure test was designed to study the static mechanical properties of laminated glass with different glass thickness distributions. The INSTRON 5900 tensile testing machine was used to apply the compression load to the specimen through a hemispherical head. The equipment setup and the quasi-static test procedure are illustrated in Fig. 2. The compression load applied to the specimen is displacement-controlled and will automatically stop when the reaction load has reduced to 10% of the maximum force prior to failure. The indenter has a descent rate of 2.5 mm/min.

2.3 Damage morphology analysis

The specimen images were pre-processed for crack identification after being captured in a specially designed lighting environment to enhance the visibility of the cracks. This pre-processing included enhancing the contrast and removing unnecessary elements from the original image. These processed images were then utilized for crack identification using AVIZO software. An alpha grey colour channel was established, and the main areas of cracks were selected based on a contrast threshold. Barely visible cracks were identified through threshold screening applied to subdivided local areas. The crack area percentage was then determined.

3. Results

3.1 Quasi-static tests

The results of the quasi-static tests, which include the peak loads and deformation (corresponding to the extension of the indenter) before failure, are shown in the Fig. 3. The ultimate (peak) failure load, P_u , is experimentally measured directly from

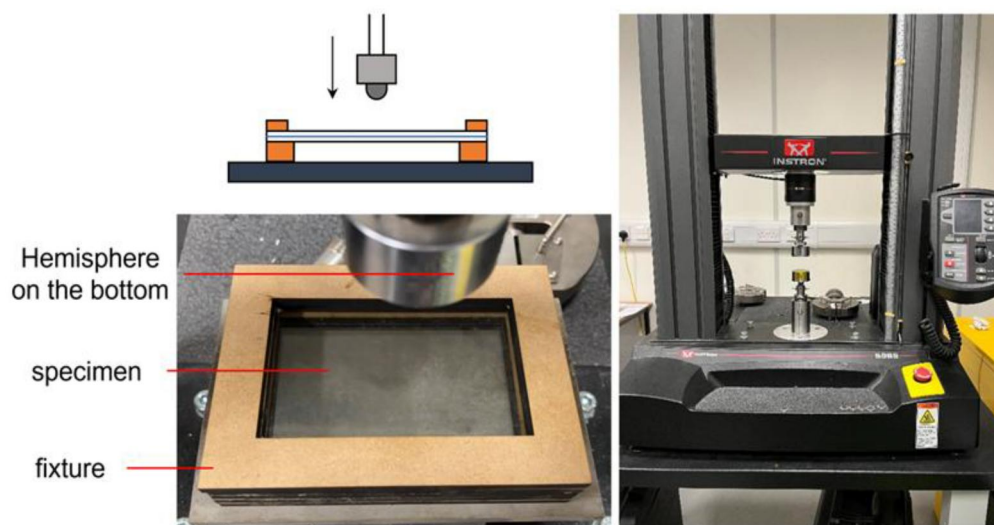


Figure 2 Experimental configuration for the quasi-static flexure test. Overview of the testing assembly featuring the INSTRON 5900 system equipped with a hemispherical indenter for controlled loading of laminated glass specimens.

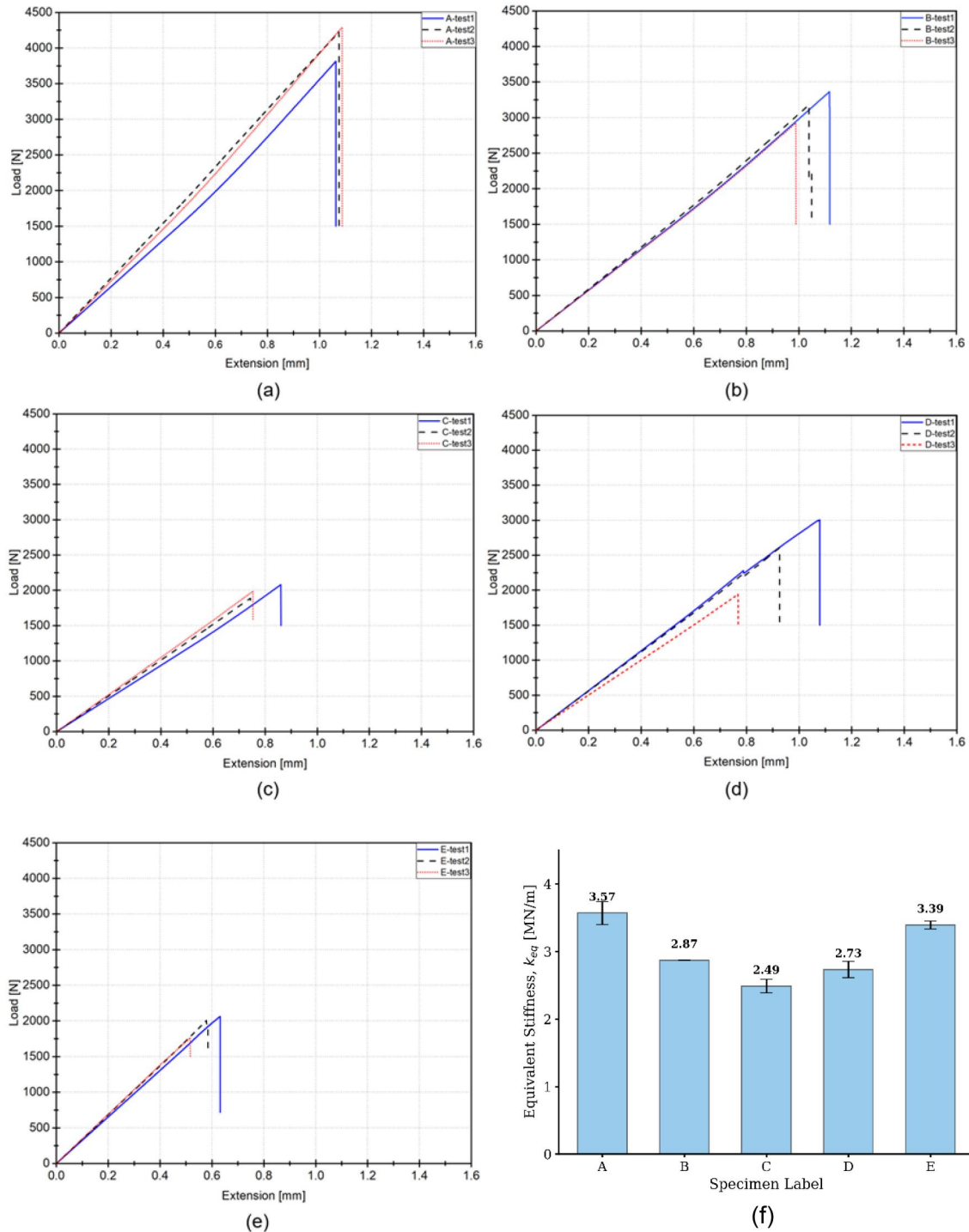


Figure 3 Load-displacement response and structural capacity of asymmetric laminates. (a–e) Quasi-static force-deflection curves for test sets A through E, respectively. Each panel displays three experimental repetitions to demonstrate result consistency and repeatability. (f) comparison of the equivalent structural stiffness, k_{eq} , for all configurations. The stiffness values, extracted from the initial linear elastic region (up to 0.2 mm displacement) in the load–extension curves presented in (a–e).

the load-displacement curves as the maximum force recorded immediately prior to the first major sudden load drop, indicating the macroscopic fracture of the glass ply. The summary of the quasi-static compression tests, including the average peak loads, the highest recorded peak loads, and the discrepancies of the most divergent values from the average for each test set, is listed

in Table 2. The table also lists the average deformations corresponding to each test set. It was found that specimens demonstrating superior load-bearing capacities correspondingly exhibit larger deformations upon failure. The ranking of test sets, in terms of load-bearing ability, aligns with the ranking in terms of maximum deformation. The observation of this phenomenon is

reasonable because of the linear behaviour of glass prior to damage. Consequently, a laminated glass structure designed with the thickest possible outer layer and thinnest possible inner layer, i.e. Sample A, not only showcases superior load-bearing capacity but also demonstrates enhanced displacement. It is important to note that glass, as a brittle material, typically exhibits strength variations governed by the distribution of microscopic surface flaws, often characterized by Weibull statistics. While the current study utilized three repetitions per configuration, sufficient to identify clear structural trends and transition points, the small sample size remains a limitation for a full statistical strength characterization. However, the observed reasonable discrepancy in peak loads and the consistent ranking of load-bearing capacities suggest that the global and local failure mechanisms described herein are robust and primarily driven by the laminate's geometric asymmetry rather than stochastic flaw distribution. This consistency is further attributed to the concentrated nature of the applied load; unlike uniform loading conditions, the localized pressure under the indenter constrains the failure initiation to a predefined zone. By effectively 'dictating' the failure location, the influence of the stochastic distribution of surface flaws is reduced, leading to a good degree of repeatability and low standard deviation observed in the experimental peak load measurements.

Based on the results, Sample A demonstrated the highest load-bearing capacity, with a highest recorded peak load of 4288 N and an average peak load of 4105 N. Conversely, Sample E exhibited the weakest load-bearing capacity, attaining a highest recorded peak load of 2062 N and an average peak load of 1942 N. Interestingly, the load-bearing capacity of Sample C closely matched that of the

weakest one, E. The difference in average peak load between Samples C and E was just 2.2%, which is less than the data variation within the tests for Sample E. Considering the inherent margin of error in laboratory testing, the performances of Samples C and E could be deemed essentially identical. It is evident that the thickness of the outer layer positively influences the strength of laminated glass structures. For instance, the average peak load of Sample E is merely 47% of that of Sample A, even though they share the same total thickness. Sample B, with an outer layer thickness of 1 mm less than that of A, exhibits a peak load 924 N lower than that of A. Nevertheless, the thickness of the thickest layer in a laminated glass also has a bearing on structural strength. For instance, Sample D, with a 2-mm outer layer, demonstrates a significantly higher average peak load than C, which has a 3 mm outer layer.

Furthermore, Sample E registers a similar average peak load to C, despite the outer layer of the former being only 1/3 as thick as that of the latter. As a result, it can be concluded that the load-bearing ability of laminated glass is influenced by a combination of the two factors mentioned above. Therefore, in the design of a laminated glass structure, to obtain the maximum load-bearing ability, it is recommended to include a layer as thick as possible, which ideally should be the outer layer, to which most of the loads will be directly applied. Furthermore, although further verification through additional studies is required, the results potentially suggest that the strength and stiffness of laminated glass may be lower than that of a single glass sheet with the same thickness. This could be attributed to the challenges associated with achieving perfect shear coupling between the interlayer materials and the glass sheets [30].

Figure 4b displays the peak ultimate failure loads, P_u , for all test series. Individual experimental repetitions are marked by crosses, while the red circles represent the mean values. The shaded vertical bands indicate the standard deviation, highlighting the consistency and scatter of the results for each specimen configuration. The average peak loads decrease when the outer layer thicknesses are 5, 4, and 2 mm. However, the peak loads are extremely similar when the outer layer is 3 and 1 mm. Among all the test sets, Sample C displays the most concentrated set of results, with a maximum discrepancy of 5.0%. In Section 3.2, these experimental observations are analytically analysed and interpreted by proposing the concept of competition between the global and local failure modes.

Table 2 Summary of quasi-static test results obtained from laminated glass specimens.

Specimen label	Highest recorded P_u (kN)	Average P_u (kN), discrepancy (%)	Average deformation corresponding to peaks (mm)
A	4.288	$4.105 \pm 7.1\%$	1.076
B	3.364	$3.152 \pm 7.1\%$	1.048
C	2.082	$1.986 \pm 5.0\%$	0.786
D	3.006	$2.518 \pm 22.8\%$	0.925
E	2.062	$1.942 \pm 9.2\%$	0.578

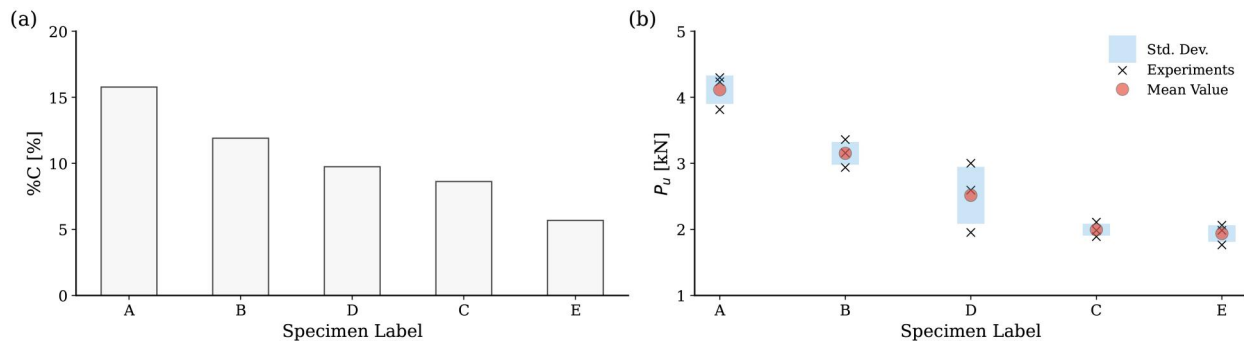


Figure 4 Experimental measurements from crack identification analysis and quasi-static tests. (a) The average cracking percentages of tested laminated glass with different configurations. (b) Distribution of peak loads for each test set, illustrating the average values of peak loads and the error ranges for tested laminated glass specimens.

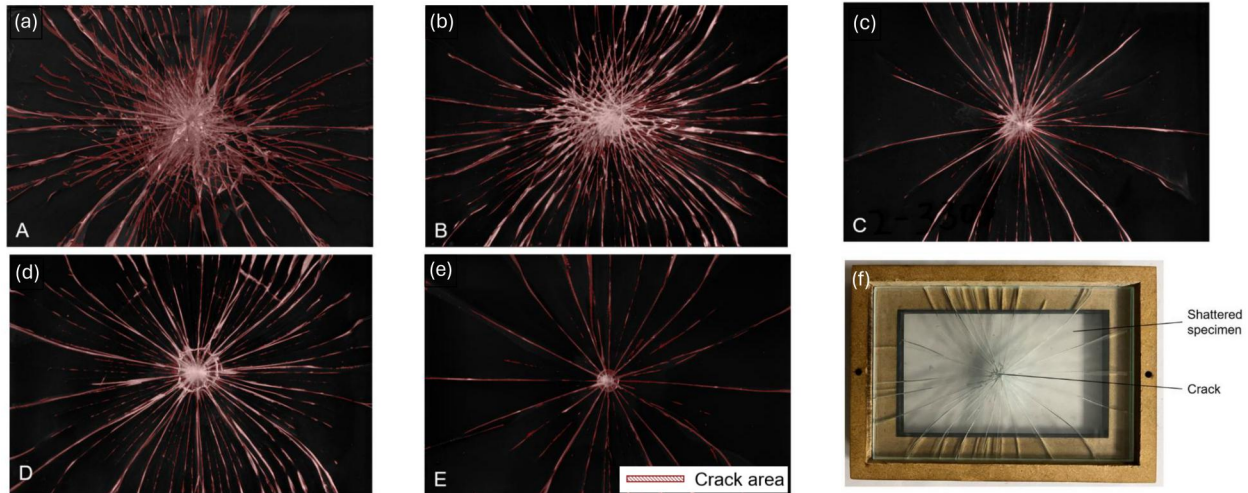


Figure 5 Fracture pattern and damage morphology analysis. (a–e) Comparative crack identification maps for all specimen configurations (A through E), illustrating crack patterns and regions across different thickness configurations. (f) Representative post-test image of a fractured laminated glass specimen secured within the wooden fixture, illustrating the overall crack distribution.

3.2 Crack identification

The pre-processed image captured in a specially designed light environment was employed for crack identification using AVIZO software. Figure 5a–e shows that the higher the peak load sustained in the quasi-static test, the denser and more extensive the resulting crack network is. Conversely, specimens with lower peak loads (e.g. Sample E) exhibit a less intense fracture pattern, characterized by a highly localized crushed zone immediately beneath the indenter. This observation suggests that the larger the load applied to the structure, the more energy is released upon failure. In other words, a laminated glass structure with a thicker outer layer will store more strain energy prior to failure and generate a larger number of cracks post failure. Moreover, the specimens in categories A and B exhibit large-scale network cracks, whereas the remaining categories basically display radial cracks. This may be attributed to the significant discrepancy between the thicknesses of the outer (5 and 4 mm) and inner (1 and 2 mm) layers. As the load is applied, internal stress within the outer layer of the laminated glass gradually builds up. The thicker the outer layer, the greater this internal stress becomes. Upon the occurrence of damage, the internal stress accumulated in the outer layer is released and transferred to the inner layer as a series of irregular loads, due to the inherent randomness of brittle material failure. If the thickness of the inner layer is significantly less than that of the outer layer, the initial point of damage will likely occur somewhere else, rather than the loading point, namely, the centre of the top surface of the specimen. The intersection of cracks on the outer and inner layers with distinct starting points forms the network cracks, as can be observed in A and B. In addition, the average crack area as a percentage of the total specimen area for each test set has been calculated and presented in Fig. 4a. It can be observed that the sequence of average crack percentages mirrors the load-bearing capacity ranking, i.e. A, B, D, C, E, from high to low. It can also be found that the specimen categories featuring network cracks have larger crack percentages compared to the categories without network cracks. Specifically, the difference between B and D

is 4.16%, while that between B and A is only 1.87%. It should be noted that the image processing captures a 2D projection of the fracture network. Due to the glass transparency, through-thickness cracks are projected onto a single plane, meaning perfectly overlapping cracks may merge. Although this yields a 2D fragmentation index rather than an absolute 3D fracture area, using identical lighting and contrast settings across all tests ensures this metric robustly and consistently compares relative damage intensity among the specimens.

Figure 5f shows the damage morphology of a typical post-tested laminated glass. The shattered laminated glass sample displays multiple cracks radiating outward from the point of indentation. The glass has fractured into several large and small shards, with noticeable crack lines intersecting across the surface. The fragmentation pattern suggests significant internal stress buildup, typical of brittle material failure. Some areas show dense crack networks, indicating regions where the glass has experienced extensive stress and damage. Despite the fractures, the layers of the glass appear to remain bonded by the interlayer, preventing complete separation of the broken fragments.

4. Discussion

4.1 Stiffness and load-displacement analysis

To analyse the structural response of the asymmetric glass-EVA laminates, we consider a rectangular plate of dimensions $a \times b$ ($b > a$), where the origin of the Cartesian coordinate system is located at the centre of the mid-plane ($z = 0$). The z -axis is defined positive downwards. The laminate consists of three primary layers: an upper glass ply, a polymeric EVA interlayer, and a bottom glass ply, with the thicknesses t_u , t_p , and t_b , respectively, resulting in a total thickness $t = t_u + t_p + t_b$. The plate is mounted within a rigid frame that replicates clamped boundary conditions along all four

edges, effectively preventing edge rotations. Regarding in-plane constraints, the frame is assumed to be rigid against expansion (positive in-plane strain) due to the close fit of the glass plies; however, it allows for free contraction (negative strain) as the laminate pulls away from the frame edges. A concentrated transverse load P is applied at the centre ($x = 0, y = 0$) by a rigid steel sphere of radius R . The mechanical properties of the glass and polymer phases, characterized by their Elastic moduli, E_g and E_p , and Poisson's ratios, ν_g and ν_p , are detailed in Table 1.

According to the Classical Laminate Theory (CLT), the constitutive behaviour is governed by the extensional, A_{11} , extensional-bending coupling, B_{11} , and bending, D_{11} , stiffness components. These are derived by integrating over the thickness coordinates [31]:

$$A_{11} = \int_{-t/2}^{t/2} \bar{Q}_{11}(z) dz = \sum_{k=1}^3 (\bar{Q}_{11})_k (z_k - z_{k-1}), \quad (1)$$

$$B_{11} = \int_{-t/2}^{t/2} \bar{Q}_{11}(z) z dz = \frac{1}{2} \sum_{k=1}^3 (\bar{Q}_{11})_k (z_k^2 - z_{k-1}^2), \quad (2)$$

$$D_{11} = \int_{-t/2}^{t/2} \bar{Q}_{11}(z) z^2 dz = \frac{1}{3} \sum_{k=1}^3 (\bar{Q}_{11})_k (z_k^3 - z_{k-1}^3), \quad (3)$$

where the vertical coordinates for the interfaces, relative to the mid-plane, are defined as:

$$z_0 = -\frac{t}{2}, \quad z_1 = -\frac{t}{2} + t_u, \quad z_2 = -\frac{t}{2} + t_u + t_p, \quad z_3 = \frac{t}{2}. \quad (4)$$

The reduced stiffness for glass layers is $\bar{Q}_{11,g} = E_g / (1 - \nu_g^2)$, while we neglect the stiffness of the polymer layer, setting $\bar{Q}_{11,p} = 0$, as $E_p \ll E_g$.

Figure 6 illustrates the evolution of these stiffness components for the five experimental samples, namely A, B, C, D, and E, as a function of the bottom glass ply thickness, t_b . As shown in Fig. 6a, the extensional stiffness A_{11} remains invariant across all samples at 4.78×10^8 N/m. This is expected as the total volume of the high-modulus glass phase is constant, regardless of the individual ply thicknesses. In

contrast, the bending-extension coupling stiffness B_{11} exhibits a linear transition from negative to positive values in Fig. 6b. For the symmetric configuration, i.e. Sample C with $t_b = 3$ mm, the bending-extension coupling vanishes ($B_{11} = 0$), indicating a decoupled response. However, for asymmetric samples (A, B, D, and E), the non-zero B_{11} implies that any curvature induced by bending will necessitate an in-plane strain (or an induced axial force if boundaries are restrained) to maintain equilibrium.

The bending stiffness analysis in Fig. 6c highlights the significance of the 'lamination effect'. The fully bonded bending stiffness is significantly higher than the debonded case, where layers are assumed to slide without shear transfer. A critical observation is the parabolic trend of D_{11} , which reaches its maximum for the symmetric Sample C when the glass layers are fully bonded. As the asymmetry increases (moving toward A or E), D_{11} slightly decreases. However, it is important to note that the debonded stiffness (green curve) represents the lower bound of the system, where the structural integrity relies solely on the sum of the individual plies' intrinsic stiffnesses, neglecting the geometric contribution of the layer spacing. In the debonded scenario, the bending stiffness is solely the sum of the individual plies' intrinsic rigidities. Consequently, the symmetric configuration (Sample C) exhibits the minimum stiffness, as the sum of the cubes of the up, t_u , and bottom, t_b layer thicknesses ($t_u^3 + t_b^3$) reaches its lower bound when the thicknesses are equal. Hence, in contrast to the fully bonded case, D_{11} reaches its maximum for the symmetric Sample C.

For a clearer physical interpretation, we adopt a cylindrical bending approximation (one-dimensional model in the x -direction). In this framework, the displacement field consists of the transverse deflection $w(x)$ and the in-plane displacement $u(x)$, which lead to the mid-plane curvature $\kappa = -d^2w/dx^2$ and the mid-plane strain $\epsilon^0 = du/dx$. The resulting axial force N and bending moment M are expressed as [31]:

$$N = A_{11}\epsilon^0 + B_{11}\kappa, \quad (5a)$$

$$M = B_{11}\epsilon^0 + D_{11}\kappa. \quad (5b)$$

In the symmetric configuration (Sample C), the coupling term vanishes ($B_{11} = 0$), leading to a decoupled response where $M =$

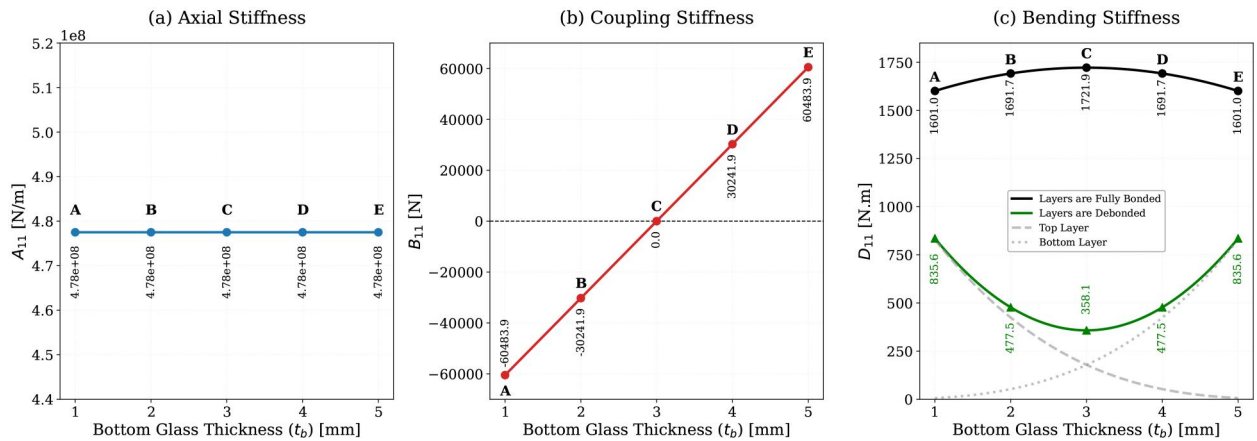


Figure 6 Analysis of the extensional, bending-extension, and bending stiffness components. (a) Constant extensional (axial) stiffness A_{11} across all samples; (b) linear transition of the bending-extension coupling stiffness B_{11} from asymmetric to symmetric configurations; (c) comparison between fully bonded and debonded bending stiffness D_{11} , illustrating the effect of layer thickness distribution and interlayer bonding on the overall structural rigidity.

$D_{11}\kappa$ and $N = A_{11}\epsilon^0$. However, for asymmetric samples, the non-zero B_{11} couples the bending and stretching modes. Under a downward load, which results in $\kappa > 0$, we analyse two distinct scenarios based on the in-plane boundary constraints:

- 1) **Expansion-Restrained (Samples A and B):** As shown in Fig. 6b, for these samples, $B_{11} < 0$. In the absence of constraints, i.e. $N = 0$, Equation (5a) would imply a positive induced strain $\epsilon^0 = -(B_{11}/A_{11})\kappa > 0$. However, the rigid frame prevents this expansion ($\epsilon^0 \approx 0$), inducing a compressive axial force $N = B_{11}\kappa < 0$. In this case, the bending stiffness remains at its full-bonded value D_{11} .
- 2) **Free-Contraction (Samples D and E):** For these samples, $B_{11} > 0$, tending to induce a negative strain (contraction). Since the frame provides no resistance to contraction, the axial force remains $N = 0$. Substituting $\epsilon^0 = -(B_{11}/A_{11})\kappa$ into Equation (5b) yields an effective bending stiffness, $D_{11}^{(\text{eff})}$ [31]:

$$M = \left(D_{11} - \frac{B_{11}^2}{A_{11}} \right) \kappa \Rightarrow D_{11}^{(\text{eff})} = D_{11} - \frac{B_{11}^2}{A_{11}} \quad (6)$$

It means for the samples with $B_{11} > 0$, i.e. Samples D and E, the effective bending stiffness is less their counterparts, A and B with $B_{11} < 0$, as experimentally confirmed in Fig. 3f.

4.2 Experimental identification of interlayer bonding efficiency

To quantify the structural performance of the glass-EVA laminates, a semi-empirical system identification approach is developed. This model reconciles the theoretical stiffness components derived from CLT (Section 3.2.1) with the measured load-displacement response, show in Fig. 3, by introducing the concept of bonding efficiency. Initially, the experimental equivalent

stiffness, k_{eq} , for the five laminate configurations was extracted from the initial linear slope of the load-deflection curves (up to 0.2 mm displacement), as presented in Fig. 3f. The recorded values for Samples A through E are 3.571 ± 0.17 , 2.872 ± 0.00 , 2.488 ± 0.10 , 2.733 ± 0.12 , and 3.393 ± 0.06 MN/m, respectively.

Based on the small-deflection CLT [31], the relationship between the concentrated centre load P and the maximum transverse deflection w for a rectangular plate is generally expressed as:

$$w = \frac{\alpha P a^2}{D_{11}} \Rightarrow k_{eq} = \frac{P}{w} = \frac{D_{11}}{\alpha a^2}, \quad (7)$$

where a represents the shorter span of the specimens, which is 75 mm for our samples. The dimensionless coefficient α accounts for the boundary elasticities, support conditions, and the specific geometry of the plate; thus, it is assumed to be identical across all tested samples. In the physical setup, the glass plates (150×100 mm) overlap the frame's aperture (125×75 mm), providing a continuous 12.5 mm gripped margin on all edges, see Fig. 1b. This geometric confinement heavily restricts edge rotations, resulting in a clamped edge condition. Furthermore, the high out-of-plane compressive stiffness of the wooden frame ($k_{\text{frame}} \approx 500$ MN/m) is orders of magnitude greater than the flexural stiffness of the samples (ranging from 2.49 to 3.57 MN/m, as shown in Fig. 3f). This substantial difference justifies the assumption of a rigid boundary. While the presence of pre-compressed rubber strips might introduce a negligible system compliance, such systematic boundary effects are effectively absorbed by the proportionality constant α in Equation (7). Because these boundary conditions are identical across all tests, the relative ranking of the equivalent stiffness, k_{eq} , and the comparative flexural response of the specimens remain consistent and valid.

In actual conditions, the bending stiffness D_{11} varies depending on the shear transfer capability of the thin EVA interlayer. To

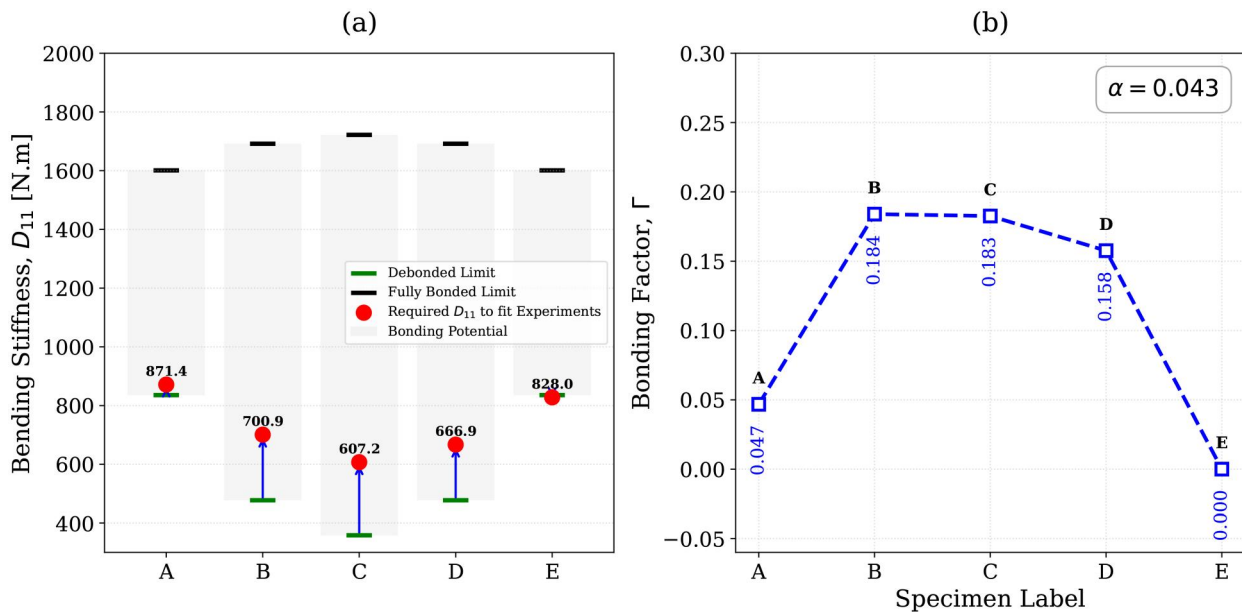


Figure 7 Interlayer bonding efficiency analysis of glass-EVA laminates. (a) Comparison of experimental effective stiffness with theoretical fully-bonded and debonded limits. (b) Identified bonding factor, Γ , for each sample.

account for this, we define the bonding factor, $0 \leq \Gamma \leq 1$, which reflects the efficiency of the interlayer bonding in contributing to the overall structural rigidity:

$$D_{11}(\Gamma) = D_{11,UB} + \Gamma(D_{11,B} - D_{11,UB}), \quad (8)$$

where $D_{11,B}$ and $D_{11,UB}$ represent the theoretical bending stiffnesses under fully bonded (perfect shear transfer) and unbonded (frictionless sliding) conditions, respectively. In this framework, $\Gamma = 1$ corresponds to a monolithic-like response where $D_{11}(\Gamma) = D_{11,B}$, while $\Gamma = 0$ implies a completely debonded state with $D_{11}(\Gamma) = D_{11,UB}$.

To align the CLT-based predictions with experimental observations, both α and Γ must be determined. A two-stage algorithm was implemented: first, we identify the minimum universal geometric factor, $\alpha = 0.04338$, that ensures the identified Γ remains within the physically consistent range of $[0,1]$ for all specimens. Second, substituting $D_{11}(\Gamma)$ from Equation (8) into Equation (7). Using the average experimental k_{eq} values presented in Fig. 3f, the bonding factors for each sample were calculated and are illustrated in Fig. 7b. In addition, Fig. 7a compares the identified effective bending stiffness required to match the experimental data with the two theoretical limits. It is observed that Samples B, C, and D exhibit moderate bonding efficiency ($\Gamma \approx 0.15 - 0.18$), indicating that the unbonded stiffness is insufficient to account for the observed structural resistance. This suggests a quantifiable degree of shear coupling provided by the EVA core. Conversely, Samples A and E require a significantly lower bonding factor to fit the experimental values, highlighting a diminished interlayer efficiency in these highly asymmetric configurations.

It should be noted that the use of the semi-empirical bonding factor, Γ , serves as a phenomenological approach to reconcile the theoretical stiffness bounds with macroscopic experimental measurements. While this effectively captures the overall structural response, exploring the underlying micromechanics would require advanced 3D Finite Element Analysis (FEA). However, developing a highly reliable FEA model necessitates precise experimental characterization of the cohesive interface properties between the glass and the EVA interlayer. As extracting these

localized interfacial parameters requires dedicated micromechanical testing beyond the macroscopic scope of the current study, comprehensive computational modelling of the interface degradation is earmarked as a critical direction for future research.

4.3 Contact mechanics and interlayer pressure distribution analysis

The localized loading of the laminated glass by a spherical metallic indenter of radius R generates a complex three-dimensional stress state. Although the contact might appear point-like, the elastic deformation of the upper glass layer results in a finite circular contact area. This phenomenon is analytically described using the classical Hertzian contact theory. The interaction between the spherical indenter and the flat glass surface, under an applied load P , results in a circular contact zone, A_0 , with radius a_0 . According to the Hertzian model, this radius is determined by [32]:

$$a_0 = \left(\frac{3PR}{4E^*} \right)^{1/3}, \quad A_0 = \pi a_0^2, \quad (9)$$

where E^* is the effective elastic modulus, defined by the Poisson's ratio and Young's modulus of the glass as $E^* = E_g / (1 - \nu_g^2)$. The pressure distribution on the surface follows a hemispherical profile, where the maximum contact pressure, p_0 , occurs at the centre of the contact circle [32]:

$$p(\eta) = p_0 \sqrt{1 - \left(\frac{\eta}{a_0} \right)^2}, \quad p_0 = \frac{3P}{2\pi a_0^2}, \quad (10)$$

where η represents the radial distance from the centre on the surface plane.

To evaluate the stress reaching the polymer interlayer, we define a local vertical coordinate ξ , where $\xi = 0$ at the top surface and increases toward the bottom. The peak compressive stress, σ_ξ , along the central axis attenuates as it penetrates the glass thickness. Following the Boussinesq-based approximation for contact mechanics, the stress at depth ξ is given by [32, 33]:

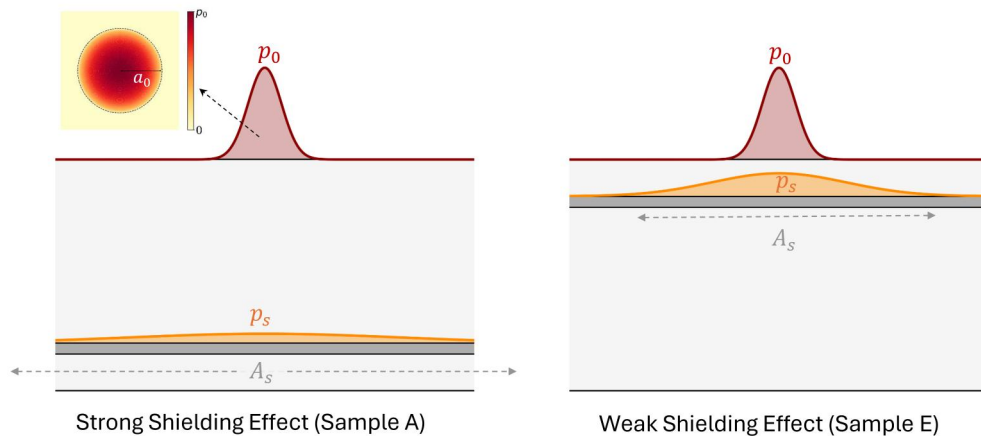


Figure 8 Schematic representation of the stress shielding effect. (Left) Thick upper layer providing high pressure attenuation and large area spreading. (Right) Thin upper layer showing sharper pressure transfer and high stress concentration on the interlayer.

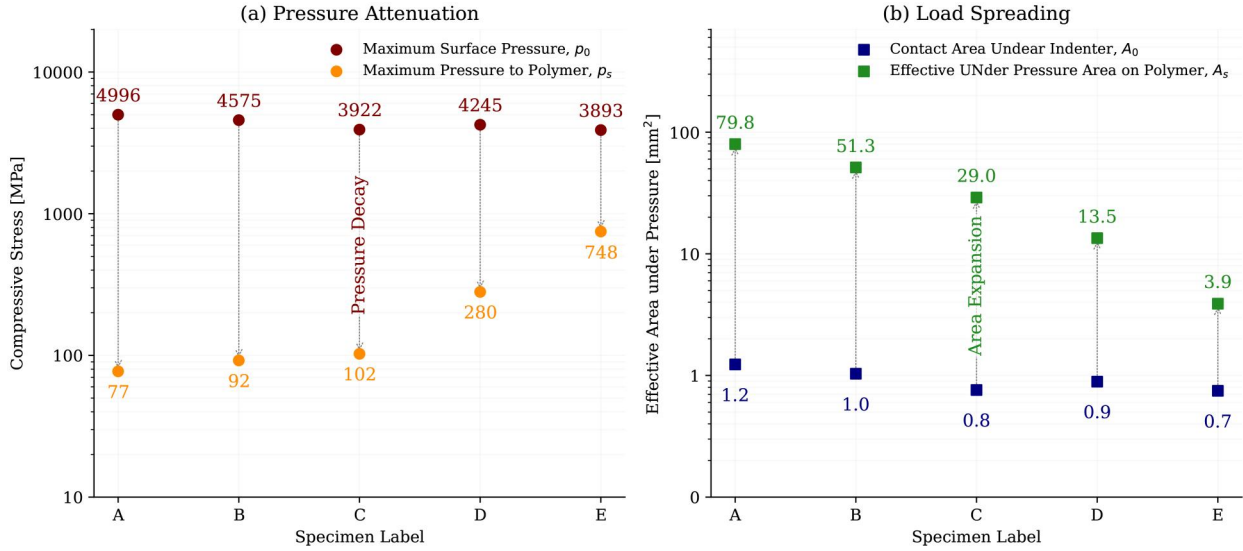


Figure 9 Quantitative contact analysis for specimens A–E. (a) Pressure attenuation showing the decay from surface p_0 to interlayer p_s . (b) Load spreading effect illustrating the expansion from A_0 to A_s . Values are plotted on a logarithmic scale for better visualization of the shielding performance.

$$\sigma_\xi = p_0 \left[1 + \left(\frac{\xi}{a_0} \right)^2 \right]^{-1}. \quad (11)$$

The maximum pressure transmitted to the polymer interlayer, p_s , is obtained by setting $\xi = t_u$, where t_u is the thickness of the upper glass layer:

$$p_s = \sigma_\xi|_{\xi=t_u} = p_0 \left[1 + \left(\frac{t_u}{a_0} \right)^2 \right]^{-1}. \quad (12)$$

As the pressure magnitude decays with depth, the load is distributed over a larger effective area to maintain force equilibrium. Considering the hemispherical pressure distribution, the effective spread area on the polymer surface, A_s , is calculated based on the equivalent force distribution as,

$$A_s = \frac{3P}{2|p_s|}. \quad (13)$$

The upper glass layer acts as a mechanical shield, filtering the high-intensity concentrated surface pressure before it reaches the relatively compliant EVA interlayer. As the schematic plot shows in Fig. 8, a thicker t_u facilitates a greater ‘pressure decay’ and ‘area expansion’. Physically, as t_u increases in Equation (12), the term $(t_u/a_0)^2$ in the denominator grows, leading to a drastic reduction in p_s . This results in a more uniform and ‘softened’ pressure profile on the polymer, preventing premature localized failure. In contrast, for thin upper layers, the pressure is transferred more sharply, subjecting the polymer to intense stress concentrations.

The quantitative results for specimens A through E are presented in Fig. 9. The applied load P was set equal to the average maximum load in each sample from Table 2 to evaluate the contact stress state at the breaking point. In Fig. 9a, the transition from Sample A with $t_u = 5$ mm to Sample E with $t_u = 1$ mm illustrates a significant surge in p_s . For Sample A, the high surface pressure of 4996 MPa is attenuated to a mere 76 MPa at the

interlayer interface. However, for Sample E, the transmitted pressure reaches from 3893 to 701 MPa, representing a much higher risk of interlayer delamination or crushing. Figure 9b confirms this by showing the load spreading. For Sample A, the initial contact area $A_0 = 1.2$ mm² expands to an effective area $A_s = 79.8$ mm² on the polymer, whereas for Sample E, the area expansion is much less ($A_s = 3.9$ mm²), indicating a high degree of stress concentration. It should be noted that the analytical derivation based on the classical Hertzian model strictly assumes contact between semi-infinite half-spaces. While this condition is well-satisfied for the thick upper ply of Sample A ($t_u = 5$ mm), the assumption is stretched for the membrane-like upper ply of Sample E ($t_u = 1$ mm), where the contact radius a_0 becomes comparable to the ply thickness. In such thin geometries supported by a compliant polymeric foundation, the glass ply undergoes micro-scale bending, which typically increases the actual contact area and slightly relieves the maximum peak pressure compared to ideal Hertzian predictions. Consequently, the Hertzian values calculated for Sample E represent a conservative upper-bound estimation. Nevertheless, this idealized comparative model effectively demonstrates the drastic loss of the stress shielding mechanism, explaining why the mechanical framework must transition from contact mechanics to a localized plate-bending approach to accurately capture the failure in thinner laminates. The pronounced localized stress concentration beneath the indenter, coupled with varying degrees of stress shielding and load transfer to the interlayer, likely governs the observed hierarchy in ultimate load capacities presented in Fig. 4b. This phenomenon, characterized by localized failure modes induced by Hertzian contact stresses, will be analysed and discussed in Section 3.2.4.

4.4 Failure mechanism analysis

The experimental results reveal a distinct transition in the failure mechanisms of the glass laminates as the thickness of the top

layer, t_u , varies. To understand this behaviour, two primary failure modes are proposed for investigation: localized contact-induced bending and global flexural failure.

For samples with a thin top layer, the intense stress concentration developed directly beneath the spherical indenter can precipitate premature failure. The localized region of the top glass layer, subjected to concentrated pressure, can be modelled as a circular plate with clamped edges. Under the assumption that the compliant polymeric substrate provides insufficient support to suppress any noticeable lateral deformation, this localized area undergoes localized bending. By approximating the contact pressure as an equivalent uniform load q distributed over a contact radius a , the maximum tensile stress induced by this local bending at the centre (specifically at the bottom surface of the upper glass ply) is given by [31]:

$$\sigma_{\text{local}}^{(\text{max})} = \frac{3qa^2}{4t_u^2}. \quad (14)$$

By assuming a uniform pressure distribution and substituting the average load q with the equivalent concentrated load $P = \pi a^2 q$, the expression simplifies to a form that is independent of the contact radius a :

$$\sigma_{\text{local}}^{(\text{max})} = \frac{3P}{4\pi t_u^2} \Rightarrow P_{u, \text{local}} = \frac{4\pi\sigma_{u,g}}{3} \cdot t_u^2, \quad (15)$$

where $\sigma_{u,g}$ is the ultimate tensile strength of glass. While the stress state directly beneath the indenter is multiaxial, failure in the localized bending mode initiates at the bottom surface of the upper glass ply, where through-thickness compression is minimal. Therefore, applying the Maximum Principal Stress Theory (Rankine criterion) with the nominal uniaxial tensile strength ($\sigma_{u,g}$) serves as a physically consistent threshold for the proposed analytical model. However, if a full 3D stress field were resolved via future FEA simulations, employing advanced multiaxial criteria like the Mohr-Coulomb model could further refine the failure envelope prediction. Substituting the average experimental ultimate failure loads, P_u from Table 1 into this localized model, the approximate bending stresses for specimens A through E are calculated as 39.2, 47.0, 52.7, 150.3, and 463.6 MPa, respectively. It is evident that for specimens D and E with $t_u = 2$ and 1 mm, the calculated stresses significantly exceed the nominal tensile strength of glass, i.e. ~ 125 MPa (see Table 1). This suggests that for thinner top plies, the failure is initiated by localized bending and high contact pressure, leading to the circular crushed zones observed in the fracture patterns of these specimens in Fig. 5d and e. It should be noted that while the analytical local model suggests a quadratic relationship between the failure load and ply thickness, $P_{u, \text{local}} \propto t_u^2$, the experimental ratio between Samples D and E ($P_{u, \text{local}}^{(D)} / P_{u, \text{local}}^{(E)} \approx 1.3$) is lower than the theoretical value of $(t_u^{(D)} / t_u^{(E)})^2 = 4$. This discrepancy is attributed to the transition in failure mechanisms; as the ply thickness increases from 1 to 2 mm, the localized bending is partially mitigated by the increased global stiffness of the laminate and a more effective load distribution across the polymeric interlayer. Consequently, Sample D benefits from a higher degree of structural support compared to the nearly membrane-like behaviour

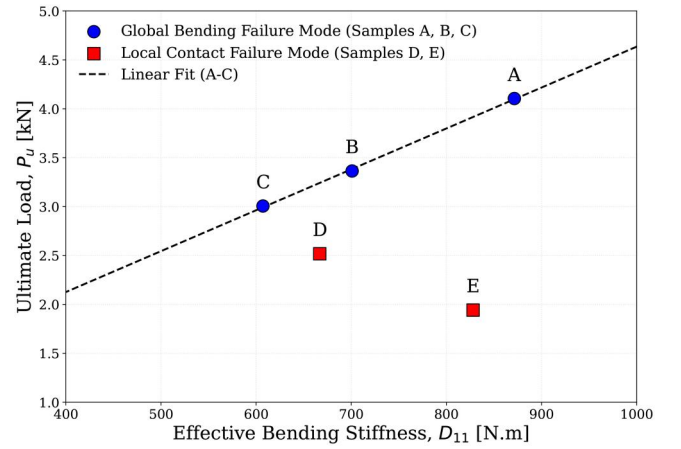


Figure 10 Experimental ultimate failure load as a function of the calibrated bending stiffness. The dashed line represents the linear fit for specimens A, B, and C, highlighting the transition from global flexural failure to local contact-induced fracture for specimens D and E.

of the 1 mm top ply in Sample E, leading to a deviation from the idealized local failure model.

In contrast, for specimens A, B, and C, where the top layer is sufficiently thick, the localized stress remains well below the critical threshold of 125 MPa. These specimens undergo significant global elastic deformation, storing a substantial amount of elastic strain energy before fracture. The sudden release of this energy upon reaching the global bending capacity results in the complex and extensive crack networks observed in their fracture surfaces in Fig. 5a and b. To quantify this, the experimental failure loads are plotted against the bending stiffness, D_{11} , derived from the fitting of the load-displacement curves, introducing the interlayer bonding factor, Γ , in Equation (8). As shown in Fig. 10, a strong linear correlation is observed for specimens A, B, and C. This relationship can be expressed through a scaling law:

$$\sigma_{\text{global}}^{(\text{max})} = \beta \frac{P}{D_{11}}, \Rightarrow P_{u, \text{global}} = \frac{\sigma_{u,g}}{\beta} D_{11}, \quad (16)$$

where β is a constant coefficient representing the system's geometry and boundary conditions. The high linearity ($R^2 > 0.99$) for the first three Samples confirms that their failure is governed by the global flexural capacity of the laminate. In contrast, the deviation of Samples D and E from this trend clearly indicates the transition to a contact-dominated failure regime.

The identified transition in failure modes suggests that the ultimate load-bearing capacity of asymmetric laminates is not governed by a single parameter (e.g. thickness of top glass layer), but rather by the competition between localized and global stress fields. For future studies focused on the structural optimization of laminated glass, such as in automotive windshield applications, the following physics-informed scaling functions are proposed to represent the idealized failure limits:

- 1) **Localized Failure Regime:** Under the assumption of pure local contact bending, the failure load $P_{u, \text{local}}$ is primarily sensitive to the top layer thickness, t_u [see Equation (15)]:

$$P_{u,local} = f_1(t_u) \approx \lambda_l \cdot t_u^2, \quad (17)$$

where λ_l is a material-dependent and boundary-related constant.

2) Global Failure Regime: When the failure is dictated by the global flexural capacity, the ultimate load $P_{u,global}$ scales linearly with the identified effective bending stiffness D_{11} , as suggested in Equation (16). By expanding the D_{11} term, derived in Equation (8), and neglecting the intrinsic stiffness of the polymer ($E_p \ll E_g$), the scaling function can be proposed as:

$$P_{u,global} = f_2(t_u, t_p, t_b) \approx \lambda_g[(t_u^3 + t_b^3) + \Gamma \cdot \frac{3t_u t_b(t_u + t_b + 2t_p)^2}{t_u + t_b}], \quad (18)$$

where λ_g is a proportionality constant encompassing the material strength and plate geometry and boundary, and Γ is the interlayer bonding efficiency factor.

In practical scenarios, the actual failure load may not strictly follow these individual asymptotic limits but rather emerge from a complex interaction between them, i.e. $P_u \approx \min(f_1, f_2)$. While the current experimental data show strong alignment with these conceptual trends, further investigations, potentially utilizing machine learning frameworks informed by these physical scaling laws, could provide a more generalized mapping of the design space. Such an approach would enable the tailoring of asymmetric laminates to maximize safety and energy absorption while minimizing total weight, bridging the gap between simplified analytical models and the multifaceted nature of brittle fracture in layered systems.

4.5 Fracture energy analysis

The energy absorption capacity of laminated glass-EVA under concentrated loading is a critical indicator of its structural integrity and failure mechanism. This absorbed energy, representing the elastic strain energy stored in the system up to the point of failure, can be experimentally determined from the area under the force-deflection ($P-w$) curves, presented in Fig. 3 for samples A to E. Given the nearly linear elastic behaviour of the glass plies prior to fracture, this energy, U , is calculated as:

$$U = \frac{1}{2} P_u w_u, \quad (19)$$

where P_u and w_u denote the ultimate load and the corresponding central deflection at the onset of fracture, respectively. The values for these parameters across different specimen categories are detailed in Table 2.

To provide a structural basis for this energy storage, the system can be modelled as an equivalent elastic plate. The total structural stiffness ($k_{eq} = P/w$) for a plate under concentrated loading is directly related to its bending stiffness, D_{11} , and geometric dimensions, as described in Equation (7); $k_{eq} = D_{11}/\alpha a^2$, where α is a geometric constant depending on the boundary conditions and a is the shorter side of the rectangular plate. By substituting this stiffness into the strain energy relation in a linear elastic regime, $U = \frac{1}{2} k w^2$, we obtain:

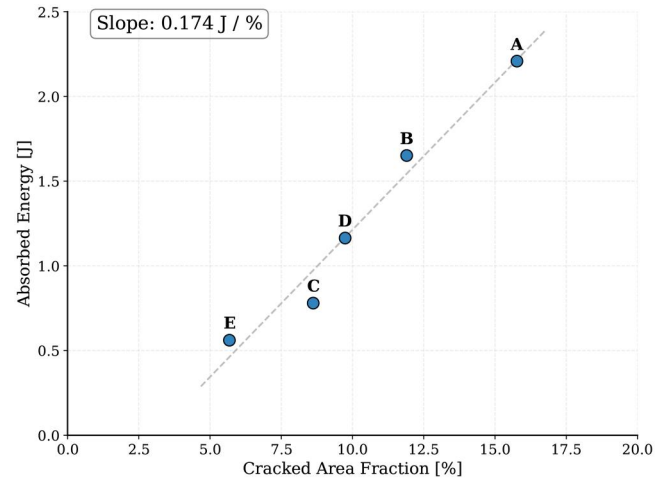


Figure 11 Quantitative correlation between pre-failure absorbed energy and fragmentation density. Linear relationship between the total strain energy, U , stored prior to fracture and the resulting cracked area fraction, %C, across Sample configurations A–E. The observed slope of 0.174 J/% signifies that the extent of the post-failure crack network is a direct physical manifestation of the released elastic energy, demonstrating a consistent energy-to-fracture conversion rate.

$$U = \frac{w_u^2}{2\alpha a^2} D_{11}. \quad (20)$$

As described in Equation (8), the actual bending stiffness is a function of the shear transfer factor, Γ , which dictates the efficiency of coupling between the glass plies through the polymer interlayer. $D_{11}(\Gamma)$ varies between the lower bound (unbonded stiffness, $D_{11,UB}$) and the upper bound (fully bonded stiffness, D_{11}). Substituting Equation (8) into Equation (20) yields the final analytical expression:

$$U = \frac{w_u^2}{2\alpha a^2} [D_{11,UB} + \Gamma(D_{11,B} - D_{11,UB})]. \quad (21)$$

Using the optimized geometric constant $\alpha = 0.043$ and the experimentally calibrated values of $D_{11,UB}$, $D_{11,B}$, and Γ , respectively from Figs 6c and 7b for each sample, the calculated energy values via Equation (21) show excellent agreement with the experimental area-under-the-curve results from Equation (19). The calculated absorbed energies are presented in Fig. 11.

The physical manifestation of this released energy in the fracture point is the creation of new fracture surfaces. As shown in Fig. 11, there is a strong linear correlation between the pre-failure stored energy and the resulting cracked area fraction, %C, measured after failure. The data fit a linear trend with a slope of 0.174 J/%, indicating that higher energy storage leads to more extensive fragmentation. To link this macroscopic observation to the fundamental physics of brittle fracture, we employ Griffith's criterion. The total fracture surface area, A_f , created within the glass thickness can be estimated by considering the geometry of the cracks. If A_{tot} is the total area of the panel and b_c is the average effective width of a crack line on the sample surface as seen in the top-view images, the total length of the cracks, l_c , is calculated as:

$$l_c = \frac{\frac{\%C}{100} \cdot A_{\text{tot}}}{b_c}. \quad (22)$$

Considering that each crack creates two new surfaces across the mean glass thickness, $\bar{t}$, the total fracture area is:

$$A_f = 2 \cdot l_c \cdot \bar{t}. \quad (23)$$

According to Griffith's theory, the energy required to create these surfaces is:

$$U = G_c \cdot A_f, \quad (24)$$

where G_c is the critical strain energy release rate. By substituting Equations (22) and (23) into Equation (24), G_c can be derived as:

$$G_c = \frac{U}{\%C} \cdot \frac{b_c}{2 \cdot \bar{t} \cdot A_{\text{tot}}} \cdot 100, \quad (25)$$

where $(U/\%C)$ is the slope of $U-\%C$ graph. For the current specimens, with $A_{\text{tot}} = 0.15 \times 0.1 = 0.015\text{m}^2$, $\bar{t} = 3\text{mm}$, and the experimental slope of $0.174\text{J}/\%$, the value of G_c depends on the effective crack width b_c . Assuming a realistic range of $b_c \approx 35 - 65 \mu\text{m}$ for fine cracks generated under concentrated loading, the calculated G_c falls within the standard range for soda-lime glass, i.e. $\approx 7 - 12 \text{ J/m}^2$. It should be noted that this estimation assumes most of the stored energy is converted into surface energy. In reality, a portion of the energy is dissipated through the deformation of the polymer interlayer or the support frame. However, the strong linear correlation confirms that the fragmentation process is fundamentally governed by brittle fracture mechanics, consistent with Griffith's theory.

5. Conclusion

In this study, the structural response and failure mechanisms of asymmetric glass-EVA laminates were comprehensively investigated through quasi-static concentrated loading tests. Specimens with a fixed total thickness of 6.38 mm were organized into five configurations (A-E), where the outer layer thickness was systematically varied from 5, 4, 3, 2, to 1 mm, with corresponding inner glass layer thicknesses, while the polymeric interlayer EVA has a constant thickness of 0.358 mm. By integrating Classical Laminate Theory (CLT), Hertzian contact mechanics, linear fracture mechanics, and a semi-empirical interlayer system identification framework, the following conclusions are drawn:

- **Stiffness Components of Laminate and Coupling Effects:** The asymmetric distribution of glass ply thicknesses significantly influences the bending-extension coupling stiffness, B_{11} . For samples with thicker upper layers (A and B), the expansion-restrained boundary conditions results in an axial pressure, maintaining high structural rigidity. In contrast, highly asymmetric samples with thin upper layers (D and E) exhibit an effective reduction ($-B_{11}^2/A_{11}$) in bending stiffness (D_{11}) due to free-contraction effects.
- **Interlayer Bonding Efficiency:** A semi-empirical identification model revealed that the bonding factor, Γ , which reflects

the efficiency of interlayer bonding through the EVA layer, is not uniform across all configurations. While symmetric and moderately asymmetric samples (B, C, and D) demonstrate stable shear coupling, highly asymmetric configurations (A and E) require significantly lower bonding efficiency to fit the stiffnesses observed in experiments. This indicates that the interlayer's ability to transfer shear is highly sensitive to the relative thickness of the glass plies.

- **Transition of Failure Modes:** A fundamental transition in failure mechanisms was identified based on the top layer thickness, t_u . For Samples A, B, and C ($t_u \geq 3 \text{ mm}$), the failure is governed by 'global flexural bending', characterized by a strong linear correlation between ultimate load and effective bending stiffness of laminates. However, in Samples D and E with a thin upper glass layer, the mechanism shifts to 'localized contact-induced bending', where stress concentrations underneath the indenter (exceeding the ultimate tensile strength of glass) trigger premature failure before the global structural capacity is reached.
- **Energy-Fracture Correlation:** Post-test image analysis confirmed that the total energy absorbed prior to failure, U , serves as a direct precursor to the extent of fragmentation. The strong linear correlation between U and the cracked area fraction, $\%C$, aligns with Griffith's brittle fracture criterion. The calculated critical strain energy release rate, $G_c \approx 7 - 12 \text{ J/m}^2$, confirms that the post-failure crack network is a physical manifestation of the released elastic strain energy stored during structural deformation.

These findings provide a robust framework for optimizing asymmetric laminated polymer-glass structures. The results suggest that for maximum load-bearing and displacement capacity, the thickness of the outer layer subjected to direct loading should be maximized. However, designers must balance this global capacity against the increased extent of fragmentation observed in higher-strength configurations due to the higher energy release at failure.

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Author contributions

Seyed Kamal Jalali (Formal analysis [lead], Investigation [equal], Visualization [equal], Writing—original draft [equal]), Minghan Du (Data curation [equal], Investigation [equal], Writing—original draft [equal]), Cuba-Kae Vanderpuye (Visualization [equal], Writing—review & editing [equal]), Xintian Xu (Data curation [equal], Visualization [equal], Writing—review & editing [equal]), Yigeng Xu (Methodology [supporting], Supervision [supporting], Writing—review & editing [equal]), Yifan Zhao (Project administration [supporting], Supervision [supporting], Writing—review & editing [equal]), and Haibao Liu (Conceptualization [lead],

Funding acquisition [lead], Project administration [lead], Supervision [lead], Writing—review & editing [equal]].

Conflicts of interest

None.

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Data availability

Data will be available on request.

References

- Osnes K, Børvik T, Hopperstad OS. Testing and modelling of annealed float glass under quasi-static and dynamic loading. *Eng Fract Mech* 2018;**201**:107–29. <https://doi.org/10.1016/J.ENGFRACMECH.2018.05.031>
- Henderson D, Harper B. Detailed modelling of the vulnerability of domestic housing to tropical cyclone winds, Tech. Rep., Cyclone Testing Station, 2023.
- Zhao S, Dharani LR, Liang X. Analysis of damage in laminated architectural glazing subjected to blast loading. *Adv Struct Eng* 2008;**11**:129–34. <https://doi.org/10.1260/136943308784069432>
- Zhao S, Dharani LR, Chai L *et al.* Analysis of damage in laminated automotive glazing subjected to simulated head impact. *Eng Fail Anal* 2006;**13**:582–97. <https://doi.org/10.1016/J.ENGFAILANAL.2004.12.038>
- Spiller K, Packer JA, Seica MV *et al.* Prediction of annealed glass window response to blast loading. *Int J Impact Eng* 2016;**88**:189–200. <https://doi.org/10.1016/J.IJIMPENG.2015.10.010>
- Allen TJ, Locke J, Scranage JK. Breaking of flat glass-part 4 size and distribution of fragments from vehicle windscreens. *Forensic Sci Int* 1998;**93**:209–18.
- Zhang X, Liu H, Maharaj C *et al.* Impact response of laminated glass with varying interlayer materials. *Int J Impact Eng* 2020;**139**:103505. <https://doi.org/10.1016/j.ijimpeng.2020.103505>
- Stelzer I. Challenging glass 2-conference on architectural and structural applications of glass, 2010. <https://doi.org/10.7480/cgc.2>
- Vedrtam A, Pawar SJ. Comparative evaluation and regression analysis of PVB, EVA, and sg inter layered laminated glass hardness. *Int J Adv Technol Eng Sci* 2015;**3**:444–50.
- Cai L, Zhang Y, Wei X *et al.* Study on the effect of different sandwich materials on the impact resistance of laminated glass. *Constr Build Mater* 2022;**360**:129603. <https://doi.org/10.1016/j.conbuildmat.2022.129603>
- Aşık MZ, Tezcan S. A mathematical model for the behavior of laminated glass beams. *Comp Struct* 2005;**83**:1742–53. <https://doi.org/10.1016/J.COMPSTRUC.2005.02.020>
- Behr RA, Minor JE, Norville HS. Structural behavior of architectural laminated glass. *J Struct Eng* 1993;**119**:202–22. [https://doi.org/10.1061/\(ASCE\)0733-9445\(1993\)119:1\(202\)](https://doi.org/10.1061/(ASCE)0733-9445(1993)119:1(202))
- Bennison SJ, Jagota A, Smith CA. Fracture of glass/poly(vinyl butyral) (butacite) laminates in biaxial flexure. *J Am Ceram Soc* 1999;**82**:1761–70. <https://doi.org/10.1111/J.1151-2916.1999.TB01997.X>
- Ivanov IV. Analysis, modelling, and optimization of laminated glasses as plane beam. *Int J Solids Struct* 2006;**43**:6887–907. <https://doi.org/10.1016/j.ijsolstr.2006.02.014>
- Galuppi L, Royer-Carfagni G. The effective thickness of laminated glass: inconsistency of the formulation in a proposal of EN-standards. *Composites B Eng* 2013;**55**:109–18. <https://doi.org/10.1016/j.compositesb.2013.05.025>
- Xu J, Li Y, Liu B *et al.* Experimental study on mechanical behavior of PVB laminated glass under quasi-static and dynamic loadings. *Composites B Eng* 2011;**42**:302–8. <https://doi.org/10.1016/j.compositesb.2010.10.009>
- Huang X, Liu G, Liu Q *et al.* An experimental study on the flexural performance of laminated glass. *Struct Eng Mech* 2014;**49**:261–71. <https://doi.org/10.12989/SEM.2014.49.2.261>
- Samieian MA, Cormie D, Smith D *et al.* On the bonding between glass and PVB in laminated glass. *Eng Fract Mech* 2019;**214**:504–19. <https://doi.org/10.1016/j.engfracmech.2019.04.006>
- Zhang X, Hao H, Ma G. Parametric study of laminated glass window response to blast loads. *Eng Struct* 2013;**56**:1707–17. <https://doi.org/10.1016/j.engstruct.2013.08.007>
- Osnes K, Dey S, Hopperstad OS *et al.* On the dynamic response of laminated glass exposed to impact before blast loading. *Exp Mech* 2019;**59**:1033–46. <https://doi.org/10.1007/s11340-019-00496-1>
- Flocker F, Dharani L. Stresses in laminated glass subject to low velocity impact. *Eng Struct* 1997;**19**:851–6. [https://doi.org/10.1016/S0141-0296\(97\)00162-4](https://doi.org/10.1016/S0141-0296(97)00162-4)
- Zhang X, Hao H, Ma G. Laboratory test and numerical simulation of laminated glass window vulnerability to debris impact. *Int J Impact Eng* 2013;**55**:49–62. <https://doi.org/10.1016/j.ijimpeng.2013.01.002>
- Zhang X, Hao H, Wang Z. Experimental study of laminated glass window responses under impulsive and blast loading. *Int J Impact Eng* 2015;**78**:1–19. <https://doi.org/10.1016/j.ijimpeng.2014.11.020>
- Belis J, Depauw J, Callewaert D *et al.* Failure mechanisms and residual capacity of annealed glass/sgp laminated beams at room temperature. *Eng Fail Anal* 2009;**16**:1866–75. Papers presented at the Third International Conference on Engineering Failure Analysis (Sitges, Spain, 13–16 July 2008) Part II. <https://doi.org/10.1016/j.engfailanal.2008.09.023>
- Xu C, Yuan Y, Zhao C *et al.* Dynamic crack-interface interactions in SGP laminated glass: an experimental investigation. *Mech Mater* 2018;**122**:76–84. <https://doi.org/10.1016/j.mechmat.2018.04.006>
- Wang X-e, Yang J, Liu Q *et al.* Experimental investigations into SGP laminated glass under low velocity impact. *Int J Impact Eng* 2018;**122**:91–108. <https://doi.org/10.1016/j.ijimpeng.2018.06.010>
- Vedrtam A, Pawar SJ. Experimental and simulation studies on fracture of laminated glass having polyvinyl butyral and ethyl vinyl acetate interlayers of different critical thicknesses due to impact load. *Glass Technol* 2017;**58**:169–78. <https://doi.org/10.13036/17533546.58.6.005>

- 28 Jintai High Pressure Glass Co., Ltd., [Calculation of glass strength](http://www.gaoyaboli.com/news/142.html) (2023). <http://www.gaoyaboli.com/news/142.html> (30 October 2023, date last accessed).
- 29 Saint-Gobain Building Glass UK. The effective thickness of laminated glass. Tech. Rep., Saint-Gobain, 2018.
- 30 Galuppi L, Manara G, Carfagni GR. Practical expressions for the design of laminated glass. *Composites B Eng* 2013;**45**:1677–88. <https://doi.org/10.1016/j.compositesb.2012.09.073>
- 31 Timoshenko SP, Woinowsky-Krieger S. *Theory of Plates and Shells*, 2nd edn. New York: McGraw-Hill, 1959.
- 32 Johnson KL. *Contact Mechanics*. Cambridge, UK: Cambridge University Press, 1985. Primary reference for Hertzian contact and pressure distribution models. <https://doi.org/10.1017/CBO9781139171731>
- 33 Timoshenko SP, Goodier JN. *Theory of Elasticity*, 3rd edn. New York: McGraw-Hill, 1970. Classic reference for fundamental elasticity and Boussinesq problems.



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