

Article

Comparative SPH–Finite Element Assessment of Aerospace Material Systems Under Bird-Strike Loading

Mohsen Lalehparvar ¹, Alex Nuttall ¹, Dhruva Bavaria ¹, Felix Massó Etxeberria ¹, Kaustubh Dwivedi ¹,
Hessam Ghasemnejad ^{1,*}, Pablo Coladas Mato ² and Wydo van de Waerd ³

¹ Aerospace and Aviation Theme, Faculty of Engineering and Applied Sciences, Cranfield University, Cranfield MK43 0AL, UK

² GKN Aerospace Services Ltd., Civil Airframe Technology, Global Technology Centre, Bristol BS34 6FB, UK

³ Fokker Aerospace B.V., Edisonstraat 1, 7903 AN Hoozeveen, The Netherlands

* Correspondence: hessam.ghasemnejad@cranfield.ac.uk

Abstract

Bird strikes cause aircraft damage, create serious risks to human safety and can contribute to catastrophic incidents, while continuing to impose substantial economic costs on airlines. The impact combines high kinetic energy with discontinuous, strongly nonlinear contact over a short duration, producing large structural deformations; appropriate nonlinear simulation techniques are therefore required to capture this complex interaction. For this purpose, the present study applies established Smoothed Particle Hydrodynamics (SPH)–finite element modelling ingredients to a controlled matrix of aerospace material systems and target geometries. The approach is first benchmarked against a published aluminium flat-plate bird-impact test using a raster-digitised force-history comparison, after which monolithic metallic and composite structures and source-described honeycomb-sandwich alternatives are assessed in flat-panel and curved leading-edge configurations. The results show that contact-force and local-displacement rankings depend strongly on target geometry and response metric, with the curved leading edge changing the ordering observed for the flat panel. More compliant systems generally permit greater local displacement, whereas stiffer systems restrict displacement but can sustain higher short-duration force peaks; consequently, no universal material ranking follows from a single response measure, and the results are most suitable for preliminary design screening.

Keywords: bird strike; SPH; explicit finite-element analysis; aircraft leading edge; honeycomb sandwich; numerical benchmark; verification limitations



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1. Introduction

Bird strike is a critical accidental loading case in the certification of civil transport aircraft because the impact combines high kinetic energy, short duration, large deformation and highly nonlinear contact. The wing leading edge is particularly vulnerable because it is directly exposed to the incoming projectile and must preserve both aerodynamic shape and the integrity of the primary load-carrying structure. Airworthiness requirements therefore impose demanding bird-strike resistance criteria for transport-category aircraft [1,2].

The structural response to bird impact is governed by the interaction between projectile hydrodynamics, material behaviour, geometry and boundary conditions. At certification-relevant velocities, a bird behaves predominantly as a soft-body projectile rather than as a rigid impactor. The resulting loading history is commonly described by an initial

shock or Hugoniot phase followed by release, steady flow and termination phases [3–5]. This transient response is difficult to reproduce experimentally and is computationally demanding because the projectile undergoes extreme deformation.

Previous studies have demonstrated the value of explicit finite-element methods, particularly when combined with Smoothed Particle Hydrodynamics (SPH), for modelling soft-body impact and large structural deformation [6,7]. However, direct comparisons among aluminium, carbon-fibre-reinforced polymer (CFRP), glass-fibre-reinforced polymer (GFRP), Kevlar/epoxy and sandwich constructions remain limited because published studies commonly use different bird models, impact velocities, geometries, material definitions and boundary conditions. Consequently, differences reported between material systems cannot always be attributed to material behaviour alone.

The objective is to quantify how response metric, target geometry, structural architecture and impact velocity affect predictions when the projectile definition, nominal structural mesh, contact treatment and output convention are held common. The production matrix contains six flat-panel and ten leading-edge configurations, each analysed at 90 and 180 m/s ($16 \times 2 = 32$ simulations). Monolithic AA2024-T3 aluminium, CFRP, GFRP and Kevlar/epoxy skins are compared with source-described Nomex and aluminium honeycomb-sandwich variants. The principal reported outputs are contact-force history and impact-centre transverse displacement U3. Source-reported ALLIE-per-mass values are retained only as an unverified archive indicator and are not interpreted as absorbed energy.

Established methodological basis. The double-hemispherical-cap projectile, hydrodynamic equation of state, SPH conversion approach, explicit shell formulation, general-contact treatment and published constituent laws are established modelling ingredients. This study does not introduce a new SPH kernel, contact algorithm, finite-element formulation or constitutive law.

Specific contribution and practical use. The contribution is a traceable cross-material and cross-geometry matrix that exposes metric-dependent ranking changes under a common nominal protocol. One new quantitative observation is that the 90 m/s GFRP-to-CFRP peak-force ordering reverses between the flat panel (approximately 125 versus 107 kN) and the leading edge (approximately 41 versus 50 kN), while GFRP impact-centre displacement is about three times the CFRP value in the 90 m/s leading-edge case (90.0 versus 29.5 mm). These comparisons are useful for hypothesis generation and preliminary screening only; their quantitative robustness to mesh, SPH resolution, solver controls, constitutive inputs and boundary conditions has not been established.

Bird-strike resistance is a long-standing structural-certification requirement and an important aircraft safety consideration. The increase in reported wildlife strikes and the consequences of major bird-ingestion and structural-impact events demonstrate the continuing relevance of this loading case. Regulatory frameworks, such as EASA CS-25 and the corresponding FAA requirements, define discrete bird masses, impact speeds and continued-safe-flight requirements for transport-category aircraft [1,2,8].

The physical response of a bird at high impact velocity is dominated by hydrodynamic effects. The classic work of Wilbeck and subsequent studies established the importance of the Hugoniot, release, steady-flow and termination phases in describing the pressure history generated by a soft-body projectile [3–5]. These phases produce the characteristic multi-peak force histories observed in many bird-strike experiments.

SPH is particularly attractive for this problem because it avoids the severe mesh distortion associated with conventional Lagrangian projectile meshes. SPH bird models have therefore been coupled with transient nonlinear finite-element models to simulate large deformation and strong discontinuities [6,9]. Coupled Eulerian–Lagrangian methods

provide an alternative approach, although the computational cost of the Eulerian domain can be substantial [10,11]. Validation at simplified structural levels is important because it separates uncertainties in the projectile model from those associated with the target structure [12].

Material behaviour also strongly influences bird-strike response. Rate-dependent plasticity and damage models are commonly used for aluminium alloys, while Hashin-type criteria are widely applied to composite laminates to distinguish fibre and matrix failure modes [13,14]. Previous studies have shown that laminate architecture, fibre orientation and material system can significantly alter both the deformation response and the damage distribution [15,16]. Sandwich structures can further redistribute impact loads through local skin bending and core crushing [17–20]. Nevertheless, the literature contains relatively few direct comparisons of a broad range of aerospace materials under common numerical conditions. The present work addresses this gap.

2. Numerical Approach and Geometrical Models

Bird strike is governed by short contact duration, large local deformation, inertia and rapidly evolving contact. The simulations were performed with an explicit dynamic SPH-finite element formulation in Abaqus/Explicit. Constitutive equations are documented below to the extent supported by the retained manuscript; describing a damage formulation does not establish that a particular damage mode occurred. Outcome-level failure claims are excluded where the corresponding production inputs and field histories are unavailable.

The modelling work comprised implementation checks followed by production comparisons. Flat-panel and rigid-impactor cases were used to inspect material definitions, discretisation and impactor representation. The same nominal workflow was then applied to a simplified NACA 23,015 leading-edge geometry. This common protocol improves internal comparability but does not remove uncertainty associated with constitutive calibration, geometry, constraints, mesh and particle resolution, or unavailable solver-control records.

2.1. Flat Panels

For implementation checks and baseline comparison, a 500 mm × 500 mm flat panel was created in Abaqus/CAE, as shown in Figure 1. The laminate lay-up comprised 24 plies with the following sequence (from front face to back face):

$$[0/45/0/-45/-45/0/45/0]_{3s}$$

This quasi-balanced lay-up was selected to provide comparable in-plane stiffness in the principal directions. The source manuscript describes flat sandwich panels with a 10 mm honeycomb region between two 2.5 mm skins and reports a 15 mm hexagonal cell size, a 0.063 mm nominal Nomex wall thickness and a 0.035 mm nominal aluminium wall thickness. Retained figures show a cellular hexagonal geometry, but the original text is internally inconsistent about shell-wall versus equivalent-continuum assignment. Because the production input decks and CAE database are unavailable, the exact element, section and material assignments cannot be independently re-audited. The term “honeycomb-sandwich configuration” below therefore identifies the source-reported model rather than a newly verified constitutive implementation (see Figure 1).

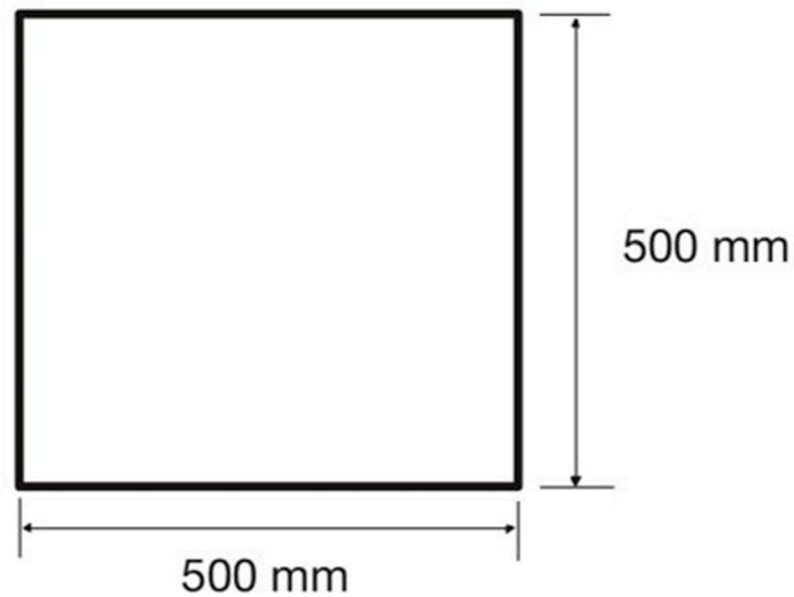


Figure 1. Flat-panel baseline geometry with 500 mm $\times$ 500 mm planform.

2.2. Leading-Edge Structures

The curved wing leading edge was generated from a NACA 23,015 aerofoil profile used as the reference section for this study. The profile was extruded to a spanwise length of 900 mm, and the forward 600 mm of the chord was retained to limit computational cost while preserving the leading-edge curvature. Monolithic leading edges were represented by a single shell surface with a uniform thickness of 5 mm. Sandwich configurations used separate outer-skin, inner-skin and explicit honeycomb cell-wall regions with a 10 mm skin separation. The core followed the leading-edge curvature and was tied to both skins. Figure 2 illustrates the profile definition and generated surface.

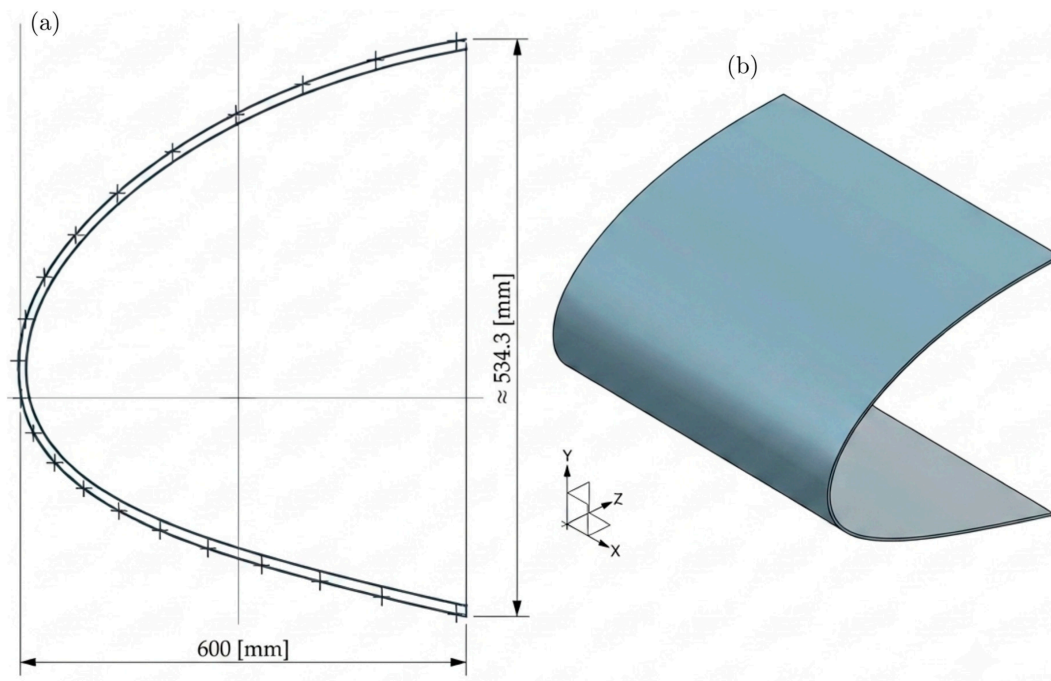


Figure 2. Leading edge CAD model. (a) forward 600 mm leading-edge profile used to define the curved wing segment and (b) three-dimensional leading-edge surface generated from the trimmed NACA 23,015 profile.

The following subsections reproduce the constitutive and geometric inputs available in the submitted manuscript for the metallic, composite and honeycomb-sandwich configurations. The retained package does not contain every production input deck, output database, failure-history field or post-processing script. Consequently, force and U3 histories are reported as archived comparative outputs, whereas damage mechanisms, rupture, core crushing, densification, debonding and residual capacity are not treated as verified outcomes.

2.3. AA2024-T3 Aluminium: Johnson–Cook Model

The authors' metallic model is designated consistently as AA2024-T3 aluminium [21]. A rate-dependent Johnson–Cook (J–C) plasticity law was used with the source-reported constants in Table 1. The T3 condition is represented through this selected parameter set; no heat-treatment or microstructural evolution model is implied. The flow stress is expressed as:

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

where ε_p is equivalent plastic strain, $\dot{\varepsilon}_p$ is plastic strain rate, $\dot{\varepsilon}_0$ is the reference rate and T^* is homologous temperature. The submitted manuscript describes Johnson–Cook damage, but the complete D1–D5 set, deletion controls and production status histories are not present in the retained package. Those settings therefore cannot be reproduced or used to support claims about rupture, deletion or containment. Table 1 reports only the available plasticity constants.

Table 1. Johnson–Cook plasticity constants for AA2024-T3 [21].

A (MPa)	B (MPa)	n	C	m	$\dot{\varepsilon}_0$ (s ^{−1})	T_m (K)
369	684	0.73	0.0083	1.7	0.01	775

2.4. Composite Laminates: CFRP, GFRP and Kevlar

Orthotropic elasticity combined with an intralaminar Hashin damage model was used for all composite plies [14]. The Abaqus Hashin formulation evaluates damage initiation using effective stress components, denoted by $\hat{\sigma}$, and separates the response into fibre-tension, fibre-compression, matrix-tension and matrix-compression modes. In failure-index form, damage initiation is identified when the relevant index reaches unity:

$$F_{ft} = \left(\frac{\hat{\sigma}_{11}}{X^T} \right)^2 + \alpha \left(\frac{\hat{\tau}_{12}}{S^L} \right)^2, \hat{\sigma}_{11} \geq 0, \quad (2)$$

$$F_{fc} = \left(\frac{\hat{\sigma}_{11}}{X^C} \right)^2, \hat{\sigma}_{11} < 0, \quad (3)$$

$$F_{mt} = \left(\frac{\hat{\sigma}_{22}}{Y^T} \right)^2 + \left(\frac{\hat{\tau}_{12}}{S^L} \right)^2, \hat{\sigma}_{22} \geq 0, \quad (4)$$

$$F_{mc} = \left(\frac{\hat{\sigma}_{22}}{2S^T} \right)^2 + \left[\left(\frac{Y^C}{2S^T} \right)^2 - 1 \right] \frac{\hat{\sigma}_{22}}{Y^C} + \left(\frac{\hat{\tau}_{12}}{S^L} \right)^2, \hat{\sigma}_{22} < 0. \quad (5)$$

The effective stress used in the damage initiation calculation is related to the true stress through the current fibre, matrix and shear damage variables:

$$\hat{\sigma} = \begin{bmatrix} \frac{1}{1-d_f} & 0 & 0 \\ 0 & \frac{1}{1-d_m} & 0 \\ 0 & 0 & \frac{1}{1-d_s} \end{bmatrix} \sigma, \quad (6)$$

Here, F_{ft} , F_{fc} , F_{mt} and F_{mc} denote fibre-tension, fibre-compression, matrix-tension and matrix-compression damage indices, respectively. X^T and X^C are the longitudinal tensile and compressive strengths, Y^T and Y^C are the transverse tensile and compressive strengths, S^L and S^T are the longitudinal and transverse shear strengths, and α controls the contribution of shear stress to fibre-tension initiation. After damage initiation, an energy-based linear softening law was used:

$$d = \frac{\delta_{eq}^f (\delta_{eq} - \delta_{eq}^0)}{\delta_{eq}^f (\delta_{eq}^f - \delta_{eq}^0)}, \quad (7)$$

where d is the damage variable, δ_{eq}^0 is the equivalent displacement at damage initiation and δ_{eq}^f is the equivalent displacement at complete failure. The fracture energy is represented by the area under the equivalent stress–displacement curve.

Composite-damage reproducibility note. The retained package does not contain the complete production damage-evolution inputs, including all mode-specific fracture energies, characteristic-length treatment, viscous regularisation and element-deletion rules. Table 2 preserves the available elastic and strength inputs, but quantitative damage extent and post-failure response cannot be reproduced and are not used as outcome evidence.

Table 2. Elastic and strength properties of CFRP (T700/M21), GFRP and Kevlar/epoxy used in the simulations. T700/M21 values are taken from [16].

Property	CFRP (T700/M21)	GFRP	Kevlar/Epoxy
Density ρ (kg/m ³)	1600	1425	≈1400
E_1 (GPa)	130	17	138
E_2 (GPa)	7.7	17	80
G_{12} (GPa)	4.8	6.1	5.5
G_{23} (GPa)	4.8	6.1	2.2
ν_{12}	0.33	0.12	0.34
X_T (MPa)—fibre tension	2080	325	400
X_C (MPa)—fibre compression	1100	325	1400
Y_T (MPa)—matrix tension	60	325	335
Y_C (MPa)—matrix compression	180	325	30
S_{12} (MPa)—in-plane shear	110	60	158

2.5. Honeycomb-Sandwich Geometry and Input Sets

Two honeycomb-sandwich sets were Nomex paper and AA2024-T3 aluminium. The retained figures depict a regular hexagonal cellular topology, but the submitted text alternately describes explicit geometry and orthotropic elastic solids. Table 3 is therefore labelled as material properties and geometry inputs and assigned to the core model. These

models were later combined with the skins presented in Section 2.2 to shape the final model of leading-edge sandwich panels.

Table 3. Source-reported honeycomb material and geometric input sets. The retained archive does not establish whether these values were applied to explicit walls or an equivalent core, or whether the Nomex strength rows were active in the analyses.

Property	Nomex Source-Reported Input Set	AA2024-T3 Source-Reported Input Set
Density ρ (kg/m ³)	720	2700
E_{11} (GPa)	2.5	73
$E_{22} = E_{33}$ (GPa)	1.3	73
$G_{12} = G_{13}$ (GPa)	0.7	28
G_{23} (GPa)	0.6	28
$\nu_{21} = \nu_{31}$	0.2	0.33
ν_{32}	0.3	0.33
Tensile strength (longitudinal, MPa)	90	516.6
Tensile strength (transverse, MPa)	60	516.6
Compressive/yield strength (MPa)	30	369
Failure strain	–	0.18
Bulk modulus K (GPa)	–	71
Core thickness (mm)	10	10
Cell size (mm)	15	15
Wall thickness (mm)	0.063	0.035

Figure 3 reproduces the honeycomb topology retained with the study. The source reports the same 15 mm nominal cell size for the Nomex and aluminium configurations and nominal wall thicknesses of 0.063 and 0.035 mm, respectively. These dimensions describe the intended geometry; exact section assignment, wall self-contact, element count and local mesh quality require the unavailable input/CAE files.

2.6. Bird and Rigid Impactor Models

The bird projectile was represented as a soft body using SPH. A double-hemispherical-cap body 226 mm long and 113 mm in diameter was assigned a nominal density of 950 kg/m³. Its geometric volume is 1.889×10^{-3} m³, giving 1.794 kg (reported as 1.8 kg). The retained manuscript reports approximately 46,000 particles. A uniform 0.5 mm spacing would instead imply roughly 1.51×10^7 particles and is therefore incompatible with the reported geometry and count. The mean particle volume V/N gives a cube-equivalent spacing of approximately 3.45 mm. This is a consistency estimate, not verification of the original seeding pitch, smoothing length or particle distribution; those parameters require the unavailable input deck. In SPH, a field variable $A(x)$ is approximated from neighbouring particles as:

$$A(x_i) \approx \sum_j A_j \frac{m_j}{\rho_j} W(x_i - x_j, h), \quad (8)$$

where m_j , ρ_j and A_j are the mass, density and field value of neighbouring particle j , and h is the smoothing length. For stable and physically meaningful interpolation, the kernel should satisfy compact support, normality and delta-function consistency [22]:

$$W(x - x', h) = 0, |x - x'| \geq h, \quad (9)$$

$$\int_{\Omega} W(x - x', h) dx' = 1, \text{ normality}, \quad (10)$$

$$\lim_{h \rightarrow 0} W(x - x', h) = \delta(x - x'), \text{ delta-function behaviour}. \quad (11)$$

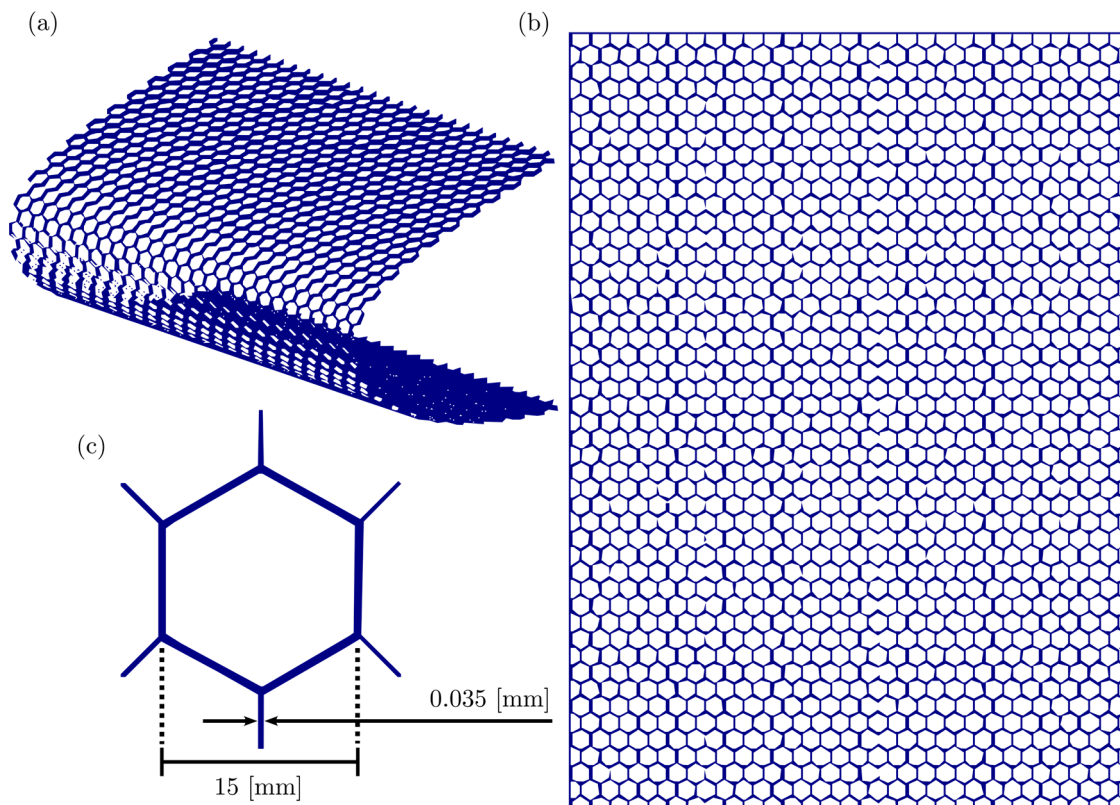


Figure 3. Representative retained honeycomb geometry: (a) cellular region wrapped to the leading-edge surface, (b) flat cellular sheet before wrapping and (c) nominal 15 mm hexagonal cell with source-reported 0.035 mm aluminium wall thickness. Exact element, section and material assignments cannot be verified without the production model files.

The initial kinetic energy of the projectile is:

$$E_k = \frac{1}{2} m_b v^2, \quad (12)$$

where m_b is bird mass and v is impact velocity. Increasing velocity from 90 to 180 m/s raises nominal initial kinetic energy by a factor of four, from 7.29 to 29.16 kJ for 1.8 kg. The particle count is approximately 46,000, but no separate SPH-spacing, smoothing-length sensitivity and numerical density/EOS sensitivity study was retained; the projectile discretisation and the SPH discretisation/smoothing convergence were not demonstrated to be converged (see Figures 4 and 5).

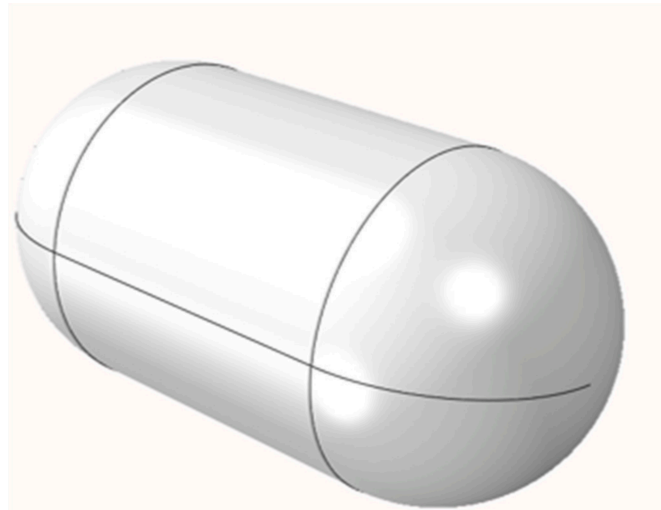


Figure 4. Schematic double-hemispherical-cap SPH bird geometry used for the soft-body projectile; nominal mass 1.8 kg. The drawing is illustrative and not to scale.

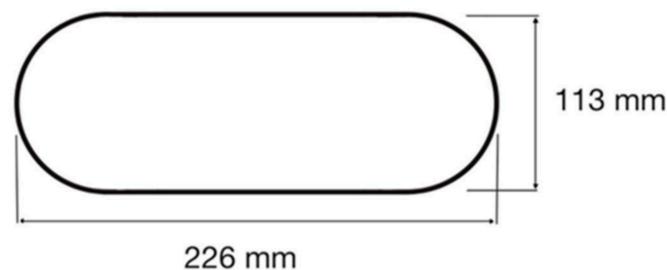


Figure 5. Principal dimensions of the DHC bird: 226 mm length, 113 mm diameter and 2:1 length-to-diameter ratio. The approximately 46,000-particle count implies a 3.45 mm cube-equivalent spacing, not a verified seeding pitch.

Bird material behaviour was described using the Us–Up Mie–Grüneisen equation of state:

$$P = \rho_0 c_0^2 \frac{\eta}{(1 - s\eta)^2} \left(1 - \frac{\Gamma_0}{2} \eta \right) + \Gamma_0 \rho_0 e \quad (13)$$

where P is pressure, ρ_0 is initial density, η is compressive strain, c_0 is bulk sound speed, s is the shock-slope coefficient and Γ_0 is the Grüneisen parameter. The source-reported values are $\rho_0 = 950 \text{ kg/m}^3$, $c_0 = 1482.9 \text{ m/s}$, $s = 2.0367$, $\Gamma_0 = 0$ and dynamic viscosity $\mu = 1 \times 10^{-9} \text{ Pa}\cdot\text{s}$, held common in the production comparisons. No simulation sensitivity sweep was retained. As a first-order scaling statement only, with geometry, c_0 and velocity fixed, a $\pm 5\%$ density change would produce $\pm 5\%$ changes in mass, initial kinetic energy and the acoustic estimate $\rho_0 c_0 v$; this is not a numerical sensitivity result (see Table 4).

Table 4. Mie–Grüneisen EOS parameters for the SPH bird model.

$\rho_0 \text{ (kg/m}^3\text{)}$	$c_0 \text{ (m/s)}$	s	Γ_0	$\mu \text{ (Pa}\cdot\text{s)}$	Bird Mass (kg)
950	1482.9	2.0367	0	1×10^{-9}	1.8

A rigid impactor provided a separate low-velocity implementation check for the structural material definitions; it is not treated as validation of the bird-strike production cases. The analytical rigid striker had a cylindrical body with a hemispherical nose, a mass of 5.52 kg and an impact velocity of 1.9 m/s. General contact with penalty enforcement and nominally zero friction was used. Quantitative experimental comparison metrics for this check were not retained and are not claimed.

2.7. Mesh Strategy and Element Selection

Structural parts were reported as S4R reduced-integration shells with nominal in-plane sizes of 20, 10, 5 and 2.5 mm in the mesh study. Figure 6 is a sensitivity study, not a convergence demonstration. The plotted peak changes from approximately 177 kN at 5 mm to 205 kN at 2.5 mm, giving $\delta F = |205 - 177| / 205 \times 100\% = 13.7\%$. This exceeds the study-defined 5% consecutive-refinement criterion; the 2.5 mm mesh is therefore only the finest practical mesh applied consistently, and no discretisation uncertainty is assigned. If the retained cellular geometry was discretised explicitly, 2.5 mm corresponds nominally to six divisions across the 15 mm cell dimension, but the local wall mesh cannot be verified. Composite plies are shell-section integration points rather than through-thickness solid layers. Convergence with respect to impulse, U3, curvature radius, localisation length and SPH spacing was not demonstrated.

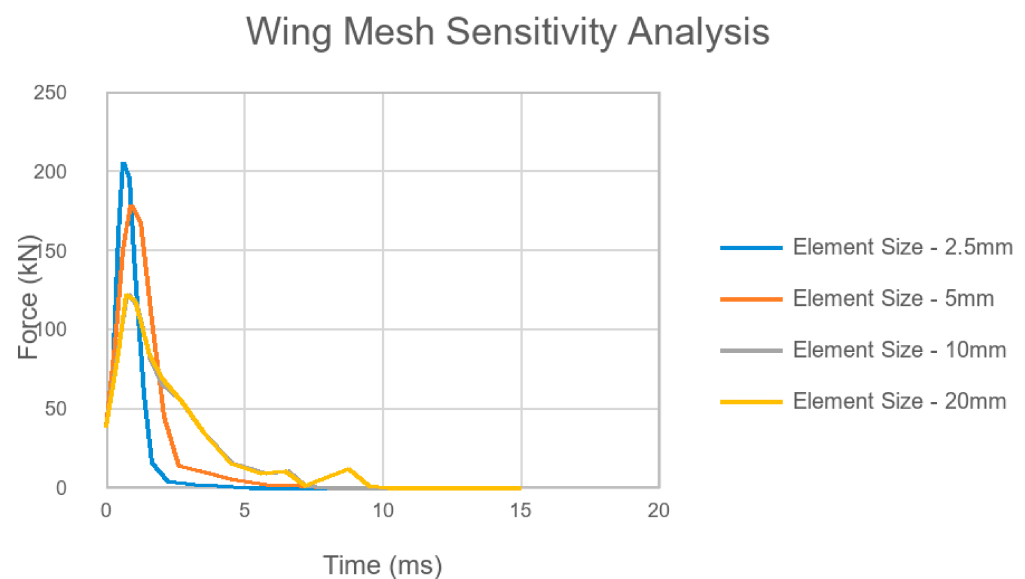


Figure 6. Leading-edge mesh-sensitivity force histories. The 5 to 2.5 mm peak-force change is 13.7% relative to the refined value and exceeds the study-defined 5% criterion; convergence is not demonstrated.

The source manuscript states that C3D8R precursor cells were converted to SPH particles using section controls. The retained files do not establish whether conversion occurred at time zero or after a distortion criterion, nor do they preserve the conversion threshold or smoothing-length controls. Figure 7 shows the leading-edge assembly and projectile at the initial position.

Solver-control and reproducibility disclosure. The retained package does not include the production .inp, .sta, .msg, CAE or ODB files. The exact step duration, stable-time-increment history, fixed or variable mass scaling and added mass, linear and quadratic bulk viscosity, shell hourglass/drill controls, SPH conversion threshold, smoothing-length controls and other SPH stabilisation settings cannot be verified. Unsupported defaults are not substituted. Independence from these controls and the requested numerical-quality thresholds have therefore not been demonstrated.

2.8. Contact Definitions and Boundary Conditions

The source manuscript describes Abaqus general contact with penalty enforcement and a near-zero friction coefficient. It also describes tie constraints between skins and the honeycomb region. Such ties impose perfect kinematic bonding and exclude adhesive failure and skin-core debonding by construction. Because the input decks are unavailable,

the complete contact-surface definitions, wall self-contact and enforcement settings cannot be re-audited; no debonding or post-crush load-transfer conclusion is drawn.

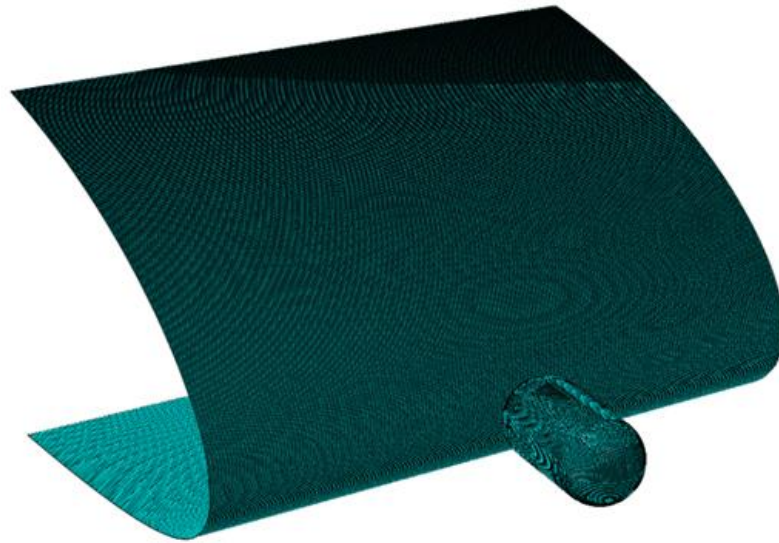


Figure 7. Meshed leading-edge model with SPH bird projectile positioned at the impact location.

Boundary conditions were simplified and do not reproduce the stiffness, load paths or local attachments of an in-service wing. Flat panels were fully clamped along two opposite edges. For the simplified leading edge, the source description most consistently identifies full constraint along the spanwise attachment edges with the remaining edges free; the original manuscript contained an inconsistent root/tip description, which has been removed. These idealisations support within-study comparison only and do not reproduce a prescribed certification test. Production velocities were 90 and 180 m/s; the separate rigid-impactor check used 1.9 m/s.

2.9. Impact Cases and Simulation Matrix

Thirty-two production simulations were reported: Table 5 contains 16 material–geometry configurations—six flat-panel and ten leading-edge configurations—and each was analysed at 90 and 180 m/s ($16 \times 2 = 32$). The Liu et al. benchmark and rigid-impactor implementation checks are separate and excluded from this count.

Output definitions. Contact-force histories retain the source convention. Leading-edge displacement is the reported impact-centre U3 nodal history; exact node labels are unavailable. Energy tables reproduce source-reported maximum ALLIE divided by reported mass and are not interpreted as absorbed energy. “Benchmark” is reserved for the one-condition Liu et al. force-curve comparison.

Table 5. Simulation matrix. Six flat-panel and ten leading-edge configurations, each at 90 and 180 m/s, give 32 production simulations; benchmark and rigid-impactor checks are excluded.

Geometry	Material Configuration	90 m/s	180 m/s
Flat panel	AA2024-T3 (monolithic)	Yes	Yes
	CFRP (monolithic)	Yes	Yes
	GFRP (monolithic)	Yes	Yes
	Kevlar/epoxy (monolithic)	Yes	Yes
	CFRP/Nomex sandwich	Yes	Yes
	CFRP/Al sandwich	Yes	Yes

Table 5. Cont.

Geometry	Material Configuration	90 m/s	180 m/s
Leading edge	AA2024-T3 (monolithic)	Yes	Yes
	CFRP (monolithic)	Yes	Yes
	GFRP (monolithic)	Yes	Yes
	Kevlar/epoxy (monolithic)	Yes	Yes
	CFRP/Nomex sandwich	Yes	Yes
	CFRP/Al sandwich	Yes	Yes
	GFRP/Nomex sandwich	Yes	Yes
	GFRP/Al sandwich	Yes	Yes
	Kevlar/Nomex sandwich	Yes	Yes
	Kevlar/Al sandwich	Yes	Yes

Note: Al denotes the source-labelled AA2024-T3 aluminium honeycomb configuration. Table 5 contains 16 configurations; each was run at both velocities, giving 32 production simulations. Benchmark and rigid-impactor checks are excluded.

3. Flat Panel Bird-Strike Simulation Results

Each flat panel was a 500 mm × 500 mm comparative target with two opposite edges fully clamped and a nominal 2.5 mm structural mesh. The nominal 1.8 kg SPH projectile impacted the centre at 90 or 180 m/s using the common source-described contact treatment. This is an idealised comparison, not an aircraft attachment or certification configuration.

3.1. Aluminium Flat-Plate Force-History Benchmark

The benchmark uses retained curves associated with Liu et al. [21], whose published programme combined flat-plate experiments with PAM-CRASH simulations and measured displacement, strain and reaction force at a fixture supported by eight force sensors. The cited test 1–24# used a nominal 1.8 kg bird at 69 m/s. The present retained curve is labelled “Abaqus/SPH,” but its exact force output, benchmark constraint implementation, mesh and material assignment cannot be recovered; fixture reaction and contact force must not be assumed equivalent. The following are therefore apparent plot-to-plot metrics, not like-for-like validation errors. From the final digitisation, present/experimental peak-force magnitudes are 59.7/60.0 kN (−0.5%) and peak times are 2.02/2.00 ms (+0.9%).

The force-magnitude impulse was evaluated as:

$$I_F = \int_0^{3\text{ms}} |F(t)| dt.$$

The corresponding values for the present and experimental curves are 67.1 and 71.4 N·s, respectively, giving a −6.0% difference. With no time shift and linear interpolation to a common grid, the normalised root-mean-square error was evaluated as:

$$\text{NRMSE} = \frac{\text{rms}(F_{\text{present}} - F_{\text{experiment}})}{\max|F_{\text{experiment}}|}.$$

The resulting NRMSE is approximately 21.5%. The waveform discrepancy is material even though the apparent peak values are close.

All benchmark values are approximate because they were digitised from raster plots rather than recomputed from archived histories; sub-pixel line thickness and axis calibration introduce digitisation uncertainty. Synchronous displacement, strain and damage-extent histories for the present reconstruction were unavailable, so comparable errors cannot be

reported. A rigid-target pressure history was also unavailable. The requested Wilbeck-type comparison of peak pressure, pulse duration and waveform could not be completed; broader validation claims have therefore been withdrawn. Table 6 separates the Liu experiment, Liu's PAM-CRASH model, the present retained Abaqus curve and the production matrix (see Figure 8).

Table 6. Evidence map separating the Liu et al. experiment, Liu's published PAM-CRASH simulation, the retained present Abaqus reconstruction curve and the production comparison matrix. "Unavailable" indicates information that cannot be recovered from the retained archive.

Feature	Liu Experiment	Liu Published Simulation	Present Retained Reconstruction	Production Matrix
Target	Flat aluminium plate bolted to a fixture; 10 mm in the cited test	Flat-plate representation in PAM-CRASH	10 mm flat plate associated with retained Abaqus/SPH curve	500 × 500 mm flat panels and simplified NACA 23,015 leading edges
Projectile/speed	Test 1–24#: nominal 1.8 kg bird at 69 m/s	SPH bird at the benchmark condition	Nominal 1.8 kg SPH bird at 69 m/s	Nominal 1.8 kg SPH bird at 90 or 180 m/s
Material	Source-designated aluminium; exact authors' designation not substituted	Published Liu material definition	Authors' AA2024-T3 model stated; exact assignment unavailable	AA2024-T3, CFRP, GFRP, Kevlar/epoxy and source-labelled honeycomb variants
Fixture/constraints	Plate bolted to fixture; fixture supported by eight force sensors on a heavy frame	Published fixture representation; see Liu et al.	Exact retained benchmark constraint implementation unavailable	Two opposite flat-panel edges clamped; idealised spanwise leading-edge attachment
Code/discretisation	Experiment	PAM-CRASH with SPH projectile	Abaqus/Explicit label retained; exact mesh and controls unavailable	Nominal 2.5 mm S4R structural mesh; sensitivity-based, not converged
Compared force	Measured fixture reaction force	Published numerical reaction/force curve	Curve labelled present Abaqus/SPH; exact output variable unavailable	Archived contact-force histories
Other outputs	Published displacement and strain measurements	Published displacement/strain comparisons	Present synchronous displacement/damage histories unavailable	Impact-centre U3 and source-reported ALLIE/mass indicator
Evidence status	Experimental reference condition	Published reference simulation	Approximate raster plot-to-plot benchmark only	Unvalidated screening-level comparative predictions

3.2. Monolithic Flat Panels

The archived 90 m/s flat-panel curves show a double-peak response, with the main peak between approximately 1.3 and 1.7 ms (see Figure 9). Approximate peak forces are 125 kN for GFRP, 112 kN for Kevlar/epoxy, 107 kN for CFRP and 104 kN for AA2024-T3. These values describe the retained curves; causal attribution to a single material property is avoided because modulus, density, lay-up, strength and unverified damage settings co-vary.

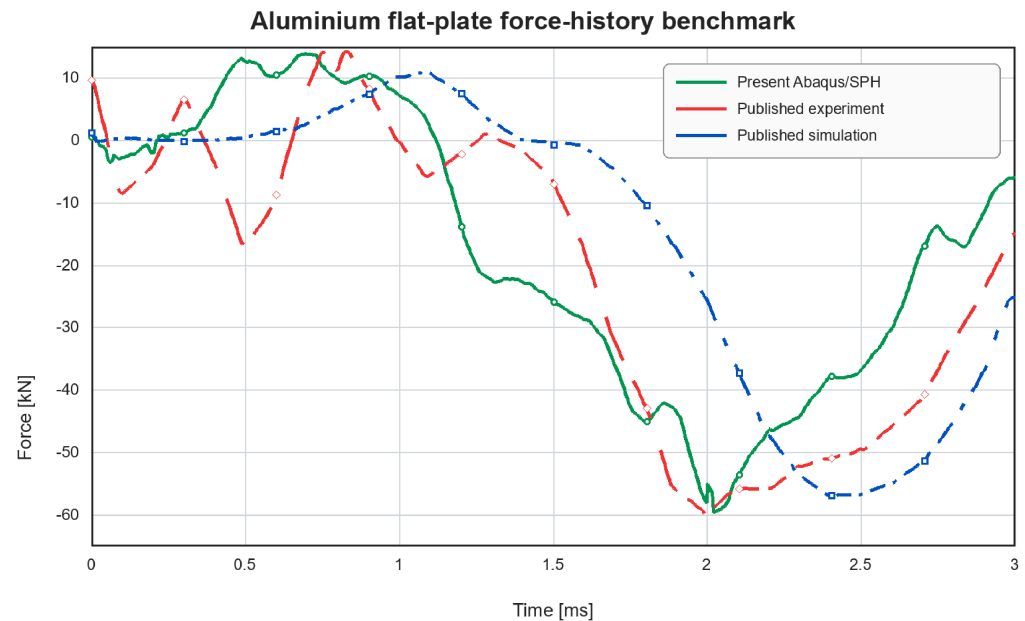


Figure 8. High-resolution replot of the retained aluminium flat-plate curves associated with a nominal 1.8 kg bird, 69 m/s velocity and 10 mm plate: present Abaqus/SPH curve, published experimental curve and Liu et al. published simulation [21]. Compression follows the source sign convention. Apparent present-versus-experiment differences from raster digitisation are -0.5% in peak magnitude, $+0.9\%$ in peak time, -6.0% in 0–3 ms force-magnitude impulse and 21.5% waveform. Exact force-variable equivalence is unverified.

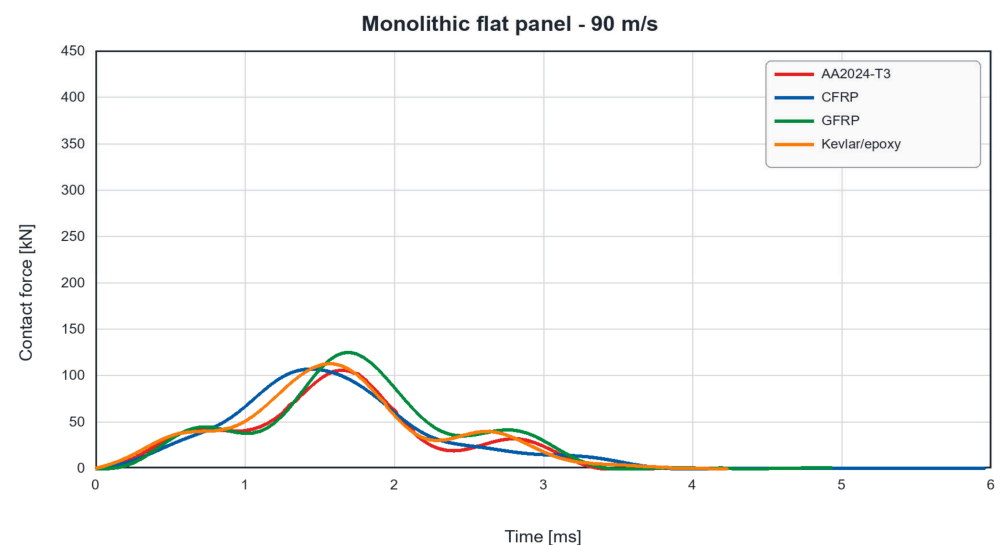


Figure 9. Contact-force histories for monolithic flat panels at 90 m/s. All force plots use a common material palette: AA2024-T3 red, CFRP blue, GFRP green and Kevlar/epoxy orange. Paired velocities use a common 0–6 ms time axis and 0–450 kN ordinate.

At 180 m/s the retained flat-panel curves have the same broad temporal sequence but higher peaks (see Figure 10): approximately 435 kN for Kevlar/epoxy, 430 kN for GFRP, 375 kN for CFRP and 350 kN for AA2024-T3. The nonlinear increase is conditional on the reported model definitions and is not used as evidence of convergence or failure accuracy.

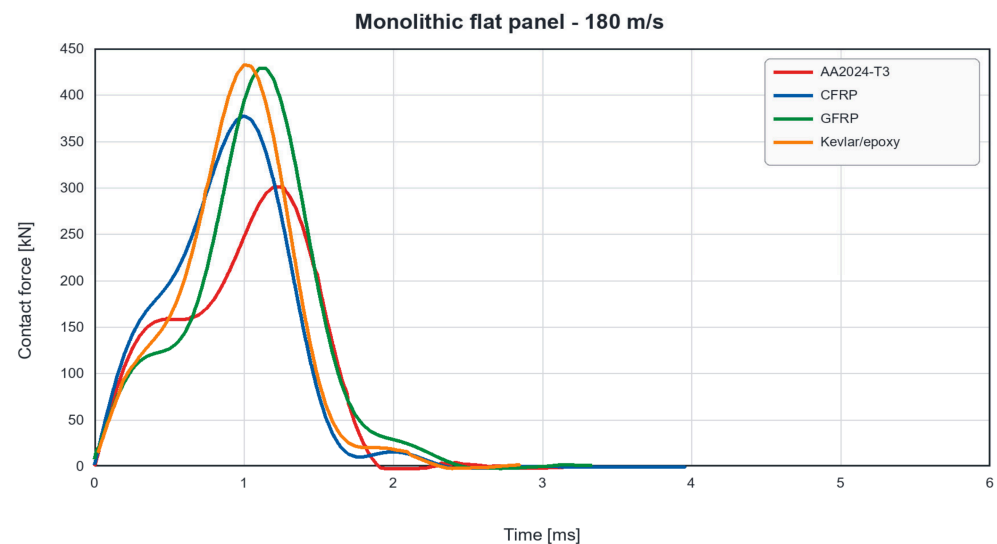


Figure 10. Contact-force histories for monolithic flat panels at 180 m/s. Material colours and axes are identical to Figure 9.

3.3. Sandwich Flat Panels

Two flat-panel honeycomb-sandwich configurations were reported: CFRP skins with Nomex and with AA2024-T3 aluminium honeycomb regions. The source gives a 10 mm region, 15 mm hexagonal cell size and nominal wall thicknesses of 0.063 and 0.035 mm. Retained images show cellular geometry, but exact shell/solid section assignment, material orientation, wall self-contact and element count cannot be verified without the model files. Perfect ties, if applied as described, exclude skin-core debonding.

The archived sandwich outputs differ from the monolithic CFRP output at both velocities. These differences are reported as conditional responses of the source-described honeycomb-sandwich models. They are not interpreted as validated crushing, densification, core damage, bond failure or post-crush load transfer because the required constitutive definitions and failure histories are unavailable.

The flat-panel production cases provide a common numerical comparison. The force ranking changes with velocity, and the more compliant configurations generally show longer responses. Because structural and SPH convergence and complete solver controls are not available, the ordering is descriptive and its quantitative reliability has not been established, including within the present matrix.

The sandwich curves show that the source-described honeycomb region changes the predicted contact response relative to monolithic CFRP. No physical mechanism is assigned beyond that observation because the exact core representation and damage behaviour cannot be independently verified.

4. Wing Leading-Edge Bird-Strike Simulation Results

This section reports the simplified NACA 23,015 leading-edge cases. Source-described monolithic skins and honeycomb-sandwich configurations used a nominal 2.5 mm structural mesh and the constraints in Section 2.8. Transverse displacement denotes the retained U3 history identified as the outer-skin point nearest the geometric impact centre; the exact node label/set cannot be re-audited without the ODB. It is a nodal history, not a full-field deformation maximum. Table 7 lists digitised peak magnitudes and times.

Table 7. Approximate peak absolute impact-centre U3 values digitised from the retained curves in Figures 11–14. Times and retained trace intervals are reported in milliseconds.

Configuration	Velocity (m/s)	Peak U3 (mm)	Time of Peak (ms)	Retained Trace Interval (ms)
AA2024-T3 monolithic	90	1.0	5.19	0–5.91
CFRP monolithic	90	29.5	3.79	0–5.91
GFRP monolithic	90	90.0	5.91	0–5.91
Kevlar/epoxy monolithic	90	45.1	5.91	0–5.91
AA2024-T3 monolithic	180	66.5	3.38	0–3.90
CFRP monolithic	180	59.6	3.90	0–3.90
GFRP monolithic	180	172.9	3.91	0–3.91
Kevlar/epoxy monolithic	180	88.0	3.91	0–3.91
CFRP/Al label	90	62.8	4.11	0–4.11
CFRP/Nomex label	90	81.7	5.91	0–5.91
GFRP/Al label	90	168.8	5.91	0–5.91
GFRP/Nomex label	90	204.3	5.91	0–5.91
Kevlar/Al label	90	96.0	5.91	0–5.91
Kevlar/Nomex label	90	108.4	5.91	0–5.91
CFRP/Al label	180	106.9	3.90	0–3.90
CFRP/Nomex label	180	105.5	3.91	0–3.91
GFRP/Al label	180	259.1	3.91	0–3.91
GFRP/Nomex label	180	281.6	3.91	0–3.91
Kevlar/Al label	180	138.9	3.91	0–3.91
Kevlar/Nomex label	180	141.0	3.91	0–3.91

Note: Values are raster-digitised from the retained histories, not recomputed from ODB data. U3 is the source-described impact-centre nodal trace rather than a global deformation maximum; the precise node/set label is unavailable. The replotted panels use a common 0–6 ms display axis, while this table reports the actual retained trace extent.

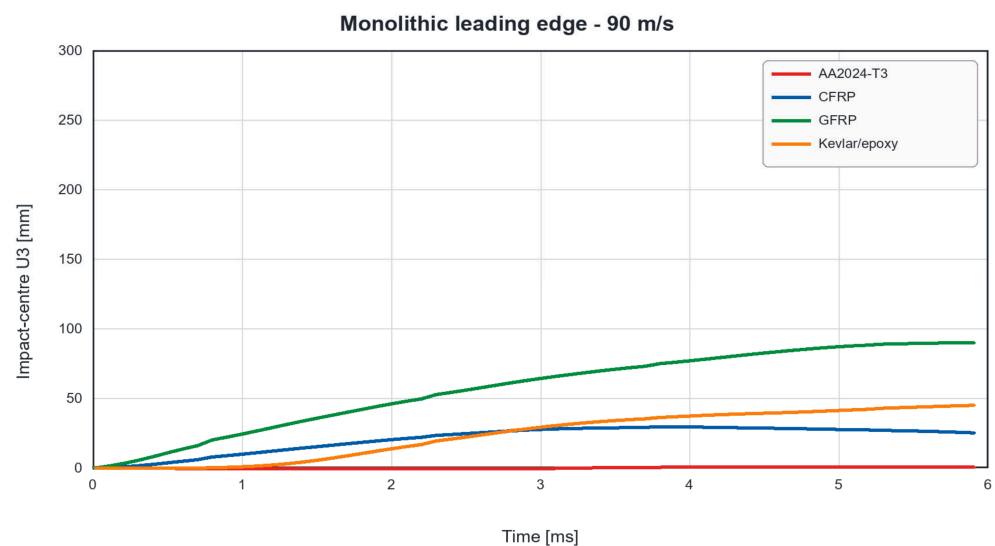


Figure 11. Retained impact-centre U3 histories for monolithic leading edges at 90 m/s, replotted in millimetres. Approximate peak |U3|/time pairs are AA2024-T3 1.0 mm/5.19 ms, CFRP 29.5 mm/3.79 ms, GFRP 90.0 mm/5.91 ms and Kevlar/epoxy 45.1 mm/5.91 ms. Values are digitised from the retained raster traces; see Table 7.

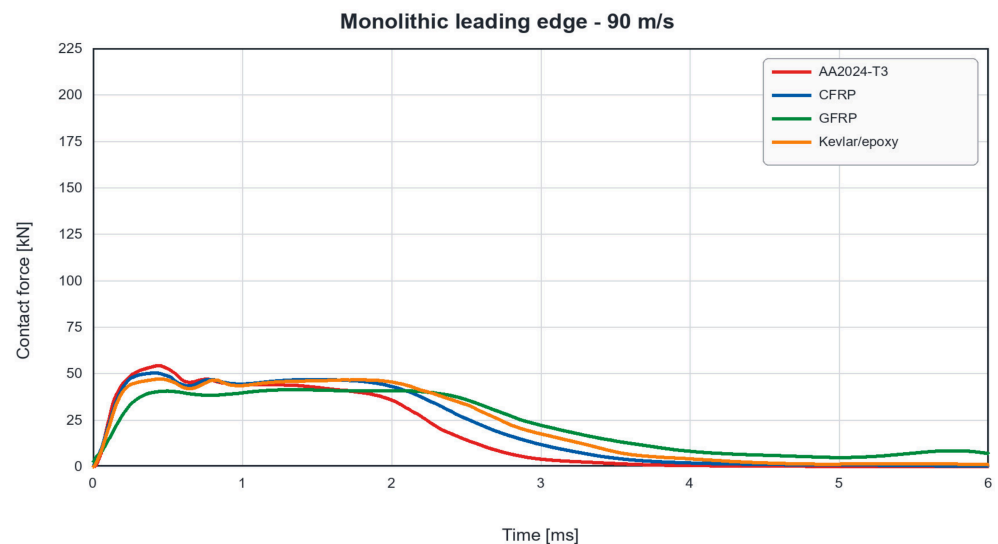


Figure 12. Contact-force histories for monolithic leading edges at 90 m/s. AA2024-T3 is red, CFRP blue, GFRP green and Kevlar/epoxy orange. Paired velocities use common 0–6 ms and 0–225 kN axes.

The leading-edge force histories show a multi-pulse sequence, but visual similarity to the flat-plate curve is not validation of curved geometry. No curved leading-edge experiment, pressure history, composite damage comparison or sandwich failure comparison was available. Only the single Liu et al. plot-to-plot force-history benchmark at 69 m/s is quantified.

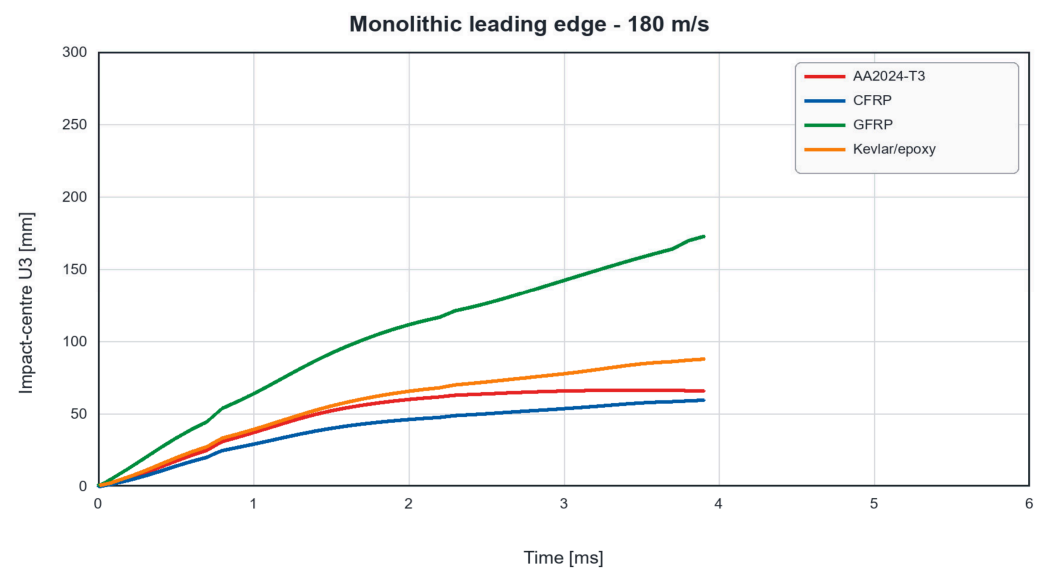


Figure 13. Retained impact-centre U3 histories for monolithic leading edges at 180 m/s, replotted in millimetres. Approximate peak $|U_3|$ /time pairs are AA2024-T3 66.5 mm/3.38 ms, CFRP 59.6 mm/3.90 ms, GFRP 172.9 mm/3.91 ms and Kevlar/epoxy 88.0 mm/3.91 ms. Axes match Figure 11; see Table 7.

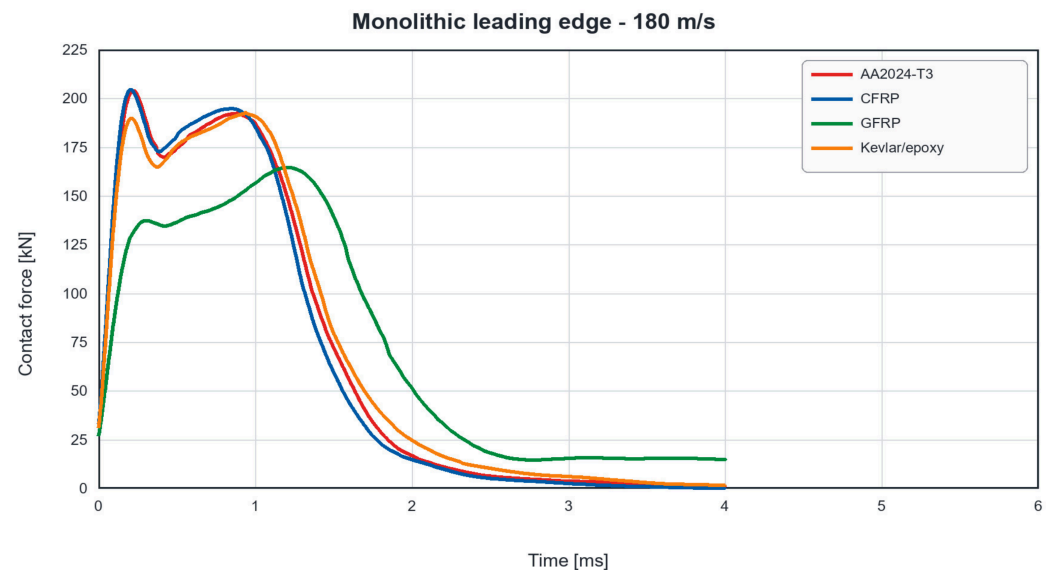


Figure 14. Contact-force histories for monolithic leading edges at 180 m/s. Material colours and axes are identical to Figure 12.

For CFRP, digitisation of the retained U3 histories gives peak absolute impact-centre values of approximately 29.5 mm at 3.79 ms for 90 m/s and 59.6 mm at 3.90 ms for 180 m/s (Figures 11 and 13; Table 7). The corresponding archived force peaks are approximately 50 and just over 200 kN (Figures 12 and 14). These displacements were under-reported by a factor of 1000 in the original prose because metre-valued ordinates were read as millimetres.

GFRP has the largest retained monolithic U3 values: approximately 90.0 mm at 5.91 ms for 90 m/s and 172.9 mm at 3.91 ms for 180 m/s. Its archived force peaks are approximately 41 and 165 kN, respectively. These observations show a larger local displacement and lower leading-edge force than CFRP for the stated cases; they do not establish a universal toughness or energy-absorption ranking.

Kevlar/epoxy gives retained peak absolute U3 values of approximately 45.1 mm at 5.91 ms and 88.0 mm at 3.91 ms, with archived force peaks of approximately 47 and 190 kN. AA2024-T3 gives approximately 1.0 mm at 5.19 ms and 66.5 mm at 3.38 ms in the corresponding U3 traces. No matrix, fibre, shear, metal rupture or element-deletion outcome is inferred because the required field histories and complete damage inputs were not retained.

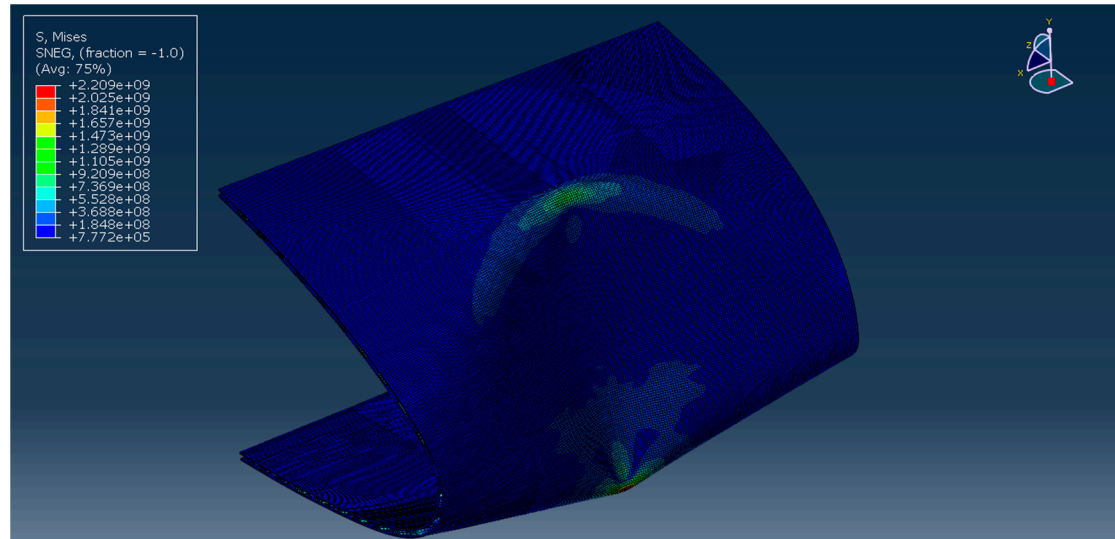
4.1. Sandwich Leading-Edge Structures

For the source-described Nomex honeycomb-sandwich cases at 90 m/s, digitised peak absolute U3 values are approximately 81.7 mm for CFRP, 108.4 mm for Kevlar/epoxy and 204.3 mm for GFRP; the retained force peaks are approximately 50, 46 and 32 kN. At 180 m/s, U3 values are approximately 105.5, 141.0 and 281.6 mm, while force peaks are approximately 189, 165 and 118 kN. Peak times and trace intervals are reported in Table 7.

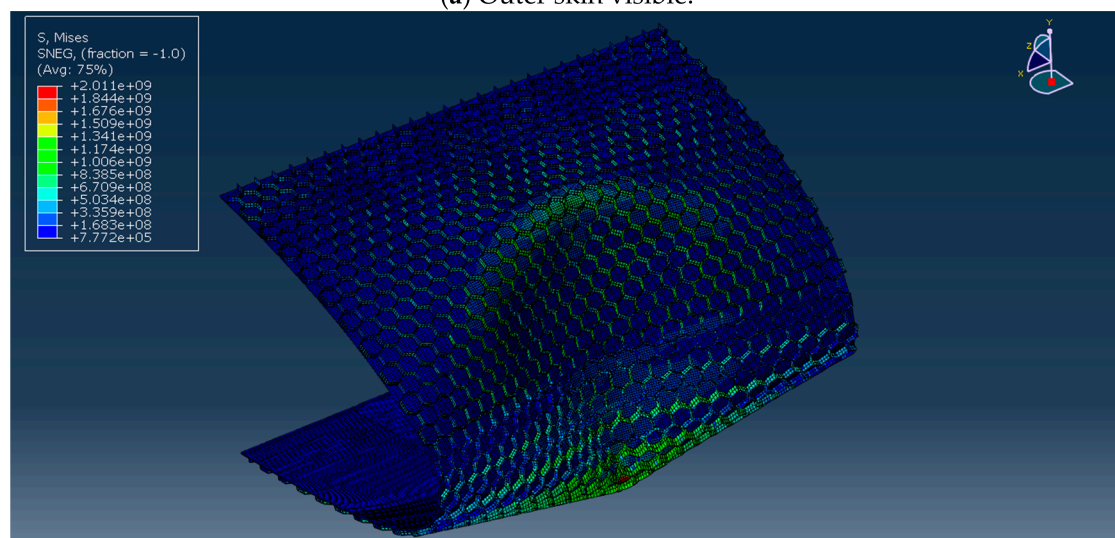
For the source-described aluminium honeycomb-sandwich cases at 90 m/s, digitised peak absolute U3 values are approximately 62.8 mm for CFRP, 96.0 mm for Kevlar/epoxy and 168.8 mm for GFRP; retained force peaks are approximately 49, 45 and 34 kN. At 180 m/s, U3 values are approximately 106.9, 138.9 and 259.1 mm, while force peaks are approximately 197, 171 and 125 kN. Small differences between paired core labels are not attributed to crushing or densification because the exact constitutive assignment is unverified.

Figures 15 and 16 are retained only as qualitative stress-location screenshots. They display S , Mises in pascals at the negative shell section point (SNEG, fraction -1.0) with

75% nodal averaging. The retained screenshots do not document a deformation display scale factor; displayed shapes must therefore not be read quantitatively. Von Mises stress is not a failure index for the orthotropic composite skin or Nomex representation, and no Hashin or core-damage contour was retained. Quantitative displacement is taken only from U3 histories (see Figures 17–20).

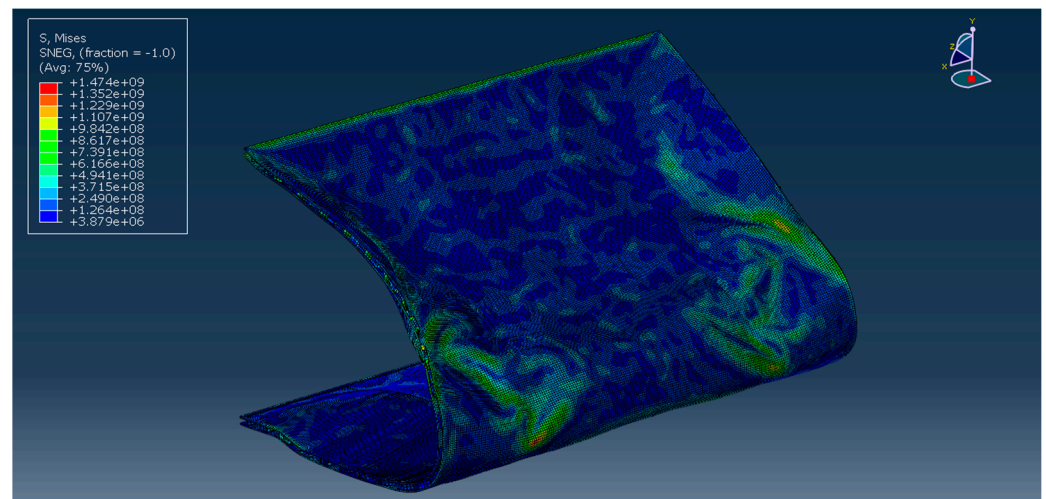


(a) Outer skin visible.

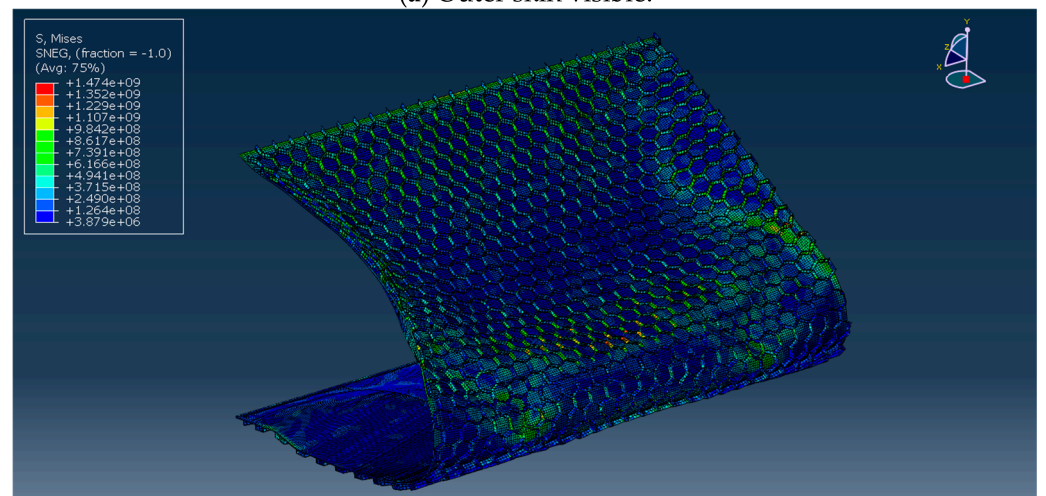


(b) Outer skin hidden to expose the honeycomb core.

Figure 15. GFRP/Nomex-labelled leading edge at 90 m/s: qualitative S, Mises screenshots in Pa at SNEG (fraction -1.0) with 75% nodal averaging. Displayed maxima are approximately 2.209 GPa (outer skin visible) and 2.011 GPa (outer skin hidden). The deformation display scale is not recorded. These contours are not composite or core failure indices.



(a) Outer skin visible.



(b) Outer skin hidden to expose the honeycomb core.

Figure 16. GFRP/Nomex-labelled leading edge at 180 m/s: qualitative S, Mises screenshot in Pa at SNEG (fraction -1.0) with 75% nodal averaging and displayed maximum approximately 1.474 GPa. The deformation display scale is not recorded. The image is not a displacement or damage contour.

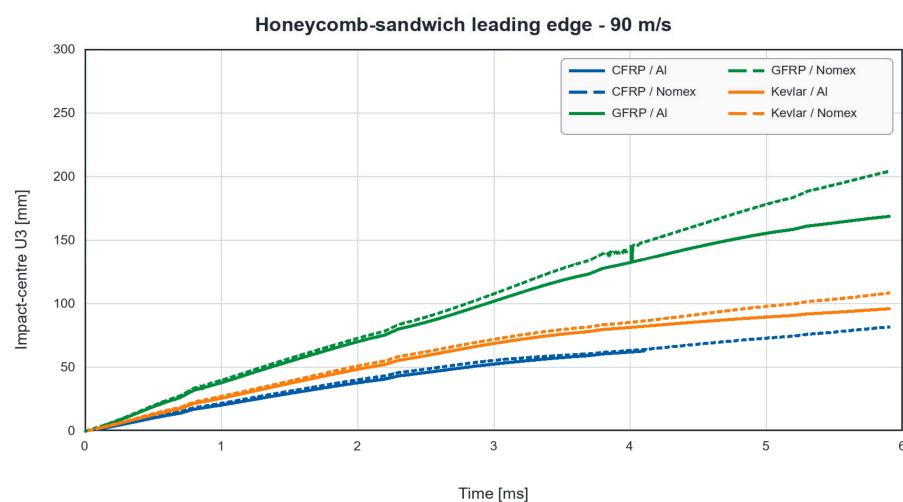


Figure 17. Retained impact-centre U3 histories for honeycomb-sandwich leading edges at 90 m/s, replotted in millimetres. Approximate peak $|U3|$ values are CFRP/Al 62.8, CFRP/Nomex 81.7, GFRP/Al 168.8, GFRP/Nomex 204.3, Kevlar/Al 96.0 and Kevlar/Nomex 108.4 mm. Skin colour is constant; solid/dashed lines denote Al/Nomex source labels. See Table 7 for times.

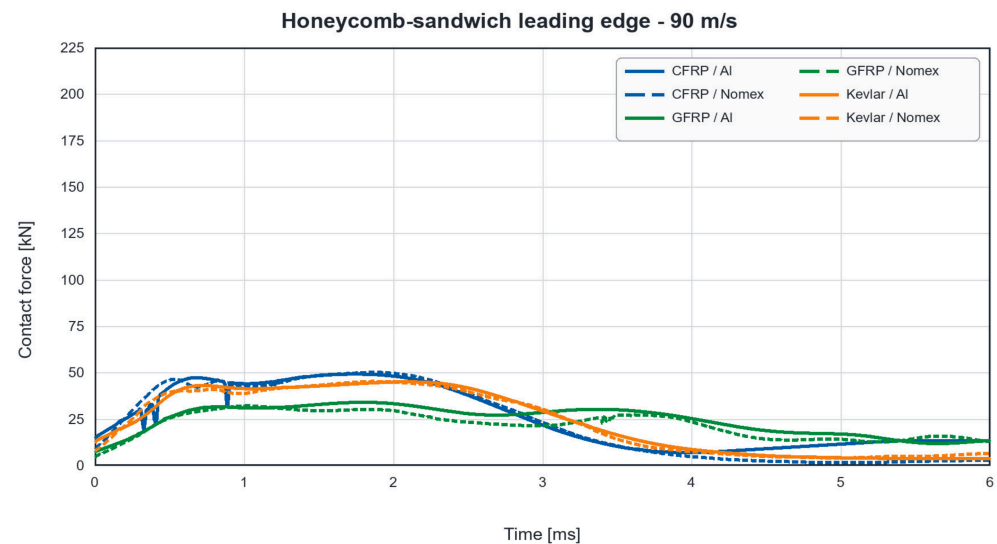


Figure 18. Contact-force histories for honeycomb-sandwich leading edges at 90 m/s. CFRP is blue, GFRP green and Kevlar/epoxy orange; solid lines denote the aluminium label and dashed lines the Nomex label. Paired velocities use common 0–6 ms and 0–225 kN axes.

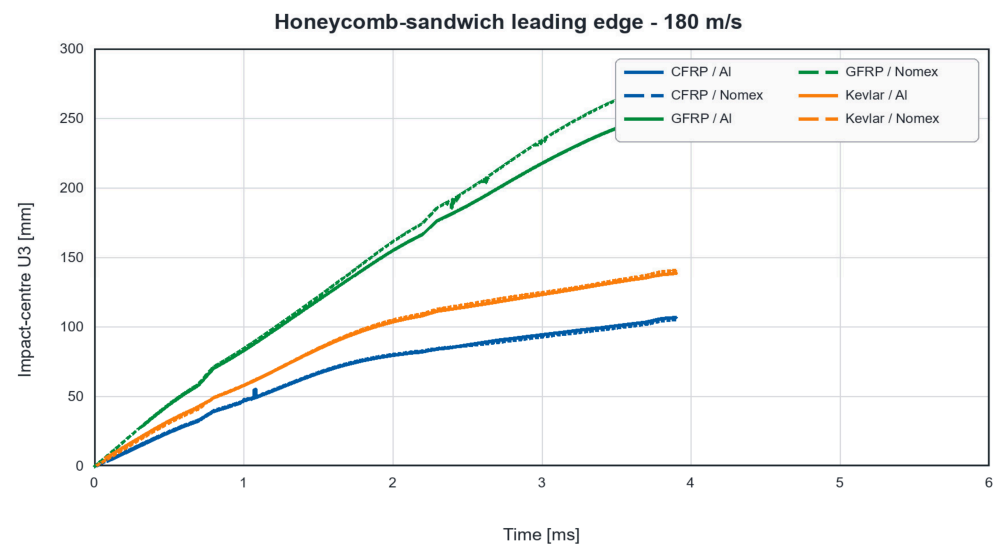


Figure 19. Retained impact-centre U3 histories for honeycomb-sandwich leading edges at 180 m/s, replotted in millimetres. Approximate peak $|U3|$ values are CFRP/Al 106.9, CFRP/Nomex 105.5, GFRP/Al 259.1, GFRP/Nomex 281.6, Kevlar/Al 138.9 and Kevlar/Nomex 141.0 mm. Axes and encoding match Figure 17; see Table 7 for times.

4.2. Source-Reported Maximum ALLIE per Reported Structural Mass

The submitted manuscript reports the maximum Abaqus ALLIE value divided by a reported structural mass. The retained archive does not establish the history-output region, whether the projectile was excluded, the extraction interval or the post-processing script. Equation (14) therefore defines only the label applied to the archived values, not a verified target-only energy measure:

$$e_{\text{reported}} = \frac{\max_{0 \leq t \leq T} [ALLIE_{\text{reported}}(t)]}{m_{\text{reported}}}, \quad (14)$$

where $ALLIE_{\text{reported}}(t)$ and m_{reported} denote the archived numerator and mass used in the submitted post-processing. Abaqus ALLIE can contain recoverable strain energy,

plastic and damage dissipation, artificial strain energy and other contributions. A maximum ALLIE/mass value is not equivalent to irreversible absorbed energy and cannot identify crushing or damage efficiency without a complete decomposition.

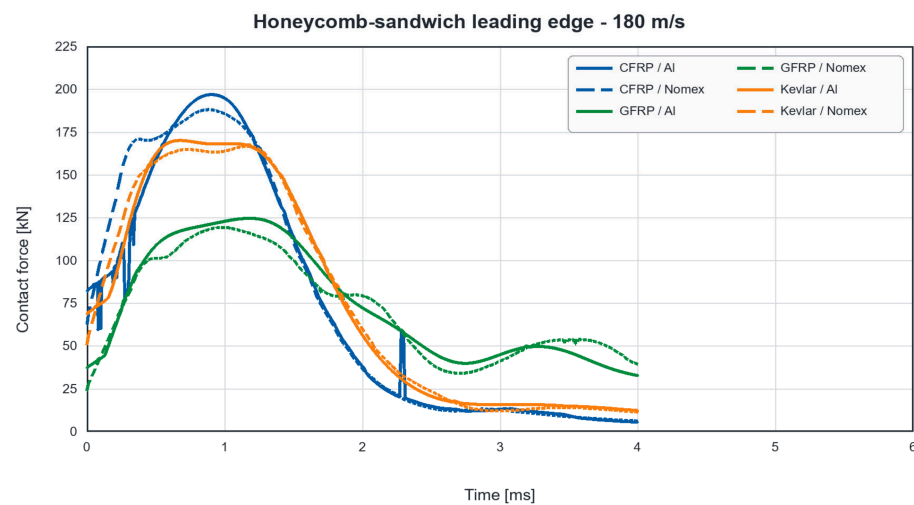


Figure 20. Contact-force histories for honeycomb-sandwich leading edges at 180 m/s. Colours, line styles and axes are identical to Figure 18.

A complete, consistent set of ETOTAL, ALLKE, ALLSE, ALLPD, ALLDMD, ALLAE and relevant contact/external-work histories was not retained for the production and coarse-mesh cases. At the global/set level, hourglass and drill contributions are assessed through ALLAE; element-level artificial strain energy can be inspected through ELASE. A separate whole-model hourglass variable is not used. A defensible energy-balance plot, total-energy drift check and artificial-energy ratio cannot be reconstructed, and no 5% or other criterion is claimed to have been satisfied (see Tables 8 and 9).

Table 8. Source-reported maximum ALLIE divided by reported structural mass for monolithic leading-edge skins (kJ/kg). The extraction definition and energy balance are unverified.

Material	Mass (kg)	90 m/s Source-Reported ALLIE/Mass (kJ/kg)	180 m/s Source-Reported ALLIE/Mass (kJ/kg)
AA2024-T3	12.56	0.07	0.57
GFRP	6.44	0.32	1.76
Kevlar	7.45	0.15	0.79
CFRP	7.23	0.09	0.57

Figures 21 and 22 reproduce the source-reported ALLIE/mass values solely for traceability. Because the extraction set and energy decomposition cannot be verified, the values are not used to rank materials, infer absorption or support a headline conclusion.

The leading-edge production curves show that force and impact-centre U3 orderings depend on geometry, velocity, skin system and the source-described sandwich configuration. CFRP gives the smallest composite U3 and GFRP the largest for the selected lay-ups, while force ranking changes between flat and curved targets. These are conditional numerical observations; they are not validated damage, energy-absorption or core-performance rankings.

Table 9. Source-reported maximum ALLIE divided by reported structural mass for honeycomb-sandwich leading-edge configurations (kJ/kg). The extraction definition and energy balance are unverified.

Skin	Core	Mass (kg)	90 m/s Source-Reported ALLIE/Mass	180 m/s Source-Reported ALLIE/Mass
CFRP	Aluminium	10.46	0.15	0.67
CFRP	Nomex	10.41	0.15	0.59
GFRP	Aluminium	9.06	0.39	1.94
GFRP	Nomex	9.01	0.33	1.33
Kevlar	Aluminium	8.78	0.35	1.60
Kevlar	Nomex	8.73	0.19	0.91

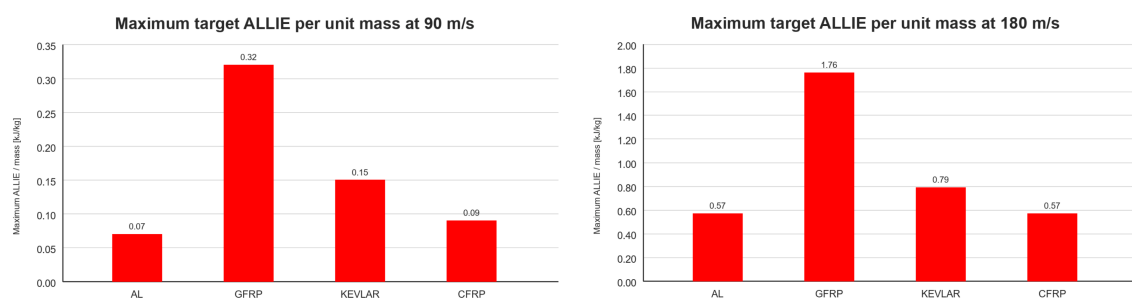


Figure 21. Source-reported maximum ALLIE divided by reported structural mass for monolithic leading-edge cases. Extraction set, time window and energy decomposition are unverified; values are not interpreted as absorbed energy.

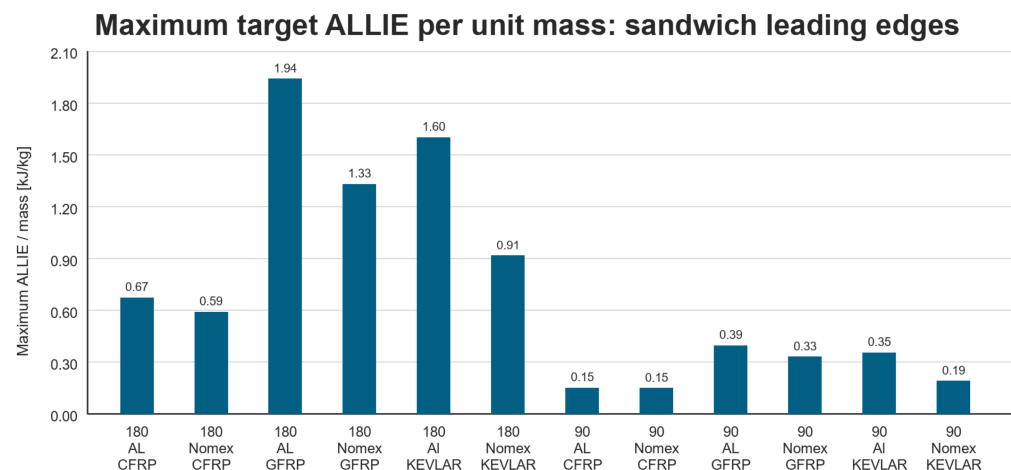


Figure 22. Source-reported maximum ALLIE divided by reported structural mass for honeycomb-sandwich leading-edge cases. Extraction set, time window and energy decomposition are unverified; values are retained for traceability only.

4.3. Further Remarks and Discussion

Across the flat-panel curves, an initial pulse, a broader main pulse and subsequent oscillation are visible. This sequence is qualitatively compatible with commonly described soft-body impact phases, but qualitative curve shape is not used as an independent validation metric.

Flat-plate benchmark. The apparent Liu et al. comparison is limited to one nominal 1.8 kg, 69 m/s flat-plate condition. Approximate plot-to-plot differences are -0.5% in peak-force magnitude, $+0.9\%$ in peak time, -6.0% in force-magnitude impulse and 21.5% in

waveform NRMSE. The sizeable waveform metric and uncertain force-variable equivalence preclude a general accuracy claim.

Monolithic composites. The retained curves demonstrate a metric- and geometry-dependent ordering. At 90 m/s, the GFRP peak force is approximately 17% above CFRP for the flat panel (125 versus 107 kN) but approximately 18% below CFRP for the leading edge (41 versus 50 kN). In the 90 m/s leading edge, GFRP U3 is approximately three times CFRP U3 (90.0 versus 29.5 mm). These comparisons are more informative than a single universal ranking, but their robustness to the unverified inputs has not been established.

Honeycomb-sandwich cases. The paired retained curves differ with the source core label, skin and velocity. Because the exact section/material assignment, crushing law, wall self-contact and interface failure treatment cannot be independently verified, the discussion is limited to the plotted force and U3 differences. Terms such as core integrity, densification, crushing efficiency and post-crush load transfer are not used as outcome claims.

The curved leading-edge geometry changes bending stiffness and load paths relative to a flat panel and is associated with a different force ordering. No curved-target experiment establishes the quantitative accuracy of these predictions.

Monolithic leading edges. CFRP has the smallest retained composite U3, GFRP the largest and Kevlar/epoxy an intermediate value for both velocities. This ordering applies only to the stated lay-ups, thicknesses, extraction point and constraints; no energy or damage superiority is inferred.

Honeycomb-sandwich leading edges. Pairwise differences are reported in Table 7 and Figures 17–20. The two source core labels cannot be interpreted as validated material-performance alternatives because the production core assignment and failure behaviour are not recoverable from the retained files.

Within the modelled matrix, the principal observable trade-off is between contact-force history and local impact-centre displacement. The archived ALLIE/mass values are not included in this design interpretation.

- **Force–displacement trade-off.** CFRP limits impact-centre U3 in the selected composite models but can retain a high short-duration force peak, whereas GFRP is more compliant and has larger U3. Stiffness is not isolated experimentally and must not be treated as the sole causal variable.
- **Source core label.** The retained honeycomb-sandwich curves vary with the Nomex/aluminium label, but the exact implementation is unverified; no crushing, densification, bond-failure or energy-absorption mechanism is assigned.
- **Velocity scaling.** Doubling velocity increases nominal projectile kinetic energy by a factor of four and raises the archived force and U3 responses nonlinearly. The response does not follow a single universal scale factor across materials and geometries.
- **Consistency with literature.** Multi-pulse force histories are qualitatively consistent with soft-body impact descriptions, but qualitative similarity does not substitute for pressure, displacement, damage, convergence and numerical-energy verification.

For preliminary screening, force and allowable local displacement should be considered together rather than reduced to one material ranking. Before quantitative design use, the model requires archived solver controls, structural and SPH convergence, complete energy histories, pressure validation, damage calibration and representative curved/sandwich experiments.

4.4. Scope and Limitations

The principal limitations are: one raster-digitised flat-plate force-history benchmark with uncertain force-variable equivalence; no direct pressure, present-model displacement or damage validation; different benchmark and production geometry, thickness, velocity

and constraints; no demonstrated structural, curvature or SPH convergence; unavailable solver-control metadata; incomplete energy histories; contradictory and unrecoverable honeycomb assignment; perfect tied interfaces if implemented as described; incomplete Johnson–Cook failure and composite damage-evolution inputs; and unavailable failure fields, deletion statistics and residual-capacity evidence. The observed ordering is descriptive; its quantitative reliability and robustness have not been established, including within the present matrix.

5. Conclusions

This study applies established SPH–finite element ingredients in a controlled 32-case production matrix. A separate Liu et al. flat-plate curve comparison at nominal 1.8 kg and 69 m/s gives apparent digitised differences of -0.5% in peak magnitude, $+0.9\%$ in peak time, -6.0% in force-magnitude impulse and 21.5% in waveform NRMSE. This is a one-condition plot-to-plot benchmark, not validation of pressure, curved geometry, composites or honeycomb-sandwich behaviour.

After correcting the displacement unit, the retained leading-edge U3 values are tens to hundreds of millimetres. At 90/180 m/s, CFRP reaches approximately 29.5/59.6 mm, GFRP 90.0/172.9 mm and Kevlar/epoxy 45.1/88.0 mm. The largest sandwich trace is approximately 281.6 mm. The 90 m/s force ordering reverses between GFRP and CFRP when the target changes from flat to curved, demonstrating that no universal material ranking follows from one response metric.

Geometry and boundary conditions materially affect the predicted response. The retained evidence does not establish rupture, penetration, containment, core crushing, debonding or residual strength. Certification-level protection and continued-safe-flight conclusions are therefore not made.

Overall, the work provides a screening-level comparative dataset and a bounded benchmark rather than a certification or fully validated model. Source-reported AL-LIE/mass values are retained only for traceability and are not used as absorbed-energy evidence or a material ranking.

Future work should include Wilbeck-type rigid-target pressure histories, multiple masses and velocities, measured plate displacement and damage extent, and representative curved and sandwich tests. Re-runs should archive the .inp, .cae, .sta, .msg and ODB files; stable-time-step and mass-scaling records; viscosity, hourglass/drill and SPH controls; ETOTAL, ALLKE, ALLIE, ALLSE, ALLPD, ALLDMD, ALLAE and relevant work/contact histories; ELASE where element-level artificial energy is assessed; and STATUS/STATUSMP, SDEG, Johnson–Cook/Hashin initiation and damage variables, deletion counts and residual-capacity results. Structural, curvature and SPH convergence, calibrated core crushing and interface failure should be demonstrated before quantitative ranking or design use.

Author Contributions: Conceptualization, M.L., A.N., D.B., F.M.E., K.D., H.G., P.C.M. and W.v.d.W.; methodology, M.L., A.N., D.B., F.M.E., K.D., H.G., P.C.M. and W.v.d.W.; software, M.L., A.N., D.B., F.M.E., K.D.; validation, M.L., A.N., D.B., F.M.E., K.D.; formal analysis, M.L., A.N., D.B., F.M.E. and K.D.; investigation, M.L., A.N., D.B., F.M.E. and K.D.; resources, H.G.; data curation, H.G.; writing—original draft preparation, M.L., A.N., D.B., F.M.E. and K.D.; writing—review and editing, M.L., A.N., D.B., F.M.E. and K.D.; visualization, M.L., A.N., D.B., F.M.E., K.D., H.G., P.C.M. and W.v.d.W.; supervision, H.G., P.C.M. and W.v.d.W.; project administration, H.G.; funding acquisition, H.G. All authors have read and agreed to the published version of the manuscript.

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Data Availability Statement: The retained and replotted figures underpinning the reported plot-based values are embedded in the revised manuscript. The original production Abaqus input decks, status/message files, output databases and post-processing scripts are not available in the retained archive; consequently, the unavailable controls, output sets and failure histories cannot be shared or independently re-audited. Future re-runs will archive these files.

Conflicts of Interest: Author Pablo Coladas Mato was employed by the company Civil Airframe Technology, Global Technology Centre, GKN Aerospace Services Ltd., author Wydo Vandewaerd was employed by the company Fokker Aerospace B.V. The remaining authors declare that the research was conducted in the absence of any commercial or financial relationships that could be construed as a potential conflict of interest.

References

1. European Aviation Safety Agency. CS-25.631: *Bird Strike Damage. Certification Specifications and Acceptable Means of Compliance for Large Aeroplanes (CS-25), Amendment 27*; Technical Report; European Aviation Safety Agency: Cologne, Germany, 2022.
2. United States Department of Transportation; Federal Aviation Administration. *Airworthiness Standards: Transport Category Airplanes. Title 14, Code of Federal Regulations, Part 25*; Technical Report; US Government Publishing Office: Washington, DC, USA, 2023.
3. Barber, J.P.; Taylor, H.R.; Wilbeck, J.S. *Bird Impact Forces and Pressures on Rigid and Compliant Targets*; Technical Report AFFDL-TR-77-60; Air Force Flight Dynamics Laboratory: Wright-Patterson Air Force Base, OH, USA; University of Dayton Research Institute: Dayton, OH, USA, 1978.
4. Wilbeck, J.S. *Impact Behavior of Low Strength Projectiles*; Technical Report AFML-TR-77-134; Air Force Materials Laboratory: Greene County, OH, USA, 1977.
5. Lavoie, M.A.; Gakwaya, A.; Ensan, M.N.; Zimcik, D.G.; Nandlall, D. Bird's substitute tests results and evaluation of available numerical methods. *Int. J. Impact Eng.* **2009**, *36*, 1276–1287. [\[CrossRef\]](#)
6. Vignjevic, R.; Orlowski, M.; De Vuyst, T.; Campbell, J.C. A parametric study of bird strike on engine blades. *Int. J. Impact Eng.* **2013**, *60*, 44–57. [\[CrossRef\]](#)
7. Heimbs, S. Computational methods for bird strike simulations: A review. *Comput. Struct.* **2011**, *89*, 2093–2112. [\[CrossRef\]](#)
8. European Aviation Safety Agency. CS-25.775: *Windshields and Windows. Certification Specifications and Acceptable Means of Compliance for Large Aeroplanes (CS-25), Amendment 27*; Technical Report; European Aviation Safety Agency: Cologne, Germany, 2022.
9. Gingold, R.A.; Monaghan, J.J. Smoothed particle hydrodynamics: Theory and application to non-spherical stars. *Mon. Not. R. Astron. Soc.* **1977**, *181*, 375–389. [\[CrossRef\]](#)
10. Smojver, I.; Ivančević, D. Bird strike damage analysis in aircraft structures using Abaqus/Explicit and coupled Eulerian Lagrangian approach. *Compos. Sci. Technol.* **2011**, *71*, 489–498. [\[CrossRef\]](#)
11. Smojver, I.; Ivančević, D. Advanced modelling of bird strike on high lift devices using hybrid Eulerian–Lagrangian formulation. *Aerosp. Sci. Technol.* **2012**, *23*, 224–232. [\[CrossRef\]](#)
12. Johnson, A.F.; Holzapfel, M. Modelling soft body impact on composite structures. *Compos. Struct.* **2003**, *61*, 103–113. [\[CrossRef\]](#)
13. Johnson, G.R.; Cook, W.H. Fracture characteristics of three metals subjected to various strains, strain rates, temperatures and pressures. *Eng. Fract. Mech.* **1985**, *21*, 31–48. [\[CrossRef\]](#)
14. Hashin, Z. Failure criteria for unidirectional fiber composites. *J. Appl. Mech.* **1980**, *47*, 329–334. [\[CrossRef\]](#)
15. Long, S.; Mu, X.; Liu, Y.; Wang, H.; Zhang, X.; Yao, X. Failure modeling of composite wing leading edge under bird strike. *Compos. Struct.* **2021**, *255*, 113005. [\[CrossRef\]](#)
16. Zhou, Y.; Sun, Y.; Huang, T. Bird-strike resistance of composite laminates with different materials. *Materials* **2019**, *13*, 129. [\[CrossRef\]](#)
17. Guida, M.; Marulo, F.; Meo, M.; Riccio, M. Analysis of bird impact on a composite tailplane leading edge. *Appl. Compos. Mater.* **2008**, *15*, 241–257. [\[CrossRef\]](#)
18. Xie, S.; Jing, K.; Zhou, H.; Liu, X. Mechanical properties of Nomex honeycomb sandwich panels under dynamic impact. *Compos. Struct.* **2020**, *235*, 111814. [\[CrossRef\]](#)
19. Fan, J.; Li, P.; Guo, W.; Zhao, X.; Su, C.; Xu, X. Experimental investigation on the low-velocity impact response of tandem Nomex honeycomb sandwich panels. *Polymers* **2023**, *15*, 456. [\[CrossRef\]](#)
20. Arachchige, B.; Ghasemnejad, H.; Yasaee, M. Effect of bird-strike on sandwich composite aircraft wing leading edge. *Adv. Eng. Softw.* **2020**, *148*, 102839. [\[CrossRef\]](#)

21. Liu, J.; Li, Y.; Gao, X. Bird strike on a flat plate: Experiments and numerical simulations. *Int. J. Impact Eng.* **2014**, *70*, 21–37. [[CrossRef](#)]
22. Monaghan, J.J. Why particle methods work. *SIAM J. Sci. Stat. Comput.* **1982**, *3*, 422–433. [[CrossRef](#)]

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