

Numerical investigation of kinetic-energy reduction and tip equivalent (von Mises) stress of an RPG surrogate inside a 3D-printed UAS fuselage under high-speed impact

Wyrlo Ricablanca^{1*}, Paula Alexandra Padro², Ben James Cuajotor³, Michael Ray Maligro⁴, Hannah Marie Tulang⁵, Alan Pierre-Miguel Gultia⁶, Michael Caleb Obalan⁷, Noel Hernandez⁸, Jhone Mychale Mariquit⁹, Cherry Mae Villame¹⁰, Jonathan Maglasang¹¹, Sherwin Guirnaldo¹²

^{1,2,3,4,5,6,7,8,9,12} Mechanical Engineering and Technology, Mindanao State University-Iligan Institute of Technology (MSU-IIT), Philippines

¹ Mechanical Engineering, Mindanao State University (MSU)- Marawi City (Main Campus), Philippines

¹⁰ Computer Engineering and Mechatronics, Mindanao State University-Iligan Institute of Technology (MSU-IIT), Philippines

¹¹ Mechanical and Aerospace Engineering, Cebu Technological University (CTU), Philippines

*Corresponding author E-mail: wyrlo.ricablanca@g.msuiit.edu.ph

Received May. 9, 2026
Revised Sep. 27, 2026
Accepted Sep. 30, 2026
Online Oct. 22, 2026

Abstract

Additive manufacturing offers a practical approach for producing low-cost UAS structures; however, the impact response of additively manufactured fuselage sections carrying internal payloads remains insufficiently established. This study numerically investigates the high-speed impact response of a 3D-printed UAS forward fuselage carrying an inert RPG surrogate. An explicit-dynamics model was developed in ANSYS Explicit Dynamics and evaluated against published battery-to-plate impact data. Additional numerical verification included a PLA failure-strain sensitivity study and local surrogate-tip mesh refinement, while a 3.0 m drop-test simulation provided an approximately 400 MPa mechanical reference stress. Nine UAS impact cases were then simulated using velocities of 15, 20, and 25 m/s and approach angles of 0°, 22.5°, and 45°. Impact severity was characterized using the kinetic-energy reduction of the surrogate assembly, $\Delta KE = KE(t_0) - KE(t)$, and equivalent (von Mises) stress at the surrogate tip. Increasing impact velocity generally increased the maximum observed kinetic-energy reduction, while greater impact obliquity reduced its magnitude and delayed its development. The highest observed ΔKE was 804.74 J at 25 m/s and 0°, while the lowest was 104.83 J at 15 m/s and 45°. Tip stress behavior exhibited a less uniform trend, with five of the nine cases reaching or exceeding the 400 MPa reference and the highest value, 564 MPa, occurring at 25 m/s and 45°. In eight cases, the maximum tip stress occurred before the maximum observed kinetic-energy reduction. Results indicate that although the global kinetic-energy reduction of the RPG surrogate assembly and the maximum local tip stress are influenced differently by impact velocity, approach angle, and fuselage-collapse behavior, they still provide complementary measures of impact severity.

1. Introduction

Loitering munitions are expendable Unmanned Aerial Systems (UAS) designed to hover over a designated area, identify targets, and deliver an onboard payload upon impact. One example is the Switchblade 300, shown in Figure 1.1 [1]. By combining the surveillance function of conventional UAS with the terminal capability of guided munitions, these systems have become increasingly significant in modern tactical military operations [2], [3]. However, advanced military UAS can involve substantial technical and infrastructural requirements [2], while complex UAS structures may also present challenges for conventional manufacturing [4].



Figure 1.1 Switchblade 300 (public-domain U.S. Army image) [1]

Additive manufacturing offers a practical alternative approach for producing lightweight UAS structures with complex geometries and reduced manufacturing constraints [4]. One example is the fixed-wing UAS shown in Figure 1.2 [5], which was developed with emphasis on ease of manufacturability and operational reliability. In this design, the fuselage is fabricated through additive manufacturing, while the aerodynamic surfaces are made from lightweight foam-board and cardboard structures. This configuration reduces fabrication time and material cost during production. The platform was originally developed for search-and-rescue applications. In the present study, however, it is adapted as a research configuration carrying an RPG surrogate to evaluate the impact severity transmitted to an internal payload.



Figure 1.2 Isometric view of cost-effective fixed-wing UAS [5]

However, placing a PG-7-type payload inside the UAS fuselage (Figure 1.3) may introduce several operational

challenges since conventional PG-7 rounds achieve their terminal conditions through rocket-assisted propulsion and typically reach high flight velocities of approximately 200–250 m/s before target impact [6]. In contrast, the UAS-based delivery system depends on aerodynamic flight and gravitational acceleration, which may produce lower impact velocities and different arrest behavior during collision. These differences require an assessment of how the current UAS configuration transfers impact energy to the internal payload region during high-speed impact. Therefore, the main objective of this study is to determine how variations in impact velocity and approach angle affect the kinetic-energy reduction and tip stress response of the RPG surrogate during high-speed impact. Particular attention is given to the magnitude and temporal development of these two response measures under the investigated collision conditions.

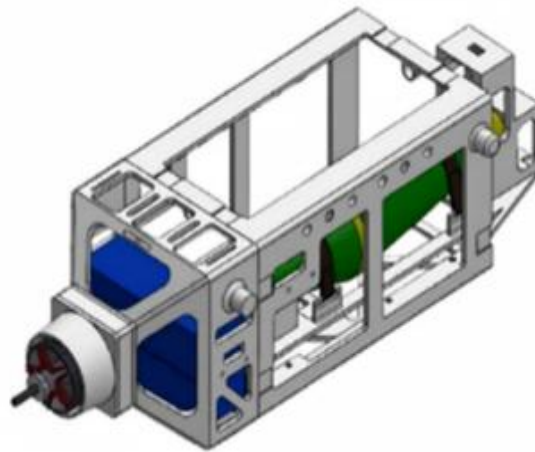


Figure 1.3 UAS fuselage section

Similar impact-related concerns have been studied in aerially delivered robotic systems. In Ref. [7], drop-induced impact loading in a drone-transported flippable ground robot is evaluated using theoretical and finite-element analysis, identifying critical stress concentrations and assessing structural reliability under repeated impact conditions. In the present study, the forward fuselage–surrogate assembly is analyzed in ANSYS Explicit Dynamics under nine controlled impact scenarios. These scenarios combine three impact velocities, 15, 20, and 25 m/s, equivalent to 54, 72, and 90 km/h, with three approach angles of 0° , 22.5° , and 45° . For each case, impact severity is quantified using the kinetic-energy reduction history and the stress response of the RPG surrogate.

2. Simulation setup

The study evaluates the performance of the UAS fuselage design during high-speed collision over a range of descent velocities. Crash dynamics simulations were conducted using ANSYS Explicit Dynamics to quantify the energy response of the RPG surrogate by tracking its kinetic-energy reduction and to characterize the local loading response using the equivalent (von Mises) stress at the RPG surrogate tip. Flanagan–Belytschko hourglass control was used with an analysis-level stiffness coefficient of 0.075, zero viscous coefficient, and zero static damping. The linear and quadratic artificial-viscosity coefficients were 0.2 and 1.0, respectively, with linear viscosity in expansion enabled. For the non-battery solid bodies, Average Nodal Pressure tetrahedral integration and Exact hexahedral integration were used. The battery used a dedicated solid-body control with the same tetrahedral and hexahedral integration formulations, a stiffness coefficient of 0.001, and a viscous coefficient of 0.1. Geometric-strain and minimum-time-step erosion were disabled, while element removal followed the material-failure criteria with eroded-material inertia retained. The interfaces among the retained internal UAS components and their supporting structures were defined using bonded contact. A 22 ms analysis window was used as a common observation interval for capturing the principal phase of the impact event and corresponding transient structural response.

2.1. UAS model and mass properties

The overall simulation setup consists of a target plate and the UAS fuselage section (Figure 2.1). The fuselage section which houses the internal components, as seen in Figure 2.1, is modelled as a 3D-printed PLA in line with collision-response studies that retain only the mass-dominant subsystems to capture impact loads [8] – [10]. Lightweight elements such as the foam-board skin, wings, and tail are omitted while battery, motor, and payload are retained. To preserve the overall mass (6.78 kg) and the original center of gravity, the masses of the excluded parts are lumped into the three dominant items: batteries 2.87 kg, RPG surrogate with rack 2.72 kg, and motor with mount 0.39 kg. The remaining 0.80 kg is carried by the structural nose mount, front frame, and middle frame, which are still explicitly modelled as they govern load transfer during impact. This mass-conserving simplification removes minor avionics and aerodynamic surfaces from the finite-element model while preserving the global inertial characteristics required for an accurate assessment of impact behavior.

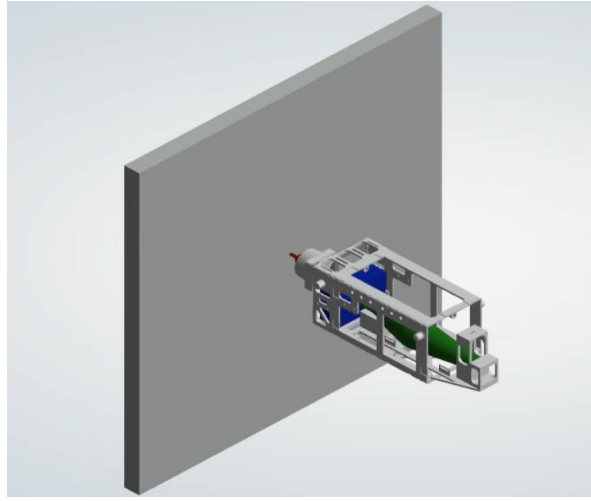


Figure 2.1 Fuselage and target plate view

2.2 Material selection

2.2.1 Johnson-Cook constitutive model

The Johnson–Cook (J-C) model provides a single constitutive law for isotropic metals experiencing large strains, high strain rates, and thermal softening. Material response is linear elastic until the equivalent (von Mises) stress reaches the yield stress; beyond that point plastic flow follows the J-C rule. This combination of strain hardening, rate sensitivity, and temperature effects makes the model well suited to high-velocity impact and crash simulations. The flow stress σ_y as shown in (1) depends on plastic strain, strain-rate, and temperature.

$$\sigma_y = \left[A + B(\epsilon_p)^n \right] [1 + C \ln(\dot{\epsilon}^*)] [1 - (T^*)^m] \quad (1)$$

where A is the initial yield stress, B is the hardening constant, n is the hardening exponent, C is the strain rate constant, m is the thermal softening exponent, ϵ_p is the equivalent plastic strain, $\dot{\epsilon}^*$ is the dimensionless plastic strain rate, and T^* is the homologous temperature [11],[12].

The Johnson–Cook failure model extends the standard J-C plasticity law by adding a ductile-fracture criterion. The critical fracture strain ϵ_f in (2) is written as a separable function of stress-triaxiality (pressure), dimensionless strain rate, and homologous temperature, allowing each factor to degrade fracture resistance independently. This formulation effectively captures progressive damage in metals subjected to high pressure, high strain rates, and thermal loading and it is also widely used in simulations of ballistic penetration, high-velocity impact, and metal-forming processes.

$$\epsilon_f = [D_1 + D_2 \exp(D_3 \sigma^*)] [1 + D_4 \ln(\dot{\epsilon}^*)] [1 + D_5 T^*] \quad (2)$$

$$D = \sum \frac{\Delta \bar{\epsilon}^p}{\epsilon_f} \quad (3)$$

In the Johnson–Cook model, damage is represented by a scalar parameter, D , which accumulates as the incremental plastic strain increases relative to the current fracture strain. The material remains intact while $D < 1$. When $D = 1$, failure is initiated. When erosion based on material failure is enabled, elements are removed once the defined failure criterion is reached. Because the fracture strain varies with stress state, strain rate, and temperature, the model can represent progressive damage under high-velocity impact conditions [16].

2.2.2 Target plate

A square AA5083-H116 plate (890 mm × 890 mm) was used as the impact target. Following the approach in [17], a 50 mm plate thickness was adopted so that the target behaved as quasi-rigid within the investigated velocity range, concentrating the deformation and damage response primarily in the UAS fuselage section and allowing clearer evaluation of energy transfer and structural deformation during impact. The target was modeled using the Johnson–Cook strength and failure parameters reported by Chi et al. [18], while the melting temperature of 893 K was adopted from Rashed et al. [19], as summarized in Table 2.1.

Table 2.1. Material properties for AA5083-H116 [18], [19]

Isotropic elasticity		AA5083-H116 Johnson Cook strength		Johnson Cook failure	
Young's modulus (MPa)	70,000	Initial yield stress, A (MPa)	167	Damage constant, $D1$	0.0261
Poisson's ratio	0.3	Hardening constant, B (MPa)	596	Damage constant, $D2$	0.263
Bulk modulus (MPa)	58,300	Hardening exponent, n	0.551	Damage constant, $D3$	-0.349
Shear modulus (MPa)	26,900	Strain rate constant, C	0.001	Damage constant, $D4$	0.247
		Thermal softening exponent, m	0.859	Damage constant, $D5$	16.8
		Melting temperature (K)	893		

2.2.3 RPG surrogate

The RPG surrogate, as shown in Figure 2.2, was modeled as a solid homogeneous Al 2024-T3, with its geometry based on the RPG configuration reported in [20]. The Johnson–Cook constitutive and failure parameters were adopted from [12], with the strength and failure parameter sets further supported by [13] and [14], respectively. These parameters are listed in Table 2.2. Because limited information is available regarding the internal geometry and material distribution of the RPG surrogate, a bulk representation was adopted. The material density of the surrogate body was adjusted to 1944.3 kg/m³ so that the numerical model reproduced the prescribed payload mass of 2.00 kg. The rack and support structure were likewise mass-adjusted using a density of 2645 kg/m³, giving a total surrogate-assembly mass of 2.72 kg. This mass-preserving simplification is consistent with the modelling approaches adopted in Refs. [11] and [17].

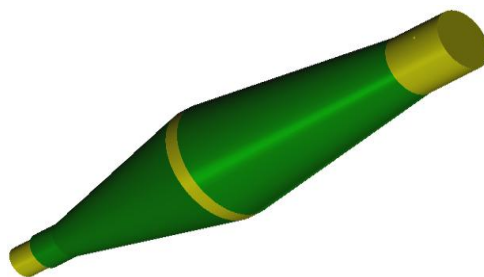


Figure 2.2 RPG surrogate isometric view

Table 2.2. Material properties for Al 2024-T3 [12]-[14]

Al 2024-T3					
Isotropic elasticity		Johnson Cook strength		Johnson Cook failure	
Young's modulus (MPa)	73,000	Initial yield stress, A (MPa)	369	Damage constant, $D1$	0.112
Poisson's ratio	0.33	Hardening constant, B (MPa)	684	Damage constant, $D2$	0.123
Bulk modulus (MPa)	71,600	Hardening exponent, n	0.73	Damage constant, $D3$	-1.5
Shear modulus (MPa)	27,400	Strain rate constant, C	0.0083	Damage constant, $D4$	0.007
		Thermal softening exponent, m	1.7	Damage constant, $D5$	0
		Melting temperature (K)	911.15		

2.2.4 Motor

The motor casing is made of Al 6061-T6 and is modeled using the Johnson–Cook strength and failure models in [21], as listed in Table 2.3.

Table 2.3. Material properties for Al 6061-T6 [21]

Al 6061-T6					
Isotropic elasticity		Johnson Cook strength		Johnson Cook failure	
Young's modulus (MPa)	69,000	Initial yield Stress, A (MPa)	324.1	Damage constant, $D1$	-0.77
Poisson's ratio	0.33	Hardening constant, B (MPa)	113.8	Damage constant, $D2$	1.45
		Hardening exponent, n	0.42	Damage constant, $D3$	-0.47
		Strain rate constant, C	0.002	Damage constant, $D4$	0
		Thermal softening exponent, m	1.34	Damage constant, $D5$	1.6
		Melting temperature (K)	925.15		

2.2.5 Batteries

The 4-cell, 4800 mAh lithium-ion-polymer (LiPo) pack was treated as a porous solid using the crushable-foam model in [22]. Material properties are listed in Table 2.4. Compressive stress–volumetric-strain response of the LiPo battery is plotted in Figure 2.3.

Table 2.4. Material properties for the batteries [22]

4-cell LiPo battery	
Density (kg/m^3)	1551.1
Stress-strain curve (MPa)	$\sigma = 276\varepsilon^{1.8}$
Young's modulus (MPa)	500
Poisson's ratio	0.01
Bulk modulus (MPa)	170
Shear modulus (MPa)	247.52
Maximum strain	0.16
Crushable foam maximum tensile stress (MPa)	55.58

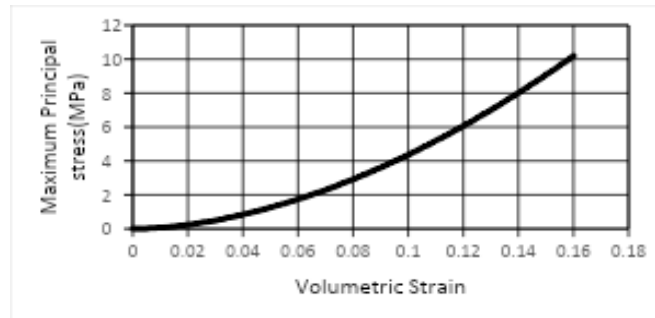


Figure 2.3 Compressive stress–volumetric-strain response of the LiPo battery

2.2.6 Body frames

The internal body frames of the UAS are likewise modelled using 3D-printed polylactic acid (PLA) as in [12],[15]. Material properties are listed in Table 2.5. The material behavior is defined using basic tensile properties including the tensile yield strength and ultimate tensile strength. Failure is modeled using a principal strain failure criterion, which captures the brittle fracture behavior typically observed in low-infill PLA structures.

Table 2.5. Material properties for the Body frames [12], [15]

Polylactic acid (PLA)	
Young's modulus (MPa)	1605
Poisson's ratio	0.3
Bulk modulus (MPa)	1,340
Shear modulus (MPa)	617
Tensile yield strength (MPa)	15.8
Tensile ultimate strength (MPa)	31.8
Maximum principal strain	0.03
Maximum shear strain	1.00E+20

2.3 Model evaluation and sensitivity studies

2.3.1 Battery–plate validation

Battery–plate validation was first performed to establish baseline global energy-transfer fidelity of the explicit-dynamics setup [11] reproduced the battery-to-plate tests reported in [17] using NASTRAN Solution 700 and compared the resulting peak impact force and maximum plate deflection with the experimental data. The present work adopted the same benchmark and conducted the simulations in ANSYS Explicit Dynamics. Accordingly, the subsequent integrated UAS–payload cases are treated as screening-level predictions focused on global energy-transfer trends, while other components are considered secondary and configuration-dependent.

In [17], a 4-cell, 4 800 mAh LiPo battery ($128 \times 48 \times 36$ mm, 340 g) strikes 890×890 mm Al 2024-T3 plates of 1.60 mm, 3.18 mm, and 6.35 mm thickness (geometry in Figure 2.4) at nominal velocities of 100 kn (51.4 m/s) and 250 kn (128.6 m/s). To verify the numerical model, the test cases in [17] exhibiting the smallest deviations from the target conditions (tests 5670, 5675, and 5661) were replicated. The battery was assigned the same isotropic-elastic and crushable-foam properties used in Figure 2.3 and Table 2.4.

Following the approach in [11], fixed boundary conditions were applied to all plate edges to simplify the numerical model, rather than modelling the full experimental support frame used in [17].

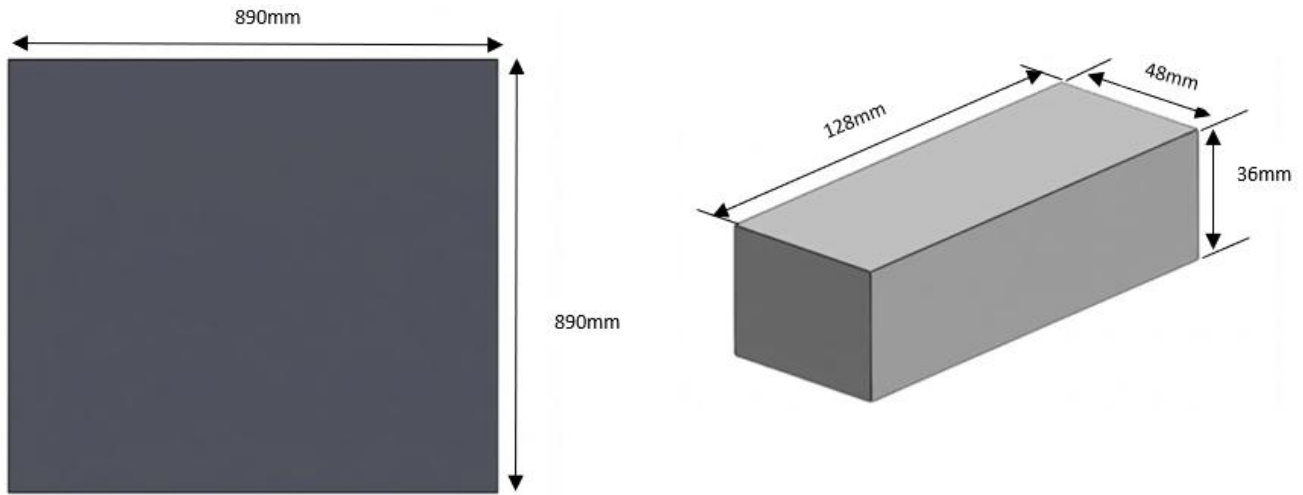


Figure 2.4. Dimensions of the battery projectile and Al 2024-T3 target panels

2.3.1.1 Contact settings

The LiPo battery was defined as the contact body and the Al 2024-T3 plate as the target body. Trajectory-based contact detection with a penalty formulation was employed to enforce separation. The resulting local penalty forces corrected node-to-surface penetration, while equal and opposite reaction forces ensured conservation of linear and angular momentum [16].

2.3.1.2 Battery and target plate meshing

A mesh convergence study was conducted to identify the coarsest mesh capable of preserving accurate force and deflection responses for both the battery and target plate.

2.3.1.3 Analysis settings

The Explicit Dynamics solver was configured with a 22 ms analysis window to encompass the complete contact event and its structural aftermath. The global energy error tolerance was limited to 0.005, while the default timestep safety factor (0.9) preserved numerical stability [16].

2.3.1.4 Data analysis

For each scenario, the target plate peak reaction forces and maximum central deformations were recorded and compared with the experimental results reported in [17].

2.3.2 PLA failure-strain sensitivity study

A numerical sensitivity study was performed on the frontal PLA fuselage section to evaluate the influence of the maximum principal failure strain on the structural response. The frontal portion of the UAS frame (Figure 2.5) was subjected to an impact velocity of 15 m/s at an impact angle of 22.5°.

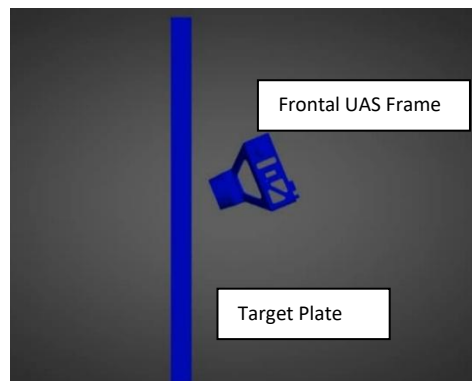


Figure 2.5 Frontal view of the frontal UAS impact setup

Three maximum principal failure-strain values, 0.02, 0.03, and 0.05, were investigated. All other material properties, geometry, and mesh settings were maintained among the three cases.

2.3.2.1 Analysis settings

The three impact cases were solved using ANSYS Explicit Dynamics under identical initial and boundary conditions. A 10 ms analysis window was used to capture the initial impact, development of the maximum structural stress, and subsequent kinetic-energy response of the frontal PLA section.

2.3.2.2 Data analysis

For each failure-strain case, the maximum equivalent (von Mises) stress and kinetic-energy of the frontal PLA section were extracted. The peak maximum equivalent (von Mises) stress was used to characterize the local structural response, while the kinetic-energy history was examined to determine how variation in the maximum principal failure strain affected the magnitude and timing of the frontal-section response during impact.

2.4 Kinetic-energy reduction calculation of the RPG surrogate assembly

The kinetic-energy reduction, $\Delta KE(t)$, was used as the primary global metric for the impact severity experienced by the RPG surrogate assembly: (4)

$$\Delta KE(t) = KE(t_0) - KE(t)$$

where $KE(t)$ is the kinetic-energy of the RPG surrogate assembly at time t , while $KE(t_0)$ is its initial kinetic-energy at $t = 0$. The resulting $\Delta KE(t)$ histories were compared among the different impact cases to analyze the maximum observed kinetic-energy reduction, its corresponding time, and the temporal pattern of the kinetic-energy response. The kinetic-energy response was extracted at a temporal sampling interval of approximately 0.22 ms throughout the 22 ms observation window.

2.5 Drop-test simulation for reference equivalent (von Mises) stress determination

To quantify the reference tip stress of the RPG surrogate, a drop-test simulation (Figure 2.6) was conducted in ANSYS Explicit Dynamics to establish a reference loading condition for comparison with the UAS impact cases.

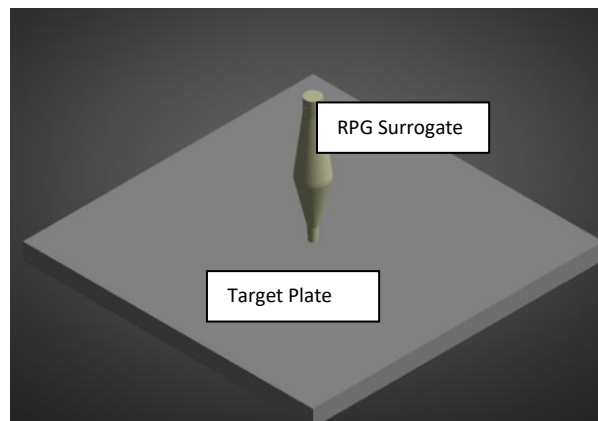


Figure 2.6 Isometric view of the Drop Test setup

The purpose of this analysis was to obtain a reference value of the equivalent (von Mises) stress at the RPG surrogate tip based on the reported 3.0 m safe-drop (no-fire) condition of the VP-7M fuse [23], providing a mechanical reference condition for comparison with the UAS impact cases. Since PG-7-series warheads employ a nose-base piezoelectric fuse, with the piezoelectric generator located in the nose section [6], the tip region of the surrogate was selected as the primary evaluation point. Accordingly, reaching or exceeding this reference indicates a mechanical tip-loading response comparable to or greater than that obtained from the simulated no-fire drop condition and does not imply fuse functioning, initiation, or detonation.

The RPG surrogate dropped from rest at a height of 3.0 m and allowed to fall vertically onto the target under gravity, corresponding to an impact velocity of approximately 7.67 m/s, neglecting aerodynamic drag. To preserve the physical meaning of the reference condition, the drop-test analysis used only the 2.00 kg RPG surrogate body, with its assigned density of 1944.3 kg/m³, excluding the rack and support structure.

To maintain consistency in the comparison, the same material definitions for the RPG surrogate and target plate, contact treatment, and stress-evaluation procedure used in the main impact analyses were retained in the drop-test simulation. The surrogate was oriented such that the tip was the first point of contact with the target, thereby representing the most direct loading path to the surrogate-tip region during impact. The resulting time history of the equivalent (von Mises) stress at the surrogate tip was then extracted for subsequent evaluation.

2.6 Projectile-impact studies

Projectile-impact studies provide a broader foundation for understanding high-rate deformation, penetration, and failure under impact loading. Ballistic penetration of steel plates using high precision experiments together with nonlinear finite-element simulations, incorporating viscoplasticity and ductile damage in the numerical model was investigated in [24]. Experimental testing and LS-DYNA simulations were conducted in [25] to analyze the ballistic perforation resistance of thin concrete slabs impacted by ogive-nose steel projectiles, with the numerical results showing good agreement with the experiments. Armor-piercing frangible projectiles impacting multilayer armor using both live-fire experiments and numerical models were also investigated in [26], with results in close agreement between the simulated and experimental responses. These works provide relevant background for high-rate impact modeling, material failure, and numerical experimental comparison

3. Results and discussion

A total of nine impact cases were analyzed by combining three impact velocities of 15, 20, and 25 m/s (54, 72, and 90 km/h) with three approach angles of 0°, 22.5°, and 45° relative to the horizontal axis, as shown in Figure 3.1. The selected velocities span the operating conditions considered for the proposed UAS platform, while the approach angles represent possible impact orientations.

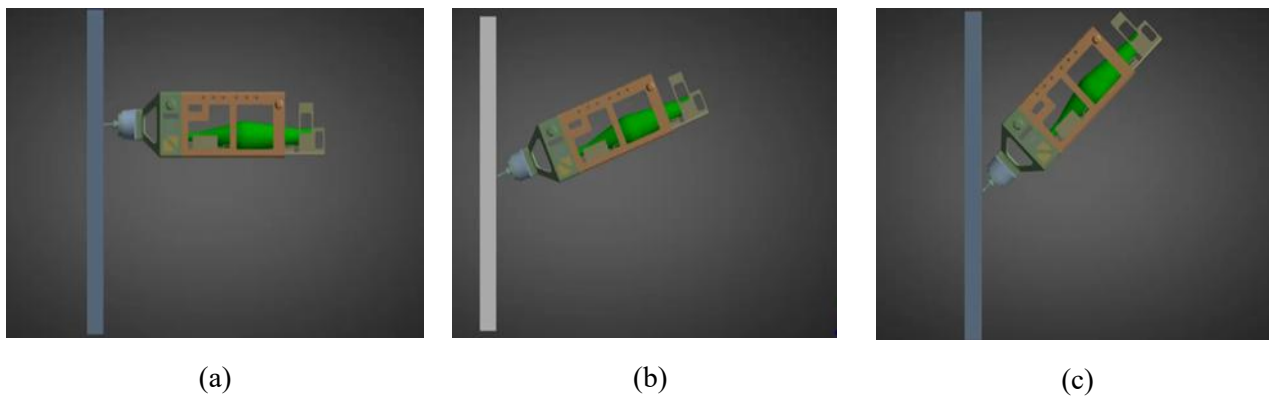


Figure 3.1 Impact setup with respect to the horizontal axis at (a) 0°, (b) 22.5°, and (c) 45°

The UAS fuselage section was defined as the contact body, while the AA5083-H116 plate was assigned as the target body. The plate boundary condition was modeled by constraining the plate edges to approximate a clamped support condition.

A mesh sensitivity analysis was also conducted to determine the element size that provides balance between computational efficiency and sufficient resolution of structural deformation. The analysis simulated the impact of the retained UAS components against the AA5083-H116 target plate and evaluated the resulting plate deformation. The mesh configuration selected from this study was then applied consistently to all nine impact cases to ensure numerical consistency and comparability across the simulation set.

3.1 Battery-to-plate impact tests replication results

The battery-to-plate impact tests reported in [17] were reproduced in ANSYS Explicit Dynamics and used to evaluate the global explicit-dynamics modeling framework, as shown in Figure 3.2. Following the approach in [11], the validation focused on the maximum central deflection and peak reaction load reported in [17].

Al 2024-T3 plates with thicknesses of 1.60 mm, 3.18 mm, and 6.35 mm, were impacted at the test velocities (Figure 3.2). For each plate thickness, the experimental shot with the smallest deviation from the target test conditions was selected for the simulation. The same isotropic-elastic and crushable-foam material properties were used for the battery in all cases, as listed in Table 2.4.

Table 3.1 compares the present simulation results with the experimental values in [17] and the numerical results in [11].

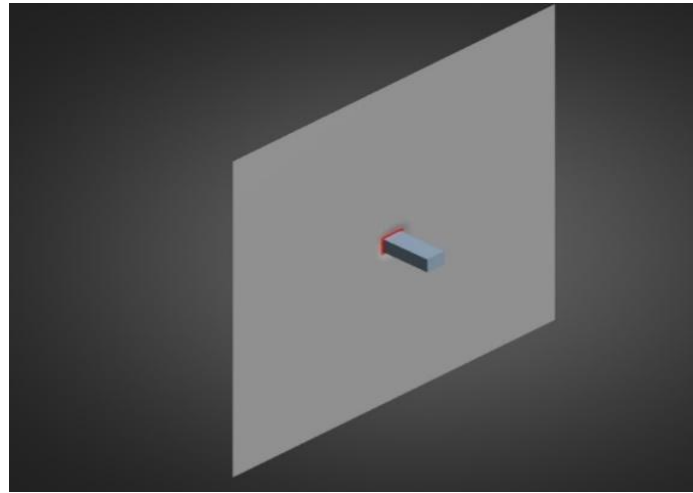


Figure 3.2 Battery-plate test at 1.60 mm plate, present simulation

Table 3.1. Maximum deflection and peak load values for the Al 2024-T3 plates

	Maximum deflection (mm)	Peak load (kN)	Maximum deflection deviation from experiment [17] (%)	Peak-load deviation from experiment [17] (%)
1.60 mm plate				
Test 5670 [17]	47.8	92.5		
Numerical model [11]	46.0	106.1	10.96	11.49
Present simulation	42.6	103.1		
3.18 mm plate				
Test 5675 [17]	14.5	48.2		
Numerical model [11]	12.7	33.6	10.34	12.23
Present simulation	16.0	54.1		
6.35 mm plate				
Test 5661 [17]	19.6	123.9		
Numerical model [11]	22.2	167.2	5.21	10.76
Present simulation	20.6	137.2		

Overall, the present model reproduced the observed deflection and peak-load trends across the different plate thicknesses. Relative to the experimental values in [17], the present simulations showed deviations of 5.21–10.96% in maximum deflection and 10.76–12.23% in peak load. These results support the use of the adopted modelling approach as a screening-level framework for the subsequent UAS impact analyses. Global mesh sensitivity for the UAS impact model

As part of the mesh-independence study, mesh sizes from 5 mm to 20 mm were tested using the same analysis settings. The resulting peak deformations are shown in Figure 3.3. Mesh convergence was assessed by comparing the peak deformation values, with a variation of 5% or less considered acceptable for this type of deformation-based evaluation. Compared with the 13 mm medium mesh, the 10 mm and 6 mm meshes differed by only 4.42% and 2.92%, respectively. This indicates that further refinement produced only a small change in the results and did not provide a meaningful improvement in accuracy. Based on the mesh-sensitivity assessments, a hybrid mesh comprising three element sizes was adopted: 13 mm for the target plate, 10 mm for the UAS frame, and 6 mm for critical components such as the motor, batteries, and RPG surrogate assembly. The suitability of the 6.0 mm surrogate mesh for the local tip-stress response is further evaluated in Section 3.4.3.1.

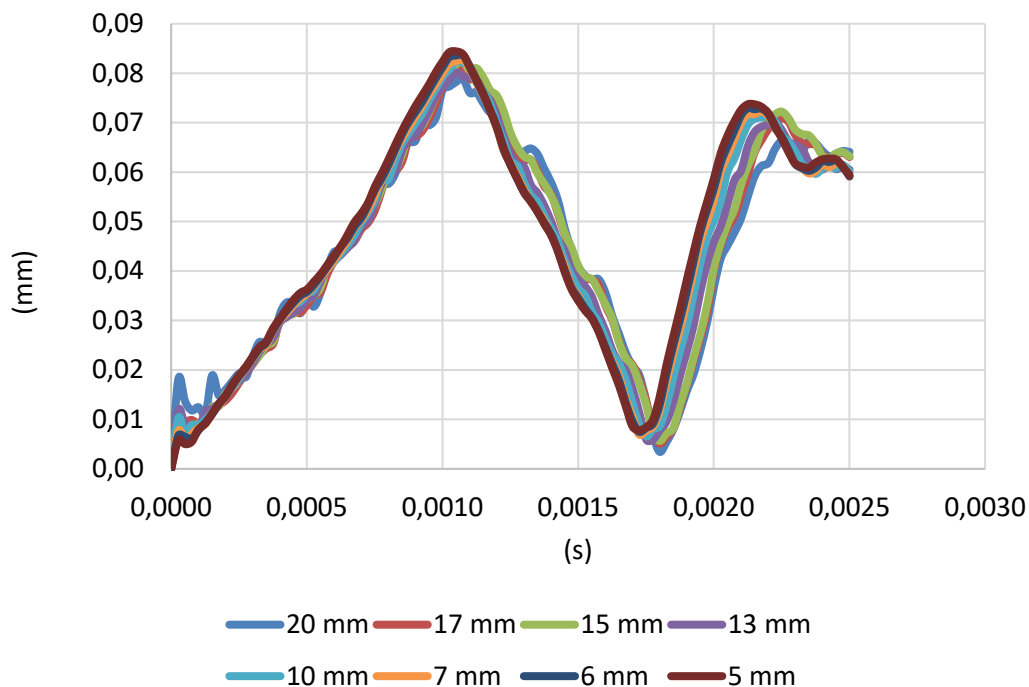


Figure 3.3 Mesh sensitivity results

3.3 PLA failure-strain sensitivity results

The frontal UAS PLA section was evaluated using maximum principal failure strains of 0.02, 0.03, and 0.05. The corresponding deformation pattern, equivalent (von Mises) stress and kinetic-energy responses were compared under the same impact condition.

3.3.1 PLA deformation pattern

The deformation patterns as shown in Figure 3.4 varied with the prescribed failure strain. At a failure strain of 0.05, only limited local chipping was observed and the frontal PLA section exhibited the greatest rebound. The 0.03 case showed slightly greater fragmentation and a reduced rebound, while the 0.02 case produced the most extensive local chipping and the least rebound. These observations indicate that reducing the prescribed failure strain promotes earlier fragmentation of the frontal PLA section and reduces its subsequent recovery after impact.

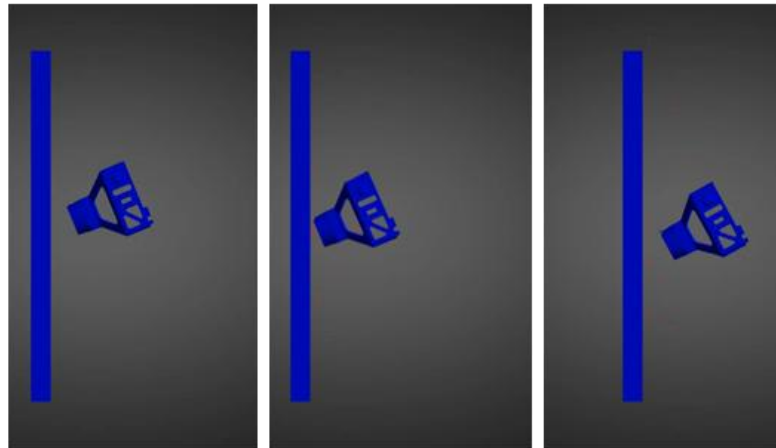


Figure 3.4 (a). Frontal PLA deformation for a maximum principal failure strain of 0.05 at 0, 5, and 10 ms

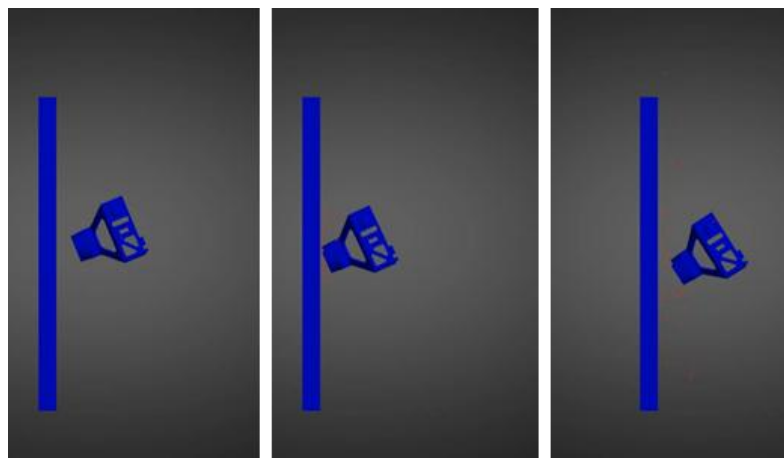


Figure 3.4 (b). Frontal PLA deformation for a maximum principal failure strain of 0.03 at 0, 5, and 10 ms

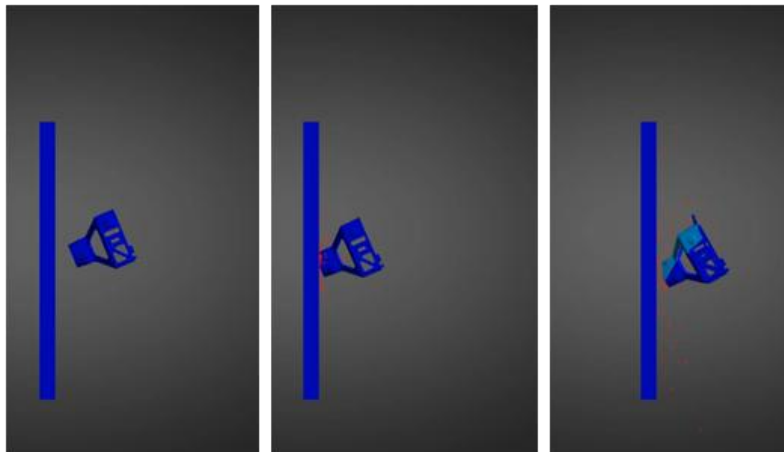


Figure 3.4 (c). Frontal PLA deformation for a maximum principal failure strain of 0.02 at 0, 5, and 10 ms

3.3.2 PLA equivalent (von Mises) stress

As shown in Figure 3.5 (a-c), the peak maximum equivalent (von Mises) stress increased from 206.27 MPa at 0.02 to 221.04 MPa at 0.03 and 231.24 MPa at 0.05, corresponding to an overall increase of about 12.1%. The peak average stress showed a larger variation, increasing from 1.004 MPa to 1.544 MPa, while the minimum stress remained very small and non-monotonic. Thus, for this impact condition, the peak maximum stress showed the smallest relative variation among the extracted stress measures.

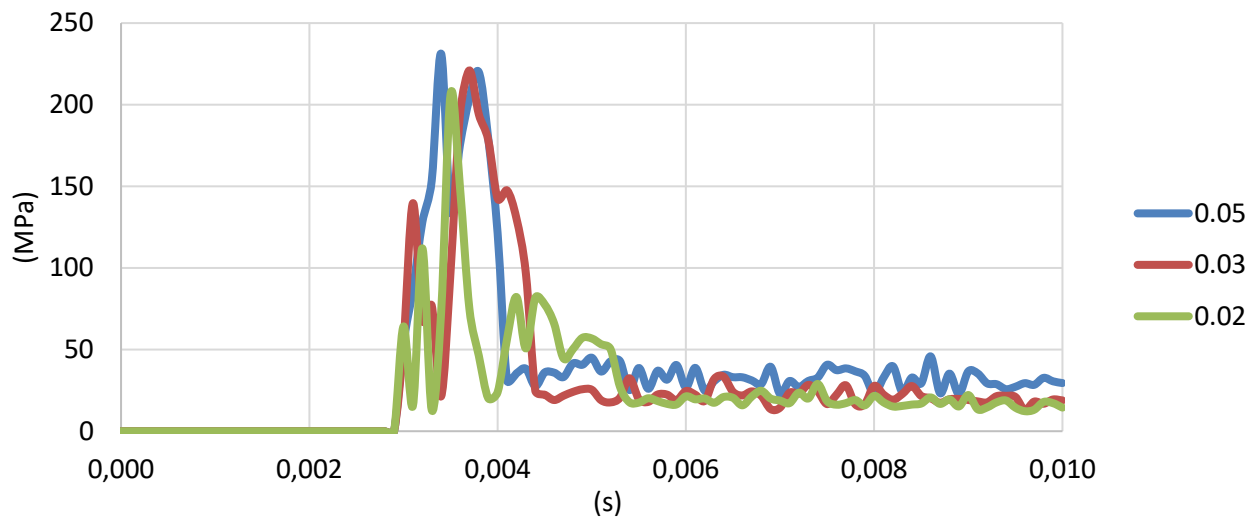


Figure 3.5 (a). Maximum equivalent (von Mises) stress histories of the frontal PLA section for different maximum principal failure strains

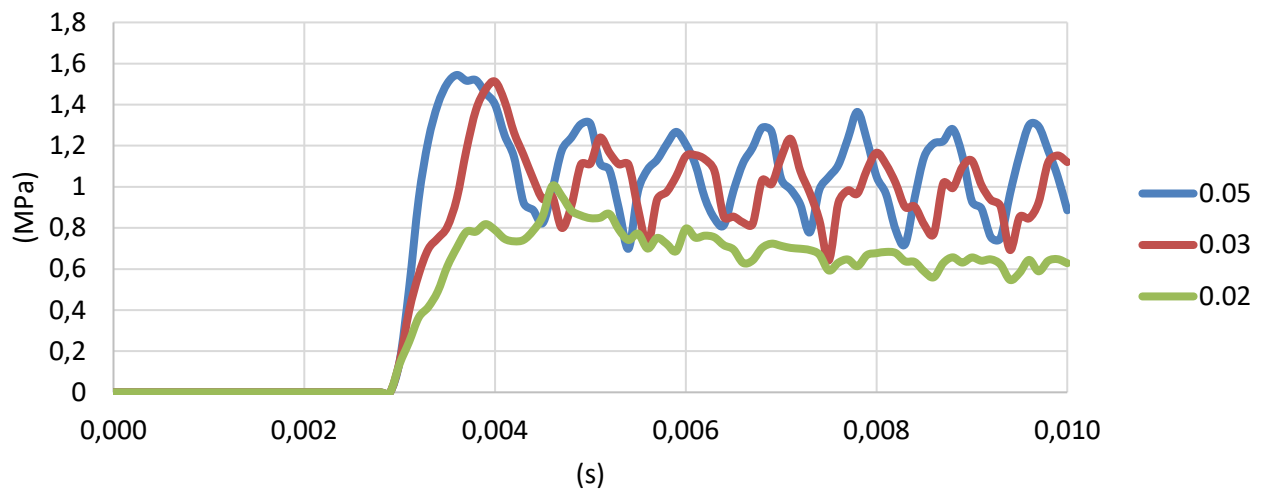


Figure 3.5 (b). Average equivalent (von Mises) stress histories of the frontal PLA section for different maximum principal failure strains

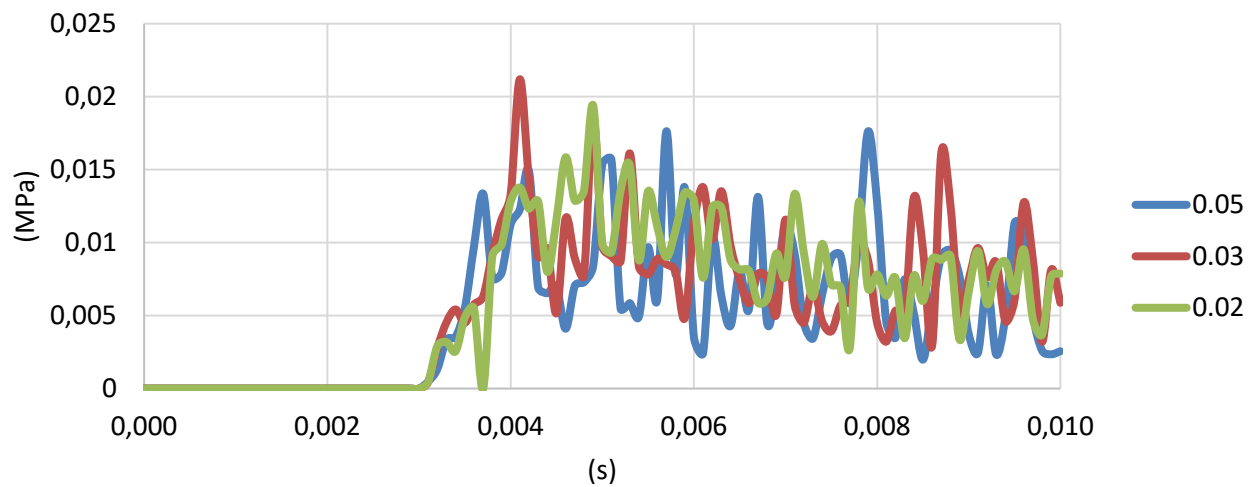


Figure 3.5 (c). Minimum equivalent (von Mises) stress histories of the frontal PLA section for different maximum principal failure strains

3.3.3 PLA kinetic energy

The kinetic-energy histories, presented in Figure 3.6 show that the minimum kinetic-energy remained comparatively consistent at 8.35–9.52 J, although its occurrence shifted earlier as the failure strain increased. This indicates that the assumed PLA failure strain affected the stress distribution and response timing more strongly than the overall kinetic-energy response.

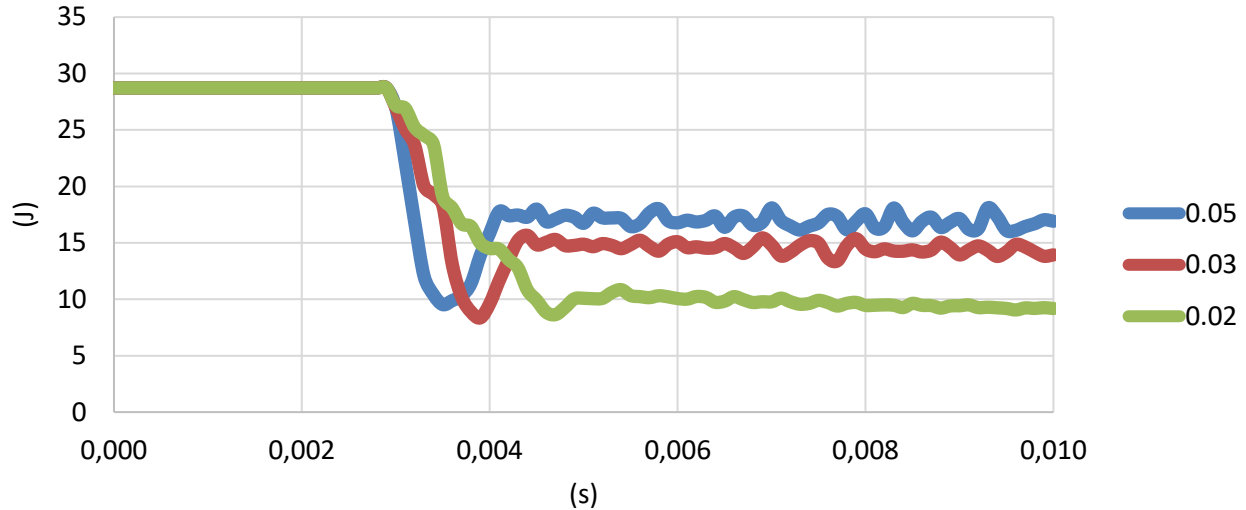


Figure 3.6. Kinetic-energy histories of the frontal PLA section for different maximum principal failure strains. Overall, the reference failure strain of 0.03 produced a response intermediate to those obtained for the 0.02 and 0.05 cases. Accordingly, the subsequent nine UAS impact simulations were conducted using 0.03 as the reference failure-strain value. Table 3.2 summarizes the PLA sensitivity-study results.

Table 3.2. Frontal PLA response for different maximum principal failure strains

Failure strain	Peak MIN stress (MPa)	Peak AVG stress (MPa)	Peak MAX stress (MPa)	Minimum KE (J)
0.02	0.0194	1.004	206.27	8.64
0.03	0.0212	1.512	221.04	8.35
0.05	0.0176	1.544	231.24	9.52

3.4 UAS impact simulations on AA5083-H116 plate

Nine impact scenarios were performed using the benchmarked explicit-dynamics modelling framework, the selected mesh configuration, and the reference PLA failure strain of 0.03 examined in the preceding sensitivity study. Each case represented a different combination of impact velocity and approach angle. The simulations were used to examine fuselage deformation during impact and to analyze the main response parameters, particularly the payload assembly kinetic-energy reduction and the equivalent (von Mises) stress at the RPG surrogate tip.

3.4.1 Fuselage behavior during various impact cases

The structural response of the UAS fuselage under different impact conditions is examined by considering the deformation and fragmentation of the PLA frame, as well as the interaction among the internal components. Particular attention is given to how the fuselage deforms and collapses during impact, as this behavior affects the way impact energy is transferred to the RPG surrogate.

3.4.1.1 Fuselage behavior on 0° impact angle

At 15 m/s (Figure 3.7), structural damage is concentrated primarily in the frontal fuselage region, affecting the motor, batteries, and surrounding fuselage structure. The main body remains comparatively less damaged than in the higher-velocity cases. At 20 m/s (Figure 3.8), a similar overall deformation pattern is observed, although the fragmentation becomes more pronounced and extends farther along the main fuselage body. At 25 m/s (Figure 3.9), the same general failure pattern persists but with substantially greater structural disruption, with more extensive deformation and damage distributed along the fuselage. The simulations therefore show that increasing impact velocity at 0° does not substantially alter the general location of the initial damage but increases its severity and extent along the main body.

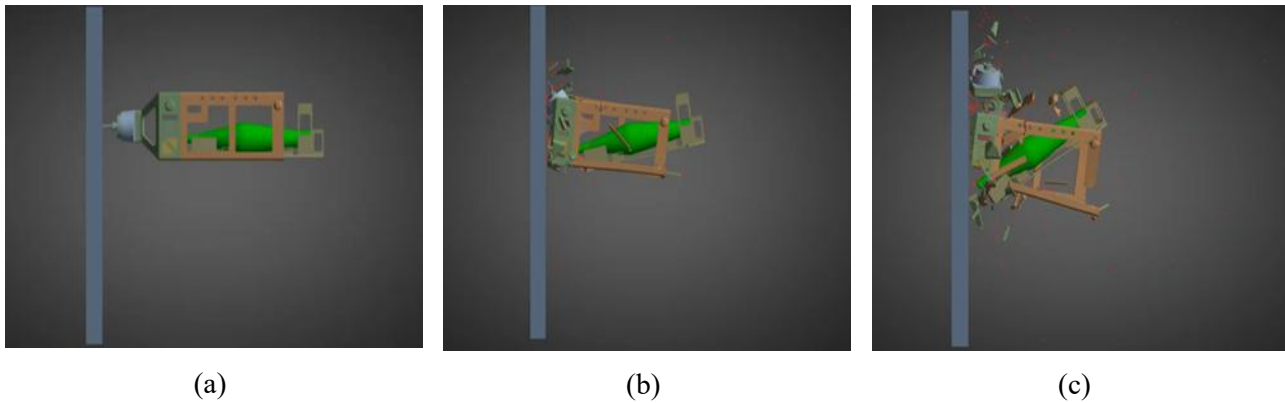


Figure 3.7 Fuselage impact at 15 m/s normal to the plate at (a) 0 ms (before contact), (b) 11 ms, and (c) 22 ms

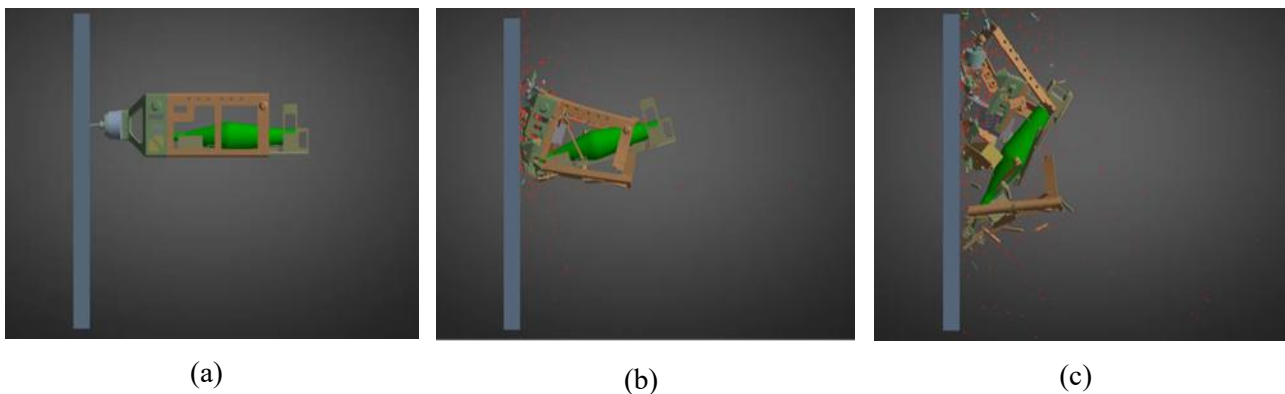


Figure 3.8 Fuselage impact at 20 m/s normal to the plate at (a) 0 ms (before contact), (b) 11 ms, and (c) 22 ms

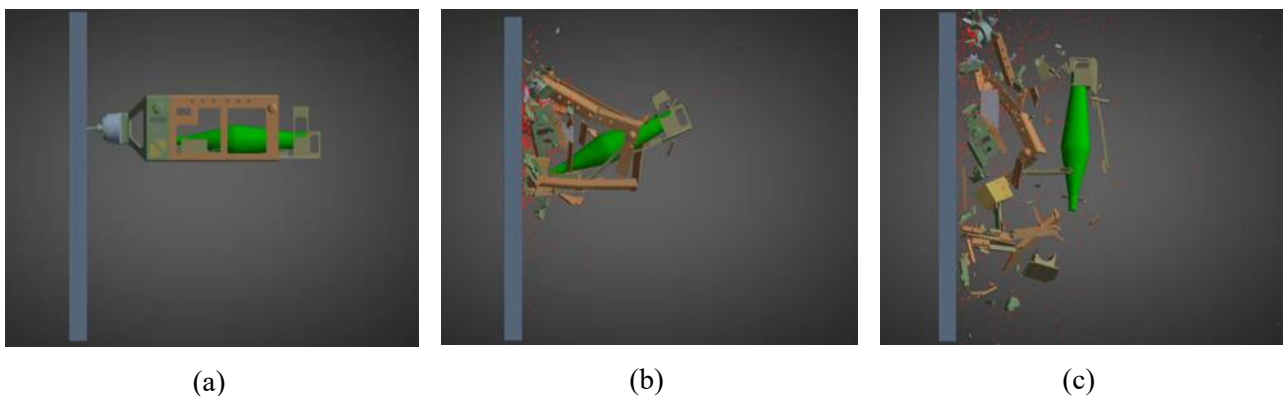


Figure 3.9 Fuselage impact at 25 m/s normal to the plate at (a) 0 ms (before contact), (b) 11 ms, and (c) 22 ms

3.4.1.2 Fuselage behavior on 22.5° impact angle

At 15 m/s (Figure 3.10), structural damage is concentrated primarily in the frontal region of the fuselage, similar to the behavior observed for the 0° impact condition. The motor mount, frontal frame, and surrounding fuselage structure experience the most noticeable deformation, while the remainder of the UAS structure is comparatively less affected. At 20 m/s (Figure 3.11), the same general damage pattern is observed, but deformation becomes more pronounced and extends farther along the fuselage. At 25 m/s (Figure 3.12), the severity and extent of structural damage increase further, with greater disruption of the front and main fuselage sections. For the 22.5° impact cases, increasing impact velocity intensifies the structural damage while maintaining a broadly similar frontal-damage pattern across the three velocities.

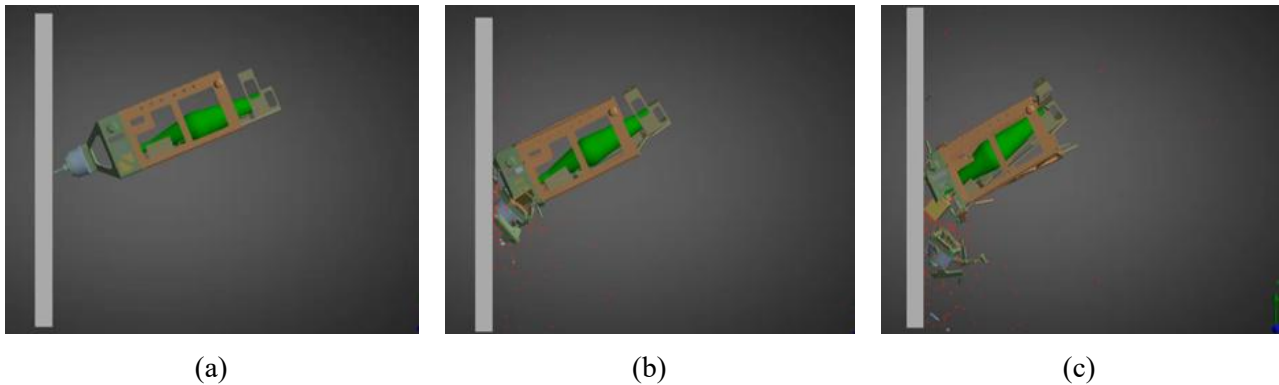


Figure 3.10. Fuselage impact behavior at 15 m/s at a 22.5° angle to the horizontal axis of the plate at (a) 0 ms (before contact), (b) 11 ms, and (c) 22 ms

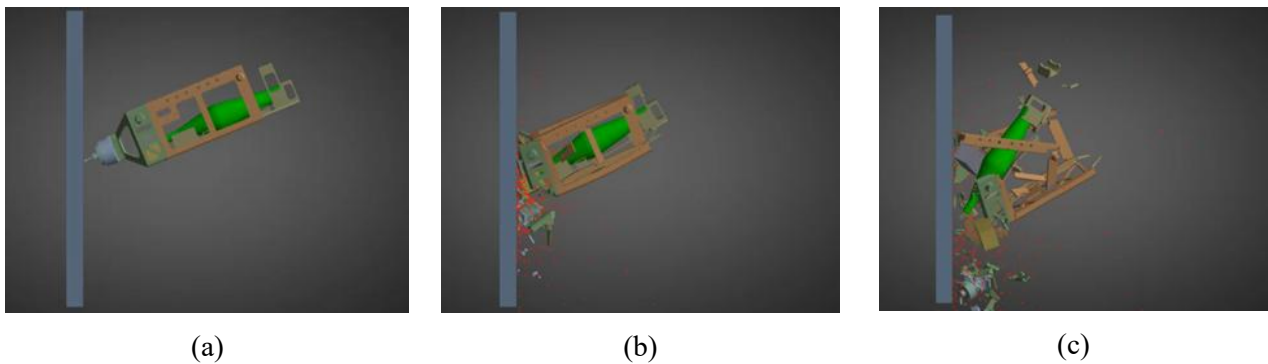


Figure 3.11. Fuselage impact behavior at 20 m/s at a 22.5° angle to the horizontal axis of the plate at (a) 0 ms (before contact), (b) 11 ms, and (c) 22 ms

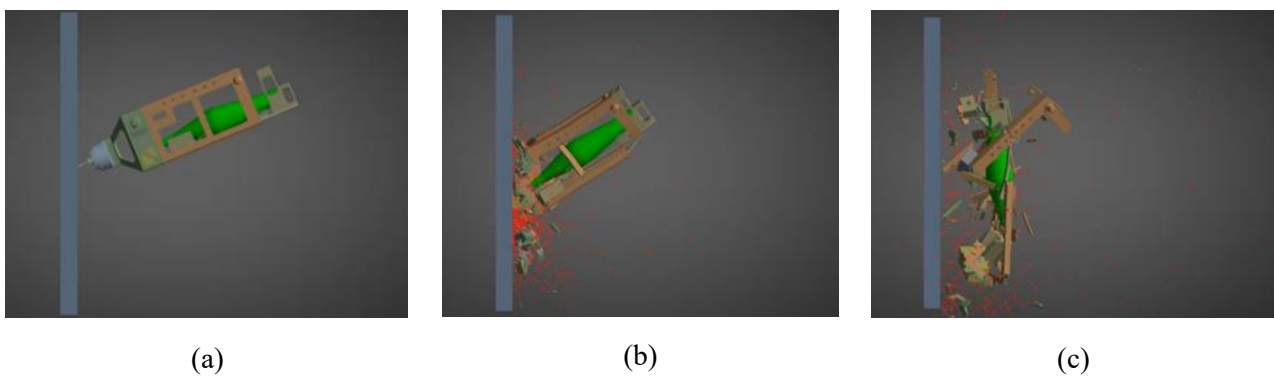


Figure 3.12. Fuselage impact behavior at 25 m/s at a 22.5° angle to the horizontal axis of the plate at (a) 0 ms (before contact), (b) 11 ms, and (c) 22 ms

3.4.1.3 Fuselage behavior on 45° impact angle

At 15 m/s (Figure 3.13), initial structural damage is concentrated primarily in the frontal region, while most of the remaining fuselage structure remains intact. Some components and structural parts become damaged, displaced, or separated, but widespread disintegration is not observed. At 20 m/s (Figure 3.14), the overall response remains similar, with damage still concentrated mainly in the frontal region; however, the severity of deformation, localized structural breakup, and part separation becomes more pronounced compared with the 15 m/s case. At 25 m/s (Figure 3.15), the impact produces a substantially more severe response, characterized by more extensive frontal damage, greater component separation, and increased disintegration of the fuselage structure. Across the 45° impact cases, the same general frontal-damage pattern remains broadly similar across the three velocities, while the severity and extent of the structural damage progressively increase with impact velocity, with the most pronounced escalation occurring at 25 m/s.

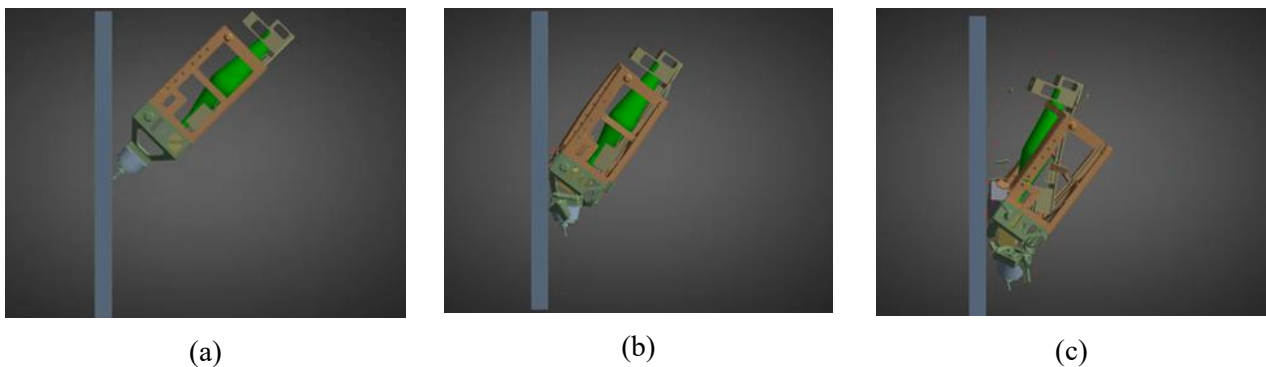


Figure 3.13. Fuselage impact behavior at 15 m/s at a 45° angle to the horizontal axis of the plate at (a) 0 ms (before contact), (b) 11 ms, and (c) 22 ms

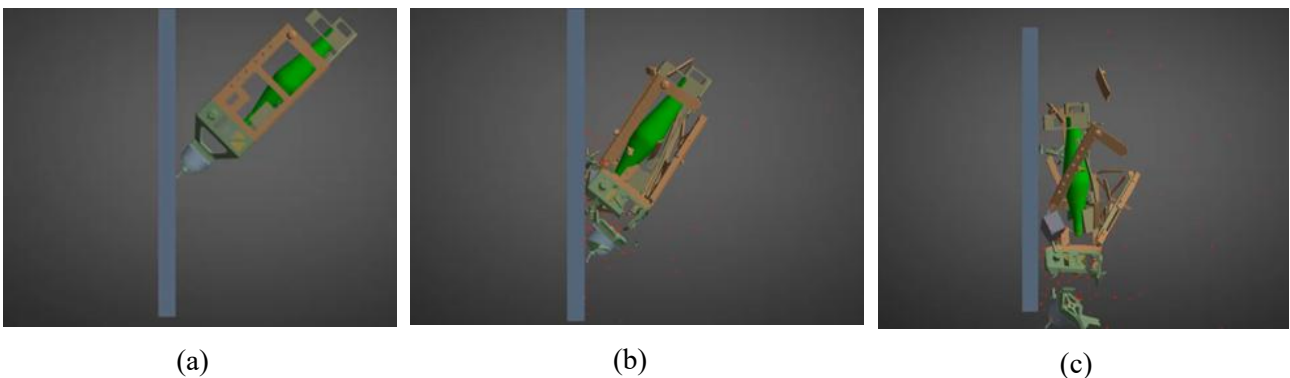


Figure 3.14. Fuselage impact behavior at 20 m/s at a 45° angle to the horizontal axis of the plate at (a) 0 ms (before contact), (b) 11 ms, and (c) 22 ms

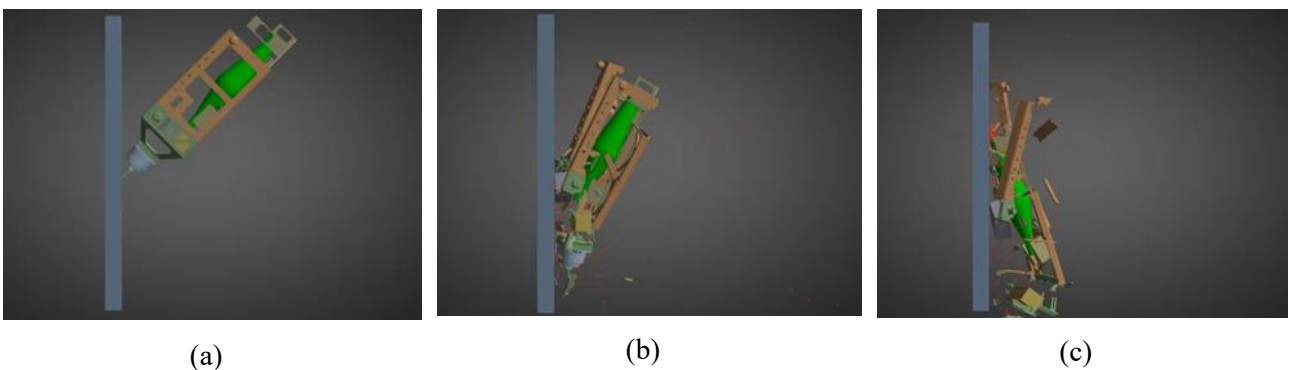


Figure 3.15. Fuselage impact behavior at 25 m/s at a 45° angle to the horizontal axis of the plate at (a) 0 ms (before contact), (b) 11 ms, and (c) 22 ms

Among the three impact orientations, the structural response results show a consistent damage pattern across the various impact orientations. Damage is generally concentrated in the frontal region of the fuselage, where the motor, batteries, supporting frames, and surrounding structure are subjected to the initial impact loading. As impact velocity increases, the severity of the structural fragmentation increases while the general location of the damage remains broadly similar. Higher impact velocities produce structural deformation, component displacement, and partial fuselage disintegration in the more severe cases. The results therefore indicate that impact velocity primarily governs the severity and extent of structural damage, while the impact angle influences the manner in which the resulting deformation and component motion develop.

3.4.2 RPG surrogate assembly energy response on various impact cases

The RPG surrogate assembly kinetic-energy reduction, defined as $\Delta KE(t) = KE(t_0) - KE(t)$, is used to characterize the magnitude and temporal development of the surrogate's reduction in kinetic-energy across the nine velocity-angle combinations. For each impact case, the maximum observed kinetic-energy reduction, its corresponding time, and the temporal pattern of the kinetic-energy response are evaluated within the prescribed 22 ms observation window.

3.4.2.1 RPG surrogate ΔKE response at 15 m/s

At 15 m/s (Figure 3.16), the 0° and 22.5° cases exhibited comparable maximum observed kinetic-energy reductions of 189.13 J and 191.39 J, respectively, whereas the 45° case reached a lower maximum of 104.83 J. The 0° case showed an initial increase in ΔKE , followed by partial recovery and a subsequent increase that produced the maximum observed reduction at 21.12 ms. The 22.5° case reached an early local maximum of approximately 139 J at 5.5 ms, decreased during the intermediate response, and increased again after approximately 15 ms to reach its maximum observed value at 21.34 ms. The 45° case exhibited a more gradual increase in ΔKE and reached its maximum observed value at 19.36 ms. These results indicate that similar maximum kinetic-energy reductions may develop through different temporal response patterns, while the 45° impact condition produced a substantially smaller reduction than the lower-angle cases at the same initial velocity.

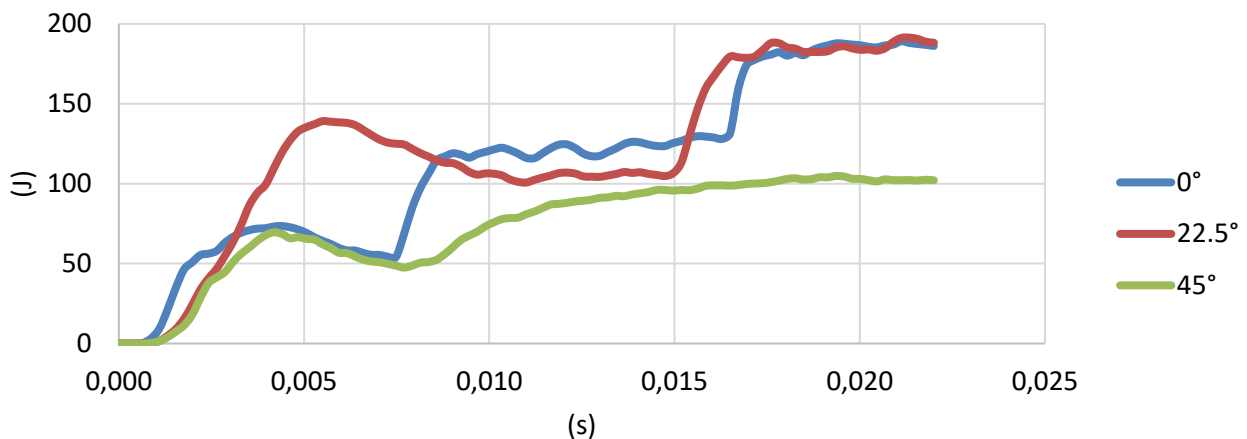
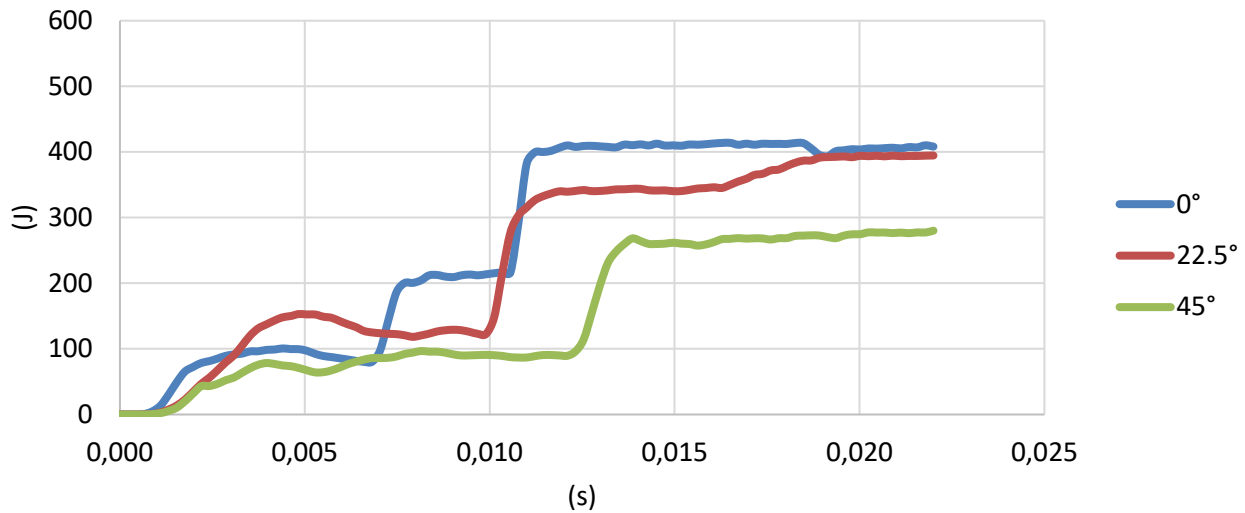


Figure 3.16. RPG surrogate ΔKE response at 15 m/s

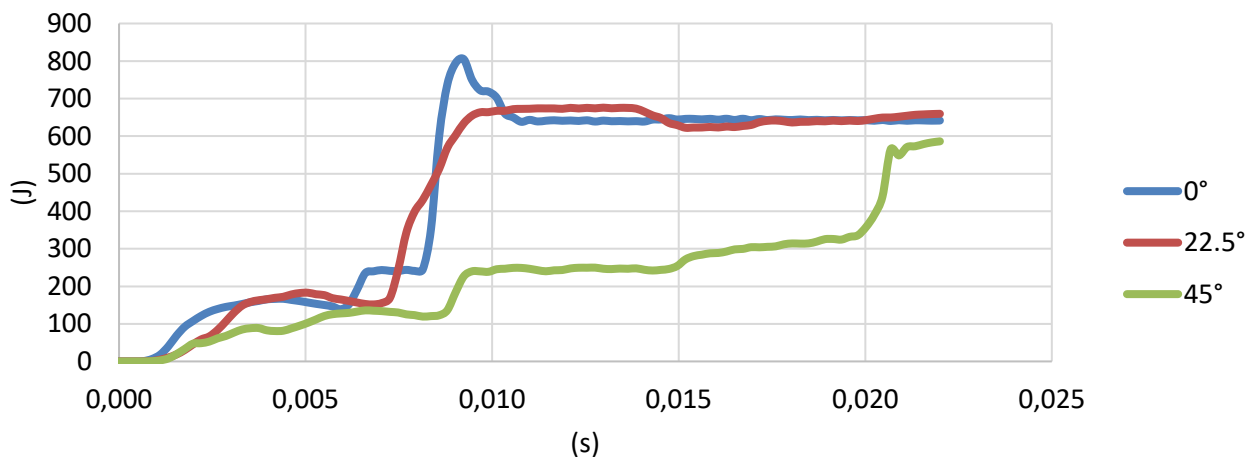
3.4.2.2 RPG surrogate ΔKE response at 20 m/s

At 20 m/s (Figure 3.17), the maximum observed kinetic-energy reduction decreased with increasing approach angle. The 0° case exhibited the highest ΔKE of 413.93 J at 16.50 ms, followed by 394.64 J for 22.5° and 280.10 J for 45° , with the latter two maxima occurring at the 22 ms observation limit. The 0° and 22.5° cases showed an initial increase followed by partial recovery and a subsequent pronounced increase in ΔKE , whereas the 45° case exhibited a delayed and comparatively smaller response. Thus, increasing the approach angle reduced and delayed the development of the RPG surrogate assembly kinetic-energy reduction.

Figure 3.17. RPG surrogate ΔKE response at 20 m/s

3.4.2.3 RPG surrogate ΔKE response at 25 m/s

At 25 m/s (Figure 3.18), the maximum observed kinetic-energy reduction decreased with increasing approach angle. The 0° case exhibited the highest ΔKE of 804.74 J at 9.24 ms, followed by 675.88 J for 22.5° at 12.98 ms and 586.17 J for 45° at the 22 ms observation limit. The 0° case showed the most rapid increase in ΔKE , followed by partial recovery after the maximum response. The 22.5° case developed more gradually and remained at a relatively high ΔKE level during the intermediate portion of the observation window. In contrast, the 45° case exhibited a delayed response, with the largest increase occurring near the end of the simulation. At 25 m/s, increasing the approach angle similarly reduced and delayed the kinetic-energy reduction.

Figure 3.18. RPG surrogate ΔKE response at 25 m/s

Across the nine impact cases, the RPG surrogate ΔKE response was governed by both impact velocity and approach angle. Increasing velocity increased the maximum observed kinetic-energy reduction, while increasing approach angle generally reduced its magnitude and delayed its development. The histories also showed that comparable maximum reductions could develop through different response patterns, including periods of partial kinetic-energy recovery and renewed reduction. These results indicate that higher impact velocity intensified the RPG surrogate assembly kinetic-energy reduction, while greater impact obliquity generally reduced and delayed the response within the prescribed 22 ms observation window. Table 3.3 summarizes the maximum observed ΔKE , the time at which it occurred, and the corresponding terminal ΔKE at the 22 ms observation limit for all nine impact conditions.

Table 3.3 Maximum observed RPG surrogate ΔKE reduction and corresponding time, $t_{\Delta KE}$

Angle	Velocity	Maximum observed ΔKE reduction (J)	$t_{\Delta KE}(\text{ms})$	ΔKE at 22 ms (J)
0°	15 m/s	189.13	21.12	186.18
	20 m/s	413.93	16.50	408.21
	25 m/s	804.74	9.24	641.59
22.5°	15 m/s	191.39	21.34	188.24
	20 m/s	394.64	22.00*	394.64
	25 m/s	675.88	12.98	659.43
45°	15 m/s	104.83	19.36	102.13
	20 m/s	280.10	22.00*	280.10
	25 m/s	586.17	22.00*	586.17

* Maximum observed value occurred at the 22 ms observation limit.

For cases in which the maximum observed ΔKE occurs at the 22 ms observation limit, the reported value is treated as an endpoint-limited observation within the prescribed analysis window.

3.4.3 Reference tip-stress determination

The equivalent (von Mises) stress developed rapidly at the RPG surrogate tip immediately following impact, as shown in Figure 3.19. The tip stress response was evaluated over the defined tip contact surface to characterize the local loading transmitted to the surrogate-tip region. The resulting reference stress was subsequently used as a reference for comparison with the nine UAS impact cases.

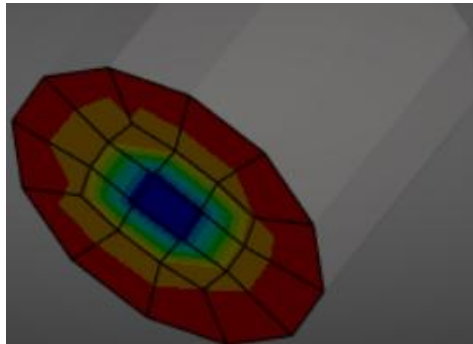


Figure 3.19 (a). Maximum equivalent (von Mises) stress distribution in the RPG surrogate tip during the drop-test simulation

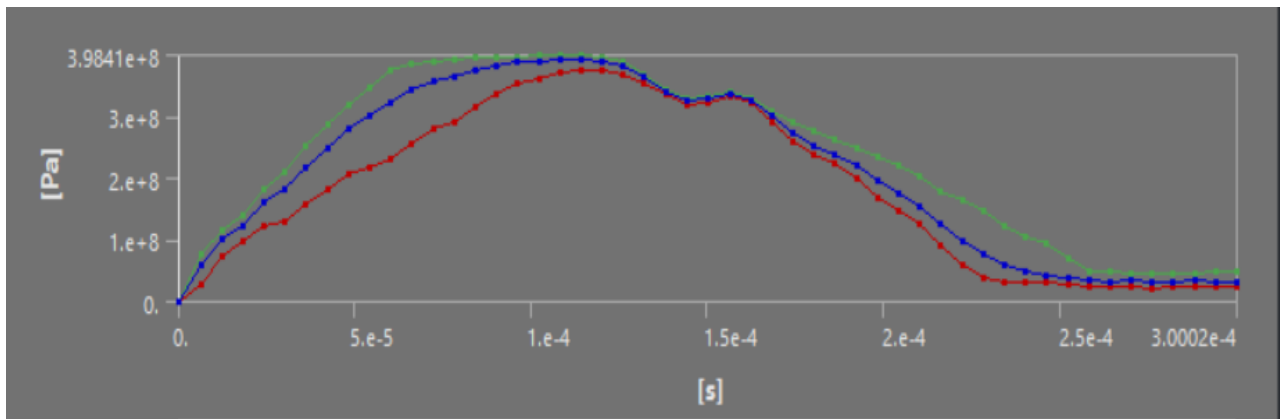


Figure 3.19 (b). Time history of the equivalent (von Mises) stress at the RPG surrogate tip obtained from the drop-test simulation for reference tip stress determination

3.4.3.1 RPG surrogate tip stress mesh sensitivity

A local mesh-sensitivity study was conducted at the surrogate tip to assess the dependence of the extracted equivalent stress response on spatial discretization. Local tip mesh sizes of 6.0, 5.0, 3.0, and 1.5 mm were evaluated while maintaining a 6.0 mm mesh size over the remainder of the surrogate body and preserving the same tip-surface stress extraction region. The minimum, surface-average, and maximum equivalent stress histories for the different mesh sizes are presented in Figure 3.20 (a–c), while the corresponding characteristic values are summarized in Table 3.4.

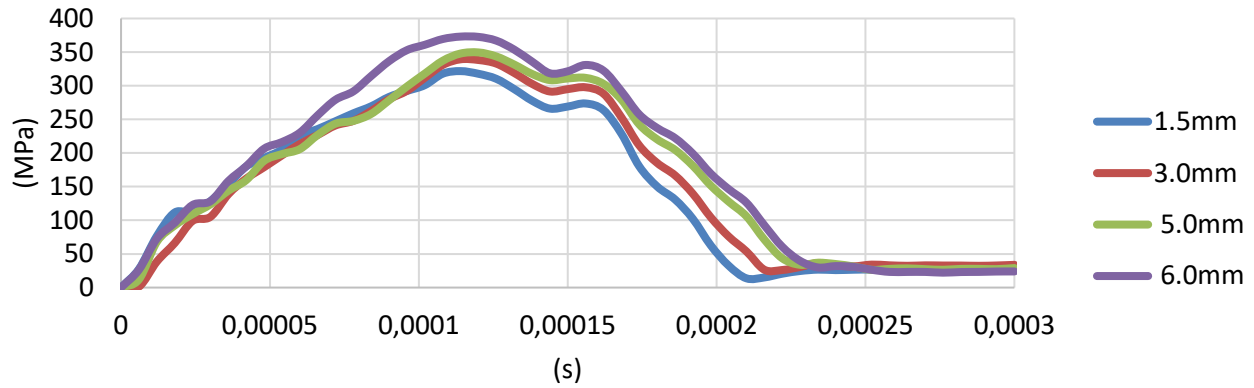


Figure 3.20 (a). Minimum RPG surrogate tip equivalent (von Mises) stress

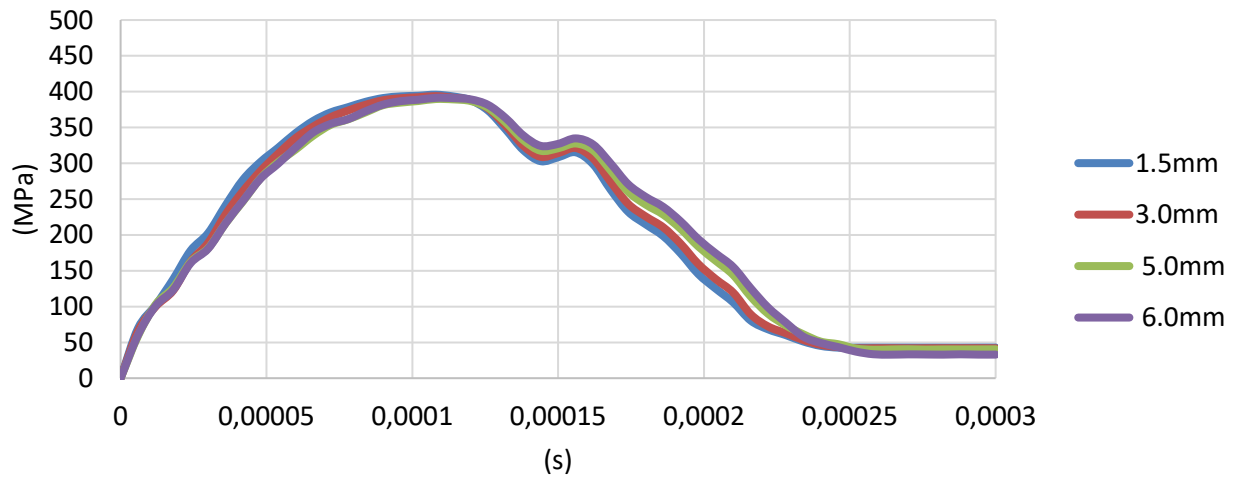


Figure 3.20 (b). Average RPG surrogate tip equivalent (von Mises) stress

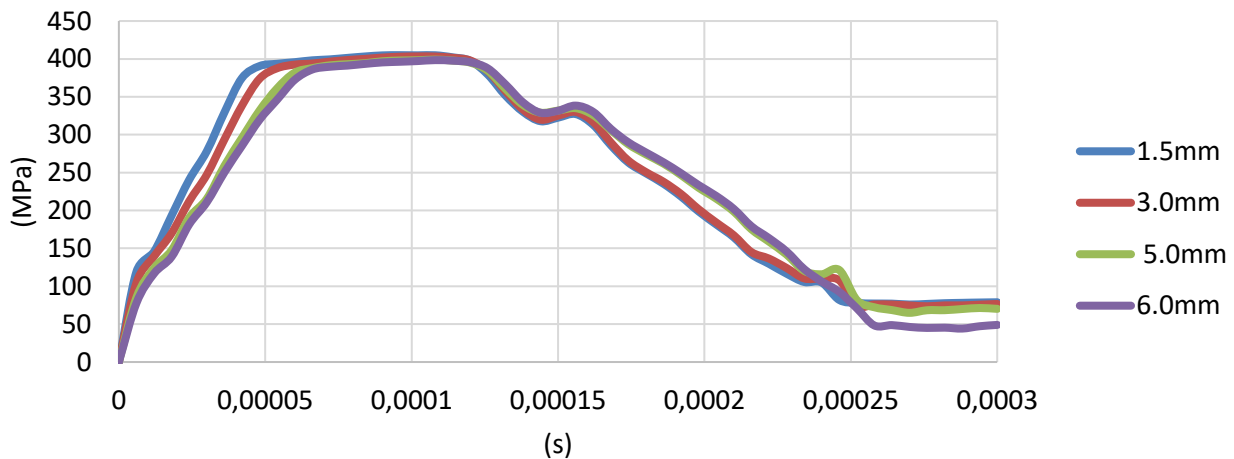


Figure 3.20 (c). Maximum RPG surrogate tip equivalent (von Mises) stress

Table 3.4. Mesh-sensitivity results for the surrogate tip surface

MESH SIZE	MIN (MPa)	AVERAGE (MPa)	MAX (MPa)	Percentage change relative to the next coarser local tip mesh		
				Δ MIN	Δ AVERAGE	Δ MAX
1.5 mm	322	396	405	5.17%	0.52%	0.41%
3.0 mm	339	394	403	2.93%	1.03%	1.06%
5.0 mm	350	390	399	6.31%	0.43%	0.16%
6.0 mm	373	391	398	-	-	-

Note: Percentage changes were calculated using the unrounded extracted stress values.

The maximum and surface-average equivalent stresses showed only small changes with progressive mesh refinement. In particular, refinement from 3.0 to 1.5 mm produced changes of approximately 0.41% in the maximum stress and 0.52% in the surface-average stress, indicating practical convergence of these quantities. In contrast, the minimum surface stress remained more sensitive to mesh refinement and was therefore not used as the primary measure of local impact severity. Overall, the maximum and surface-average equivalent stress responses were not strongly dependent on further local mesh refinement. The peak maximum stress varied from approximately 398 MPa for the 6.0 mm mesh to 405 MPa for the 1.5 mm mesh. Accordingly, approximately 400 MPa was adopted as a rounded reference stress for subsequent comparison of the UAS impact cases. The local refinement study demonstrates practical mesh stability of the defined tip-surface stress-extraction procedure under the reference drop-test configuration. Based on the local refinement study performed using the reference drop-test configuration, the selected 6.0 mm surrogate mesh and the same tip-surface extraction procedure were subsequently applied consistently to the nine UAS impact cases.

3.4.3.2 RPG surrogate equivalent (von Mises) stress on various impact cases

The equivalent (von Mises) stress of the RPG surrogate tip is evaluated for the nine velocity–angle combinations considered in this study, as shown in Figures 3.21-3.23. For each case, the tip stress history is used to characterize the magnitude and timing of local impact loading during target contact. These stress histories indicate how effectively impact forces are transmitted through the fuselage–payload to the surrogate-tip contact region. The results are interpreted relative to the adopted reference stress of 400 MPa, obtained from the drop test simulation. Emphasis is placed on the effects of impact velocity and approach angle on peak stress development, the occurrence of several stress peaks during arrest, and the extent to which the computed tip stress approaches or exceeds the reference.

3.4.3.3 RPG surrogate equivalent (von Mises) stress on 0° impact angle

Figure 3.21 shows the equivalent (von Mises) stress histories at the RPG surrogate tip for the 0° impact cases at 15, 20, and 25 m/s. All three cases exhibit multiple transient stress peaks during impact arrest, indicating repeated changes in the local loading transmitted to the surrogate tip. The 15 m/s case reaches a peak stress of 406 MPa at approximately 16.94 ms, while the 20 and 25 m/s cases attain peak stresses of 491 MPa at 10.78 ms and 483 MPa at 9.24 ms, respectively. The 20 and 25 m/s cases produced comparable peak responses of about 490 MPa. The occurrence of the maximum stress also shifts to an earlier time as impact velocity increases. Relative to the adopted reference stress of 400 MPa, all three 0° impact cases reach or exceed the reference level.

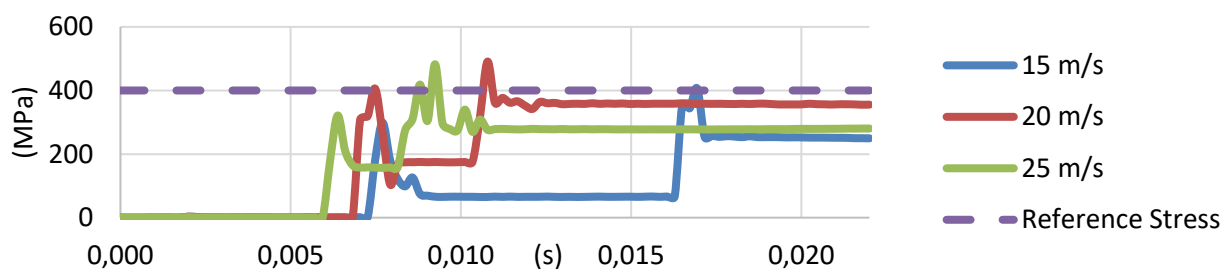


Figure 3.21. Equivalent (von Mises) stress at 0°

3.4.3.4 RPG surrogate equivalent (von Mises) stress on 22.5° impact angle

Figure 3.22 shows the equivalent (von Mises) stress histories at the RPG surrogate tip for the 22.5° impact cases at 15, 20, and 25 m/s. Similar to the 0° impact cases, the stress response exhibits multiple transient peaks during impact arrest, reflecting variations in the local loading transmitted to the surrogate tip. The peak stress increases with impact velocity, from 275 MPa at approximately 16.06 ms for the 15 m/s case to 360 MPa at 10.34 ms for the 20 m/s case and 417 MPa at 8.80 ms for the 25 m/s case. The occurrence of the maximum stress therefore shifts progressively earlier as impact velocity increases. Relative to the adopted reference stress of 400 MPa, the 15 and 20 m/s cases remain below the reference level, while the 25 m/s case exceeds it. These results indicate that, for the 22.5° impact orientation, increasing impact velocity produces a corresponding increase in the maximum tip stress response while also causing the peak response to occur earlier.

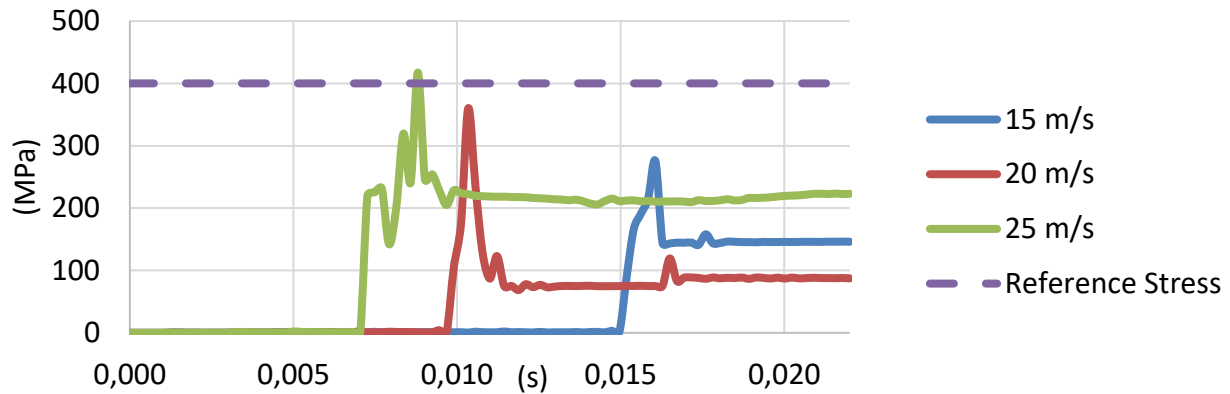


Figure 3.22. Equivalent (von Mises) stress at 22.5°

3.4.3.5 RPG surrogate equivalent (von Mises) stress on 45° impact angle

Figure 3.23 shows the equivalent (von Mises) stress histories at the RPG surrogate tip for the 45° impact cases at 15, 20, and 25 m/s. The three cases exhibit markedly different responses, with peak tip stress increasing substantially with impact velocity. The 15 m/s case reaches only 5 MPa at approximately 8.58 ms, while the 20 m/s case reaches 389 MPa at 14.08 ms and the 25 m/s case attains the highest value of 564 MPa at 9.02 ms. Unlike the 0° and 22.5° cases, the timing of the maximum stress does not vary monotonically with impact velocity, with the 20 m/s case exhibiting a comparatively delayed peak response. Relative to the adopted reference stress of 400 MPa, the 15 and 20 m/s cases remain below the reference level, although the 20 m/s case approaches it closely, whereas the 25 m/s case exceeds it.

The markedly lower stress response at 15 m/s is consistent with the structural behavior observed for the low-velocity 45° impact. In this case, damage is concentrated primarily in the frontal region, while most of the remaining fuselage structure remains intact, with only limited displacement or separation of some components. This comparatively limited structural disruption suggests that less severe loading was transmitted to the surrogate-tip region during the principal impact response. Consequently, the 45°–15 m/s case exhibits a substantially lower reported peak tip stress than the corresponding 20 and 25 m/s cases.

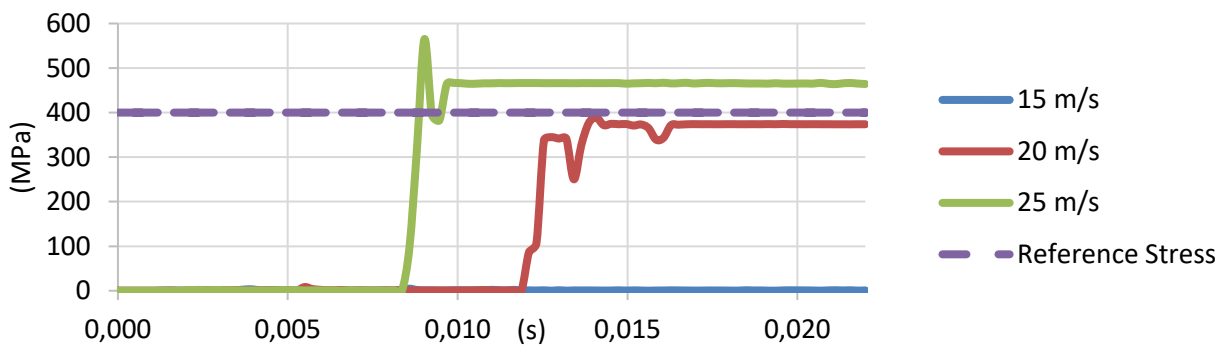


Figure 3.23. Equivalent (von Mises) stress at 45°

The tip stress results demonstrate that local loading at the RPG surrogate tip is not a simple, single stress-peak phenomenon. Rather, the response is built up from a sequence of several peaks whose timing, magnitude, and duration vary greatly. They also change significantly with both impact velocity and approach angle. This suggests that the local tip stress is highly sensitive to the way the impact load is transmitted through the fuselage–payload assembly during collapse.

Five of the nine impact cases reached or exceeded the adopted 400 MPa reference stress. For the 0° impacts, the peak stress increased from 406 MPa at 15 m/s to 491 MPa at 20 m/s, before decreasing slightly to 483 MPa at 25 m/s. The 20 and 25 m/s cases therefore produced comparable peak stresses. At 22.5°, the response increased progressively with velocity, from 275 MPa at 15 m/s to 360 MPa at 20 m/s and 417 MPa at 25 m/s. The 45° cases showed the largest changes in peak stress, increasing sharply from only 5 MPa at 15 m/s to 389 MPa at 20 m/s and then to 564 MPa at 25 m/s. The 45°–25 m/s case therefore produced the highest tip stress among all nine conditions. These results show that the tip stress response is strongly influenced by the combined effects of impact velocity and approach angle and does not follow a uniform trend across all impact conditions. Table 3.5 summarizes the maximum tip stress for each impact case.

Table 3.5 Summary of RPG surrogate tip maximum equivalent (von Mises) stress for each impact case.

Angle	Velocity	RPG surrogate tip maximum equivalent (von Mises) stress (MPa)	$t_{\sigma}(\text{ms})$	Relative to Reference (400 MPa)
0°	15 m/s	406	16.94	Reached reference
	20 m/s	491	10.78	Above reference
	25 m/s	483	9.24	Above reference
22.5°	15 m/s	275	16.06	Below reference
	20 m/s	360	10.34	Below reference
	25 m/s	417	8.8	Above reference
45°	15 m/s	5	8.58	Below reference
	20 m/s	389	14.08	Below reference
	25 m/s	564	9.02	Above reference

3.4.4 RPG surrogate ΔKE reduction and equivalent (von Mises) stress occurrence comparison

Table 3.6 compares the occurrence of the maximum surrogate tip stress with the maximum observed kinetic-energy reduction, ΔKE , for the nine impact cases. In eight cases, the peak tip stress occurs earlier than the maximum observed ΔKE , with time differences ranging from 4.18 to 12.98 ms. However, the 0°–25 m/s case shows coincident maxima at approximately 9.24 ms, indicating that the peak local tip loading and the maximum observed kinetic-energy reduction occurred at the same stage of the impact response. The comparison shows that the peak local tip stress generally develops earlier than the maximum observed kinetic-energy reduction, although both responses may coincide under certain impact conditions.

Table 3.6. Comparison of maximum surrogate tip stress and maximum observed kinetic-energy reduction

Angle	Velocity	Maximum tip stress (MPa)	$t_{\sigma,max}(\text{ms})$	Max ΔKE reduction (J)	$t_{\Delta KE,max}(\text{ms})$	$\Delta t = t_{\sigma,max} - t_{\Delta KE,max}$
0°	15 m/s	406	16.94	189.13	21.12	–4.18
	20 m/s	491	10.78	413.93	16.50	–5.72
	25 m/s	483	9.24	804.74	9.24	0
22.5°	15 m/s	275	16.06	191.39	21.34	–5.28
	20 m/s	360	10.34	394.64	22.00*	–11.66
	25 m/s	417	8.80	675.88	12.98	–4.18

Angle	Velocity	Maximum tip stress (MPa)	$t_{\sigma,max}(ms)$	Max ΔKE reduction (J)	$t_{\Delta KE,max}(ms)$	$\Delta t = t_{\sigma,max} - t_{\Delta KE,max}$
45°	15 m/s	5	8.58	104.83	19.36	-10.78
	20 m/s	389	14.08	280.10	22.00*	-7.92
	25 m/s	564	9.02	586.17	22.00*	-12.98

* Maximum observed value occurred at the 22 ms observation limit.

4. Conclusions

This study investigated the high-speed impact response of an additively manufactured UAS forward fuselage section carrying an inert RPG surrogate. An explicit-dynamics model was developed in ANSYS Explicit Dynamics to examine the structural response of the fuselage, the kinetic-energy reduction of the surrogate assembly, and the equivalent (von Mises) stress developed at the RPG surrogate tip. The modelling framework was first evaluated against published battery-to-plate impact data. Relative to the experimental results, the present simulations produced deviations of 5.21–10.96% in maximum plate deflection and 10.76–12.23% in peak load, supporting the use of the adopted modelling approach as a screening-level framework for the subsequent UAS impact analyses.

Additional numerical verification was conducted to assess the sensitivity of the results to the assumed PLA failure strain and the local mesh resolution at the surrogate tip. Maximum principal failure strains of 0.02, 0.03, and 0.05 were examined for the frontal PLA section. Increasing the prescribed failure strain reduced fragmentation and increased rebound, while the peak maximum equivalent (von Mises) stress varied from 206.27 to 231.24 MPa, corresponding to an overall variation of approximately 12.1%. The 0.03 case produced an intermediate response and was therefore used as the reference failure-strain condition in the nine principal UAS impact simulations. For the RPG surrogate tip stress response, local mesh sizes of 6.0, 5.0, 3.0, and 1.5 mm produced maximum stresses of approximately 398–405 MPa and surface-average stresses of approximately 390–396 MPa. Mesh refinement from 3.0 to 1.5 mm changed the maximum and surface-average stresses by only about 0.4% and 0.5%, respectively. Based on these results, approximately 400 MPa was adopted as a rounded mechanical reference stress for comparison among the UAS impact cases.

Nine impact conditions were investigated using velocities of 15, 20, and 25 m/s and approach angles of 0°, 22.5°, and 45°. Across the simulated cases, structural damage generally starts in the frontal region of the fuselage, where the motor, batteries and frontal frames first interacted with the target. Increasing impact velocity generally increased the severity of deformation, fragmentation, component displacement, and structural dislocation. The general location of the initial damage remained broadly similar among the investigated orientations, while the impact angle affected the subsequent deformation pattern and motion of the internal components.

The surrogate kinetic-energy response showed a clearer dependence on both velocity and approach angle. At 15 m/s, the maximum observed kinetic-energy reductions were 189.13 J, 191.39 J, and 104.83 J for 0°, 22.5°, and 45°, respectively. At 20 m/s, the corresponding values were 413.93 J, 394.64 J, and 280.10 J, while at 25 m/s they increased to 804.74 J, 675.88 J, and 586.17 J. Thus, increasing impact velocity generally increased the maximum observed kinetic-energy reduction, whereas increasing impact obliquity generally reduced its magnitude and delayed its development within the prescribed 22 ms observation window. The highest observed kinetic-energy reduction occurred at 25 m/s and 0°, while the lowest occurred at 15 m/s and 45°.

The RPG surrogate tip stress response exhibited a more localized and less uniform trend. Five of the nine impact cases reached or exceeded the approximately 400 MPa mechanical reference stress. For the 0° impacts, peak stresses of 406, 491, and 483 MPa were obtained at 15, 20, and 25 m/s, respectively, with the 20 and 25 m/s cases producing comparable peak stresses. At 22.5°, the peak stress increased progressively from 275 MPa at

15 m/s to 360 MPa at 20 m/s and 417 MPa at 25 m/s. The 45° cases showed the largest change with velocity, increasing sharply from 5 MPa at 15 m/s to 389 MPa at 20 m/s and 564 MPa at 25 m/s. The 45°–25 m/s condition therefore produced the highest tip stress among all nine cases. These results demonstrate that local tip stress does not follow the kinetic-energy trend uniformly and is also influenced by the evolving collapse sequence of the fuselage and the interaction of the internal components during arrest.

Comparison of the response timing further showed that the maximum surrogate-tip stress occurred before the maximum observed kinetic-energy reduction in eight of the nine cases, with time differences ranging from 4.18 to 12.98 ms. The 0°–25 m/s condition was the only case in which the two maxima coincided, at approximately 9.24 ms. This indicates that the maximum local loading at the surrogate tip generally develops during an earlier stage of the impact event than the maximum overall reduction in surrogate kinetic-energy.

Overall, the results show that impact velocity primarily governs the magnitude of structural disruption and surrogate assembly kinetic-energy reduction, while the approach angle modifies the manner and timing by which impact loads are transmitted through the fuselage–payload assembly. Although the RPG surrogate assembly kinetic-energy reduction and the maximum local tip stress are influenced differently by various factors, they still provide complementary measures of impact severity across the various cases. However, since the RPG surrogate was represented as a simplified homogeneous, density-adjusted body and the present analysis remains numerical, the results describe the mechanical response of the modeled inert surrogate rather than the detailed behavior of an actual RPG-7 assembly. Future work should incorporate controlled impact experiments and more representative surrogate construction to further evaluate the predicted structural, kinetic-energy, and local stress responses.

Declaration of competing interest

The authors declare that they have no known financial or non-financial competing interests in any material discussed in this paper.

Funding information

Funding was received from Mindanao State University - Iligan Institute of Technology (MSU-IIT) and Department of Science and Technology-Engineering Research and Development for Technology (DOST-ERDT).

Acknowledgements

The authors gratefully acknowledge the support of Mindanao State University (MSU)- Marawi City (Main Campus), Mindanao State University – Iligan Institute of Technology (MSU-IIT) and Cebu Technological University (CTU) for providing the academic and research environment necessary for this work. This study was supported in part by the Engineering Research and Development for Technology (ERDT) Scholarship Program, which enabled the completion of the work presented in this paper.

Declaration of use of AI in the writing process

The authors used OpenAI's ChatGPT during the preparation of this work for grammar checking and to improve the clarity and overall quality of the writing. The authors reviewed and edited the work as necessary and take full responsibility for the final version.

References

- [1] U.S. Army AMRDEC Public Affairs, “Switchblade 300 launch,” Wikimedia Commons, Jun. 14, 2021. [Online]. Available: https://commons.wikimedia.org/wiki/File:Switchblade_300_launch.jpg. (Accessed: Jan. 10, 2026).

-
- [2] K. Chávez and O. Swed, “Emulating underdogs: Tactical drones in the Russia-Ukraine war,” *Contemporary Security Policy*, vol. 44, no. 4, pp. 592–605, 2023, <https://doi.org/10.1080/13523260.2023.2257964>.
 - [3] M. Voskuijl, “Performance analysis and design of loitering munitions: A comprehensive technical survey of recent developments,” *Defence Technology*, vol. 18, no. 3, pp. 325–343, Mar. 2022, <https://doi.org/10.1016/j.dt.2021.08.010>.
 - [4] S. Nvss, B. Esakki, L.-J. Yang, C. Udayagiri, and K. S. Vepa, “Design and Development of Unibody Quadcopter Structure Using Optimization and Additive Manufacturing Techniques,” *Designs*, vol. 6, no. 1, Art. no. 8, Jan. 2022, <https://doi.org/10.3390/designs6010008>.
 - [5] M. R. Maligro et al., “Engineering a low-cost, expendable fixed-wing drone for search and rescue missions using readily available components,” *Sustainable Engineering and Innovation*, vol. 7, no. 2, pp. 537–558, Nov. 2025, <https://doi.org/10.37868/sei.v7i2.id464>.
 - [6] P. Żochowski, R. Warchoń, M. Miszczak, M. Nita, Z. Pankowski, and M. Bajkowski, “Experimental and Numerical Study on the PG-7VM Warhead Performance against High-Hardness Armor Steel,” *Materials*, vol. 14, no. 11, Art. no. 3020, June 2021, <https://doi.org/10.3390/ma14113020>.
 - [7] J. C. Pao, C. A. G. Banglos, C. J. O. Salaan, and S. A. Guirnaldo, “Simulation and Strength Analysis of Flippable Mobile Ground Robot for Volcano Exploration and Monitoring Application,” *2022 IEEE 14th International Conference on Humanoid, Nanotechnology, Information Technology, Communication and Control, Environment, and Management (HNICEM)*, pp. 1–6, Dec. 2022, <https://doi.org/10.1109/hnicem57413.2022.10109285>.
 - [8] Y. Choi, S. Eon Lee, J.-W. Jung, and J.-W. Hong, “Collision mechanism of unmanned aerial vehicles onto glass panels,” *International Journal of Micro Air Vehicles*, vol. 13, Art. no. 17568293211060504, Jan. 2021, <https://doi.org/10.1177/17568293211060504>.
 - [9] K. R. Kota, T. Ricks, L. Gomez, J. E. de Monteros, G. Olivares, and T. E. Lacy, “Development and validation of finite element impact models of high-density UAS components for use in air-to-air collision simulations,” *Mechanics of Advanced Materials and Structures*, vol. 27, no. 13, pp. 1178–1199, May 2020, <https://doi.org/10.1080/15376494.2020.1740956>.
 - [10] J. Yu et al., “Numerical simulation of a UAV impacting engine fan blades,” *Chinese Journal of Aeronautics*, vol. 34, no. 10, pp. 177–190, Oct. 2021, <https://doi.org/10.1016/j.cja.2020.10.025>.
 - [11] T. P. Drumond, M. Greco, and C. A. Cimini, “Evaluation of increase weight in a wing fixed leading edge design to support UAS impact,” in *Proc. 10th Aerospace Technology Congress*, Stockholm, Sweden, Oct. 2019, pp. 71–80, <https://doi.org/10.3384/ecp19162008>.
-

-
- [12] F. Franke, U. Burger, and C. Hühne, “A novel reduced order model for drone impacts with aircraft structures,” *CEAS Aeronautical Journal*, vol. 14, pp. 381–412, 2023, <https://doi.org/10.1007/s13272-023-00646-1>.
 - [13] D. R. Lesuer, *Experimental Investigations of Material Models for Ti-6Al-4V Titanium and 2024-T3 Aluminum*, U.S. Department of Transportation, Federal Aviation Administration, Washington, DC, USA, Rep. DOT/FAA/AR-00/25, 2000.
 - [14] G. Kay, *Failure Modeling of Titanium 6Al-4V and Aluminum 2024-T3 with the Johnson-Cook Material Model*, U.S. Department of Transportation, Federal Aviation Administration, Washington, DC, USA, Rep. DOT/FAA/AR-03/57, 2003.
 - [15] F. Franke, U. Burger, and C. Hühne, “Models and methods for drone strike load predictions,” in *Proc. 32nd Congr. Int. Council Aeronaut. Sci. (ICAS)*, Shanghai, China, 2021, pp. 2955–2967.
 - [16] ANSYS, Inc., *ANSYS Explicit Dynamics Analysis Guide*, Release 2023 R1, Canonsburg, PA, USA, 2023. [Online]. Available: <https://ansyshelp.ansys.com/>. (Accessed: 2024).
 - [17] G. Olivares et al., *UAS Airborne Collision Severity Evaluation: Executive Summary—Structural Evaluation*. Wichita, KS, USA: National Institute for Aviation Research, Wichita State University, Jul. 2017.
 - [18] R. Chi, A. Serjouei, I. Sridhar, and G. E. B. Tan, “Ballistic impact on bi-layer alumina/aluminium armor: A semi-analytical approach,” *International Journal of Impact Engineering*, vol. 52, pp. 37–46, 2013, <https://doi.org/10.1016/j.ijimpeng.2012.10.001>.
 - [19] A. Rashed, M. Yazdani, A. A. Babaluo, and P. Hajizadeh Parvin, “Investigation on high-velocity impact performance of multi-layered alumina ceramic armors with polymeric interlayers,” *Journal of Composite Materials*, vol. 50, no. 25, pp. 3561–3576, 2016, <https://doi.org/10.1177/0021998315622982>.
 - [20] M. Ganesh Madhan, A. G. Sanjeev, and S. Piramasubramanian, “A comparative study of RPG missile modeling for radar cross section analysis at 10 GHz,” in *2018 International Conference on Applied Electromagnetics, Signal Processing and Communication (AESPC)*, Bhubaneswar, India, Oct. 22–24, 2018, pp. 104–108, <https://doi.org/10.1109/AESPC44649.2018.9033338>.
 - [21] D. R. Lesuer, G. J. Kay, and M. M. LeBlanc, “Modeling large-strain, high-rate deformation in metals,” *Lawrence Livermore National Laboratory*, Livermore, CA, USA, Rep. UCRL-JC-134118, 2001.
 - [22] E. Sahraei, R. Hill, and T. Wierzbicki, “Calibration and finite element simulation of pouch lithium-ion batteries for mechanical integrity,” *Journal of Power Sources*, vol. 201, pp. 307–321, Mar. 2012, <https://doi.org/10.1016/j.jpowsour.2011.10.094>.
 - [23] VMZ Sopot, “VP-7M fuze.” [Online]. Available: <https://vmz.bg/en/production/fuzes-igniters-and-pyro-delay-mechanisms/vzrivatel-vp-7m> (Accessed: Mar. 24, 2026).
-

-
- [24] T. Børvik, M. Langseth, O. S. Hopperstad, and K. A. Malo, “Ballistic penetration of steel plates,” *International Journal of Impact Engineering*, vol. 22, nos. 9–10, pp. 855–886, Oct. 1999, [https://doi.org/10.1016/s0734-743x\(99\)00011-1](https://doi.org/10.1016/s0734-743x(99)00011-1).
- [25] M. Kristoffersen, O. L. Toreskås, S. Dey, and T. Børvik, “Ballistic perforation resistance of thin concrete slabs impacted by ogive-nose steel projectiles,” *International Journal of Impact Engineering*, vol. 156, Art. no. 103957, Oct. 2021, <https://doi.org/10.1016/j.ijimpeng.2021.103957>.
- [26] M. Badea, D. G. Zisopol, V. Nastasescu, and A. Hadar, “A Research on the Impact of 20 mm × 10 mm Armor-Piercing Frangible Projectiles on Multilayer Armor,” *Engineering, Technology & Applied Science Research*, vol. 15, no. 5, pp. 26833–26837, Oct. 2025, <https://doi.org/10.48084/etasr.12971>.