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Numerical study of high-velocity impact on aeronautic carbon composites

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ABSTRACT

This paper presents research concerning the numerical analysis of two new carbon composite materials subjected to high-velocity impact. These new composites were produced using new out-of-autoclave processes, making them more environmentally friendly. An experimental campaign was conducted on these materials in order to determine the effects of a metal sphere during impact. A numerical model using ABAQUS explicit is assembled using shell finite element models, and a non-linear dynamic analysis is applied. For the constitutive relation of the material, a fiber-reinforced composite damage model from the ABAQUS library is used. This initial numerical model is then compared with the outputs of the experimental campaign in order to validate it. Next, a numerical campaign is performed to understand carbon composites response to the impact of a hailstorm strike, in which it is possible to verify that one of the specimens presented higher performance, by displaying less penetration and damaged area.

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Impact projectile; dynamic explicit analysis; composite orthotropic damage; penetration velocity; hailstorm strike

1. Introduction

Composite materials have become increasingly popular over the past few decades, and they are now heavily utilized in a variety of applications, such as the manufacturing sectors of aerospace [1], automotive [2], industry systems and components [3,4], generic modeling and application components [5], and maritime structure components [6,7]. These also comprise defence industry systems and components [3,4].

Engineers have largely embraced composite materials in the design of structures that have historically used metals for a variety of purposes. The mechanical characteristics of composites, such as their high strength and stiffness relative to weight, make them a desirable material for designers [8].

While composites have improved qualities over their parent materials, they are more susceptible to a greater range of damage and failure modes due to internal stresses created in the reinforcement or matrix [9,10]. Extensive testing is necessary to assess the dependability and forecast the behavior of the composites, especially in industries where structural failure could have disastrous effects.

Recent studies have demonstrated that to create an accurate and efficient framework for designing the next generation of aeronautical applications, numerical modeling across multiple length scales and laboratory testing to characterize material behavior and generate validation data are required [11]. In order to assist applications such as engine blade containment and occupant seat safety, aircraft designers and certification

authorities have been striving to employ dynamic finite element (FE) analysis more frequently [12].

To ensure that the FE model is built and used correctly, producing accurate results for the application of interest, it calls for meticulous model construction and checking. The procedures for modeling, simulation, and supplementary experiments should be preferably in line with the American Society of Mechanical Engineers [13] and the European Aviation Safety Agency's recommended adoption [14]. These verifications and validation processes aim to assure the validity of the numerical model and analytical results. In the end, it evaluates the acceptability and agreement between the simulation and experimental results. Applying these processes to the modeling of composite materials requires special attention to developing a strong methodology that may address unpredicted issues. The Federal Aviation Administration [15] acknowledges the significance of thorough verification and validation procedures. In a bottom-up method, the numerical model is calibrated at the lowest levels using statistically valid data from test coupons and elements. Higher degrees of analysis methodologies' capacity to forecast local strains and failure modes may be validated using detailed, subcomponent, and component tests.

For impact loads in aeronautical applications, where impact velocities and consequently impact energies can be very high, the dynamic response of these materials becomes crucial [16]. Impact testing is becoming an essential method used to evaluate how well composite materials behave precisely in real-world collisions. In parallel, FE modeling

approaches have grown in popularity for simulating impact testing of composites due to recent advances in software and an increase in computational resources available for software processing [17].

In experimental impact testing, a composite material is subjected to an impact at a predetermined distance, angle, and velocity. The resulting force and displacement are then measured [18,19]. Impact testing is carried out in a controlled setting, such as a test facility, using specialized impact and measuring equipment in accordance with safety regulations. To evaluate the composite material's resilience to damage and its capacity to absorb impact energy, data gathered from impact tests needs to be examined and evaluated [20,21].

Using characteristics and conditions identical to the real impact scenario, such as impact angle, velocity, and material properties, FEA non-linear tools [22] can be used to model the composite behavior and simulate the impact situation. The simulations may confirm that the results are essential for the study of the manufacturing process, material choice, and composite structure design.

The majority of composite structures in aeronautic applications are usually produced using autoclaves, and considering the high cycle times, capital investments, and tooling costs associated with autoclave processes, questions arise concerning the environmental and economic viability of composite materials [23–25]. In the last few years, there has been a concern about turning the production process of composites into autoclaves cheaper and hopefully more environmentally friendly. Because of this, it has become essential to develop and adopt solutions that are out-of-autoclave (OoA). Presently, the new generation of OoA processes is gaining acceptance as they now offer the possibility to obtain composite materials with equivalent autoclave quality [23]. These processes use vacuum, pressure, and heat outside of the autoclave to manufacture composites [26,27]. However, the OoA methods currently used to produce these structures are not fully optimized [23], and two variations of these are tested in this study.

1.1. Objectives

This paper proposes a FE modeling approach to simulate the high-velocity impact response of two carbon composite laminates manufactured *via* two OoA processes. The materials were developed within the scope of a research project dedicated to the study of more sustainable OoA manufacturing processes, enabling higher production rates of large-scale aeronautical structures. Due to no disclosure agreement, only essential mechanical properties of carbon composite laminates are present in this work, and their manufacturing names were omitted. The developed materials can be applied in impact zones, which can occur for various reasons, such as bird strikes or hail, for example. Therefore, the development of materials capable of withstanding such events will require tests to assess their resistance. A pneumatic cannon was used to perform impact resistance tests with spheres made of steel.

To complement these experimental tests, a numerical campaign is embarked on in which new material properties

and impact objects are tested to assess future design choices, using the commercial software ABAQUS explicit [28].

1.2. Research significance

This paper discusses research on the numerical analysis of two innovative carbon composite materials subjected to high-velocity impacts. After the finite element is validated, it will be used to numerically test the new carbon composites against a hailstorm strike using similar standard testing conditions provided by [29], to deliver some information regarding their performance.

2. Recent studies for impact on carbon composites

During the last few years, several new studies regarding experimental and numerical campaigns have been undertaken, to simulate the impact of objects on composite panels [30]. It is beyond the scope of this work to cite them all, but the most important for the given study are briefly mentioned.

Recently, the impact modeling of composite panels using a new damage model combined with viscoplasticity was proposed by [31] in order to better represent post-peak deterioration behavior, rate, and temperature dependent damage curves. The newly developed generalized three-dimensional orthotropic damage constitutive model was tested and compared with experimental data. Additionally, the erosion related parameters were easily calibrated.

To investigate the residual strength in compression and damage patterns following impact tests of quasi-isotropic IM7/8552 carbon fiber reinforced composite laminates, an extensive study was performed by [32], using the modeling technique in ANSYS LS-DYNA [33]. According to the authors, the advantages of using this software were due to the modeling approach already being validated and proven to work for simulations of transverse impact tests with impact energies ranging from 6 J to 20 J.

Sandwich composite panels with ultra-high molecular weight polyethylene composite facings and polyvinyl chloride polyethylene terephthalate foam cores were researched [34] to examine variations in their low-velocity impact response behavior. Furthermore, in order to forecast the intra- and inter-laminar damages of sandwich composite panels, a FEM including a cohesive law, crushable foam plasticity model, and Puck's failure criterion were assembled and verified.

Additionally, [35] employed new simulation methods based on high-order beam theories to predict the progressive damage assessments of composite laminates hit by impacts at low velocities. The results demonstrate the capacity of 2D models to appropriately evaluate damage mechanisms and are in good agreement with reference 3D finite element solutions with much lower computational costs. The emphasis is on the employment of higher-order 2D elements with layer-wise capabilities.

Other authors have chosen different composites [36] for ballistic applications and implemented numerical models for aramid composites. These were experimentally validated in

terms of permanent deformation, energy absorption, and damage mechanisms.

In order to clarify the failure mechanisms and damage behavior of asymmetric sandwich panels [37] developed a novel non-linear FE model, which was successfully validated by experimental data. Furthermore, a parametric analysis was conducted to investigate the mechanisms of failure of asymmetric sandwich panels that promoted penetration using different conditions and characteristics

Some authors [38] have offered both experimental and numerical results for composite honeycomb sandwich panels in order to look into how impact energy and structural factors affect the damage behaviors of composite panels that are subjected to localized low-velocity impacts.

In order to examine the impact responses of composite airframe structures and assess the capabilities of the commercial software ANSYS LS-DYNA [33], to simulate these responses, including damage initiation and progressive failure, researchers document a joint NASA-Sikorsky study [39]. It was possible to build and test two full-scale composite components and directly compare the expected failure modes from the experimental and numerical campaigns.

There are several possible factors that could affect air-plane parts, including hail and bird strikes. Using a multi-scale approach [40] demonstrated how to ascertain the mechanical behavior of composites and capture damage across various scales, from ply to laminate. The authors have shown in this study that, through numerical testing, homogenized characteristics may be obtained using three-dimensional representative volume elements.

In order to produce validation data for model repair patches, some researchers have used low-velocity laboratory impacts [41], to detect barely noticeable damages. The optimal patch shape is then determined by performing a parametric evaluation of the system's reaction to impactor speed and material using the FEM that was created with ABAQUS/Explicit [28].

3. Experimental database

3.1. Materials and specimen geometry

Two carbon composite laminates manufactured *via* different processes were considered. Each material was provided by different industrial manufacturers. Due to the lack of disclosure, the laminates are called SPEa and SPEb, and these presented different compactness during production. SPEa laminates were manufactured using a reinforcement constituted by a fabric of continuous carbon fiber and a thermosetting epoxy *via* Vacuum-Assisted Resin Transfer Molding with ply sequences of $[-45^\circ; +45^\circ]$ and $[0^\circ; 90^\circ]$. SPEb

laminates were manufactured using unidirectional composite prepreg *via* hot stamping process, composed of a highly toughened system and high viscosity thermosetting epoxy resin as the base, reinforced by continuous high-performance carbon fibers. The adopted material properties for the numerical models are presented in Table 1 for SPEa.

The primary difference between SPEa and SPEb was an increase in: (i) stiffness $E_1 = E_2 = 122\text{GPa}$, and (ii) fracture energy $G_{I,f,m} = 1.5\text{MPa}\cdot\text{mm}$. The material testing has shown that for the two specimens on the plane axis, the material characteristics were identical in both directions. Properties used in the numerical model were experimentally determined following standard testing and fully detailed in the report [42]. Four plates measured $300\times 300\times 2.936\text{mm}$ were manufactured, two for SPEa and two for SPEb. These were later tested under high-speed impact conditions using a compressed air cannon developed in house. Examples of projectiles used are shown in Figure 1a. The steel spheres are obtained from a ball bearing manufacturer thus have very high precision concerning size and weight, with a hardness greater than 60 HRC. For the present research, a steel sphere with a 19.05 mm diameter and a weight of 28.2 g was preferred. Figure 1b shows an example of a sabot. The present tests were carried out at speeds in the range of 76.74 to 176.34 m/s. Figure 2a depicts the air cannon used to perform the impact test campaign.

3.2. Test setup

A compressed air cannon (see Figure 2a) was used for the impact tests. This cannon has a single action compressed air device operating at a maximum pressure of 10 bar. The barrel has a smooth bore with a 108 mm diameter, configurable to 3- or 4-meter length. Driving power is supplied from a 150-litre pressure tank connected to the barrel by a large diameter fast response valve. An extruded polystyrene foam sabot that seals the barrel, and allows air pressure to be applied, drives the projectile. As sabot debris can produce unwanted impacts on the target panels as well as interfere with the speed measuring system, the muzzle is fitted with a spring loaded sabot catcher, which effectively reduces the bore to about 90 mm and the maximum projectile diameter to 75 mm. Exit speeds depend on the payload mass (projectile plus sabot) and pressure. So far, for payloads up to about 100 g with less than 5 bar (about half of full pressure), speeds between 60 and 250 m/s have been achieved.

The impacts were monitored by a ballistic chronograph placed in front of the target coupon to measure the impact speed and two high speed video cameras, one facing the impact point and the other the rear surface of the coupon Figure 2b. The rear camera was also used to measure the

Table 1. Material properties for the SPEa specimen.

$E_1 [\text{MPa}]$	$E_2 [\text{MPa}]$	$E_3 [\text{MPa}]$	$G_{12} [\text{MPa}]$	$G_{13} [\text{MPa}]$	$G_{23} [\text{MPa}]$	$G_{If} [\text{MPa}\cdot\text{mm}]$
74000	74000	74000	3100	761	761	0.22
$\nu_{12} [-]$	$\nu_{13} [-]$	$\nu_{23} [-]$	$X_t [\text{MPa}]$	$X_c [\text{MPa}]$	$Y_t [\text{MPa}]$	$G_{Im} [\text{MPa}\cdot\text{mm}]$
0.04	0.04	0.04	969	690	969	0.22
$Y_c [\text{MPa}]$	$Z_t [\text{MPa}]$	$Z_c [\text{MPa}]$	$S_L [\text{MPa}]$	$S_T [\text{MPa}]$	$\rho [\text{kg}/\text{m}^3]$	$\alpha [-]$
690	969	690	87	87	1.51	1.0

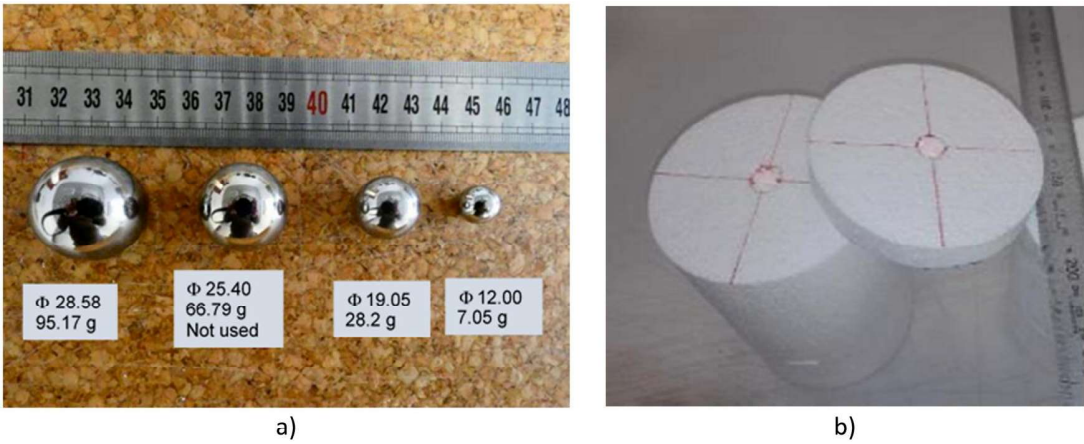


Figure 1. a) Used impact steel spheres; b) example of a typical sabot.

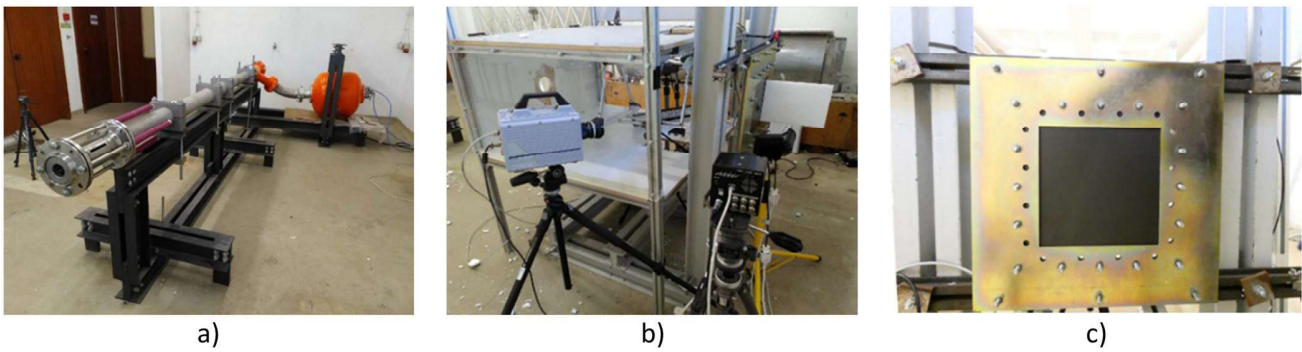


Figure 2. a) Compressed air cannon used to perform the impact tests; b) high speed video cameras and ballistic chronograph; c) steel frame to support the target panels.

Table 2. Summary of ballistic impact test results.

Coupon	Impactor		Perforation	Impact Velocity	Residual Velocity	Impactor Mass	Impact Energy	Residual Energy	Absorbed Energy	Visible Damage	
Name	Material	Diameter Mm	YES/NO	m/s	m/s	g	J	J	J	Impact side mm ²	Exist side mm ²
SPEa1	steel	19.05	YES	87	84.9	28.2	106.6	101.6	5	435.8	6763.6
SPEa2	steel	19.05	YES	143.7	132	28.2	291	245.7	45.3	448.3	7155.5
SPEa3	steel	19.05	YES	157	153.9	28.2	347.3	333.7	13.6	434.4	6976.1
SPEa4	steel	19.05	YES	176.3	163.5	28.2	438.5	376.9	61.5	465.4	5569.8
SPEb1	steel	19.05	YES	76.7	65	28.2	83	59.5	23.5	393.4	1201.4
SPEb2	steel	19.05	YES	137.6	125.7	28.2	267	222.8	44.3	450.1	3148.5
SPEb3	steel	19.05	YES	169.2	158.9	28.2	403.8	356.1	47.7	478.7	4355
SPEb4	steel	19.05	YES	173.7	155.7	28.2	425.3	341.8	83.5	458.4	2829.2

exit velocity in the case of coupon perforation, and the front camera was used to estimate the exit velocity when no perforation occurs and the projectile rebounds from the coupon surface. The test coupons were firmly gripped between two steel frames, which were bolted to a rigid steel supporting structure (Figure 2c). The high speed video cameras were initially calibrated using classical intrinsic and extrinsic tests and parameters. The ballistic chronograph was also calibrated using standard guidelines provided by the user's manual.

3.3. Experimental results

A total of eight tests were performed (four for each material). A summary of the main results and the observable

damage by comparing images before and after the damage is presented in Table 2. Although both materials present similar entry damage on the impact face, the SPEa material exhibits considerably higher damage on the exit face (see Figure 3), probably resulting from the lower compactness produced by its manufacturing process. Figure 4 shows examples of the damage observed on the impact and exit faces of the SPEb specimen.

The resistance to ballistic impact is usually analyzed and compared using the V_{50} parameter, basically the impact velocity at which there is a 50% probability of target perforation, often called the ballistic limit. For the first analysis in Figure 5a it is depicted a comparison of the damaged area on the exit face as a function of absorbed energy. Figure 5b shows the residual velocity as a function of impact velocity.

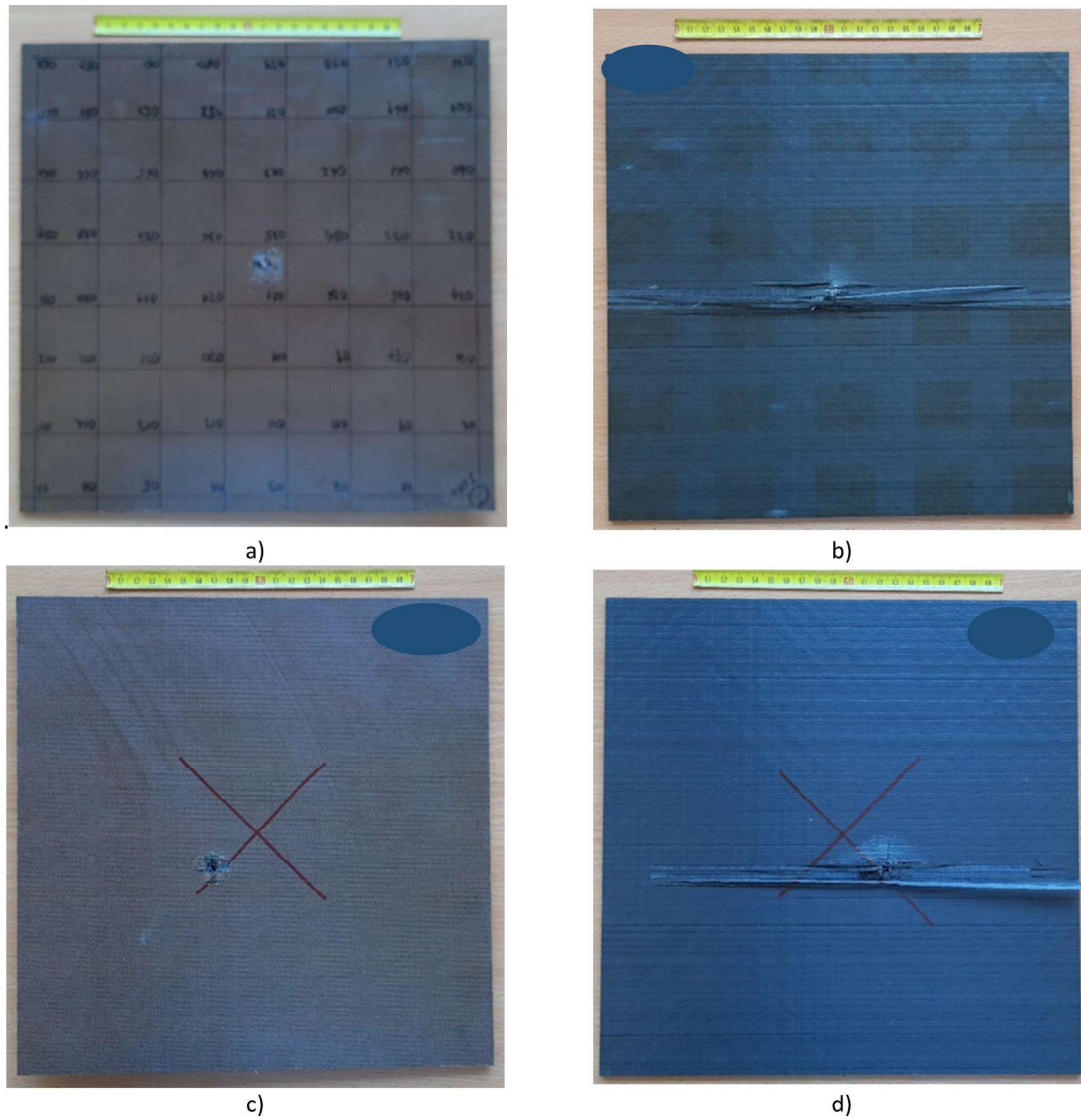


Figure 3. Front and rear view of panels SPEa after test. a) Front view 87 m/s; b) rear view; c) front view 176.3 m/s; d) rear view.

Quite surprisingly, both materials exhibit very similar behavior, with SPEb material marginally superior. It is important to point out, that in this test the projectile was very rigid and did not collapse during the impact. It is not possible to extrapolate these conclusions for flexible projectiles that may collapse during the impact. A simple log fit of the data would predict a V_{50} of 40 to 45 m/s for both materials. The limited number of tests and different impact velocities impair a more refined analysis, however, a non-linear fit of the data using the well-known Lambert-Jonas model (1), would estimate a V_{50} of 35.1 and 39.1 m/s for the SPEa and SPEb materials, respectively.

$$V_{res} = \alpha(V_i^{1/p} - V_{50}^{1/p})^{1/p} \quad (1)$$

In which V_{res} is residual velocity, V_i the impact velocity, the assumed V_{50} ballistic limit, and the adjusted α, p fitting coefficients. As expected, the SPEa material had a worse performance than the SPEb from vacuum infusion, nevertheless, both materials seem somewhat weak for protection against high velocity impacts.

4. Modeling approach

4.1. Model geometry and boundary conditions

Due to the rigid fixed supports used in the experimental campaign Figure 2c, a specimen of $240 \times 240 \text{ mm}^2$ was adopted for the numerical model. In the case of the carbon composite specimen, a fixed support was used on all four sides for all directions and rotations. Due to the spheres being at a given velocity, no imposed displacement restrictions were adopted. It was not used symmetry, to reproduce the real testing conditions due to the lack of damage symmetry during penetration tests Figure 6. For the steel spheres used as impact objects, these were simulated and admitted as full dense 3D solid elements.

4.2. Impose velocities

Since it was admitted the carbon composite specimen was fixed, this one needed no impose velocity. However, for the steel sphere, it was admitted initially two initial velocities,

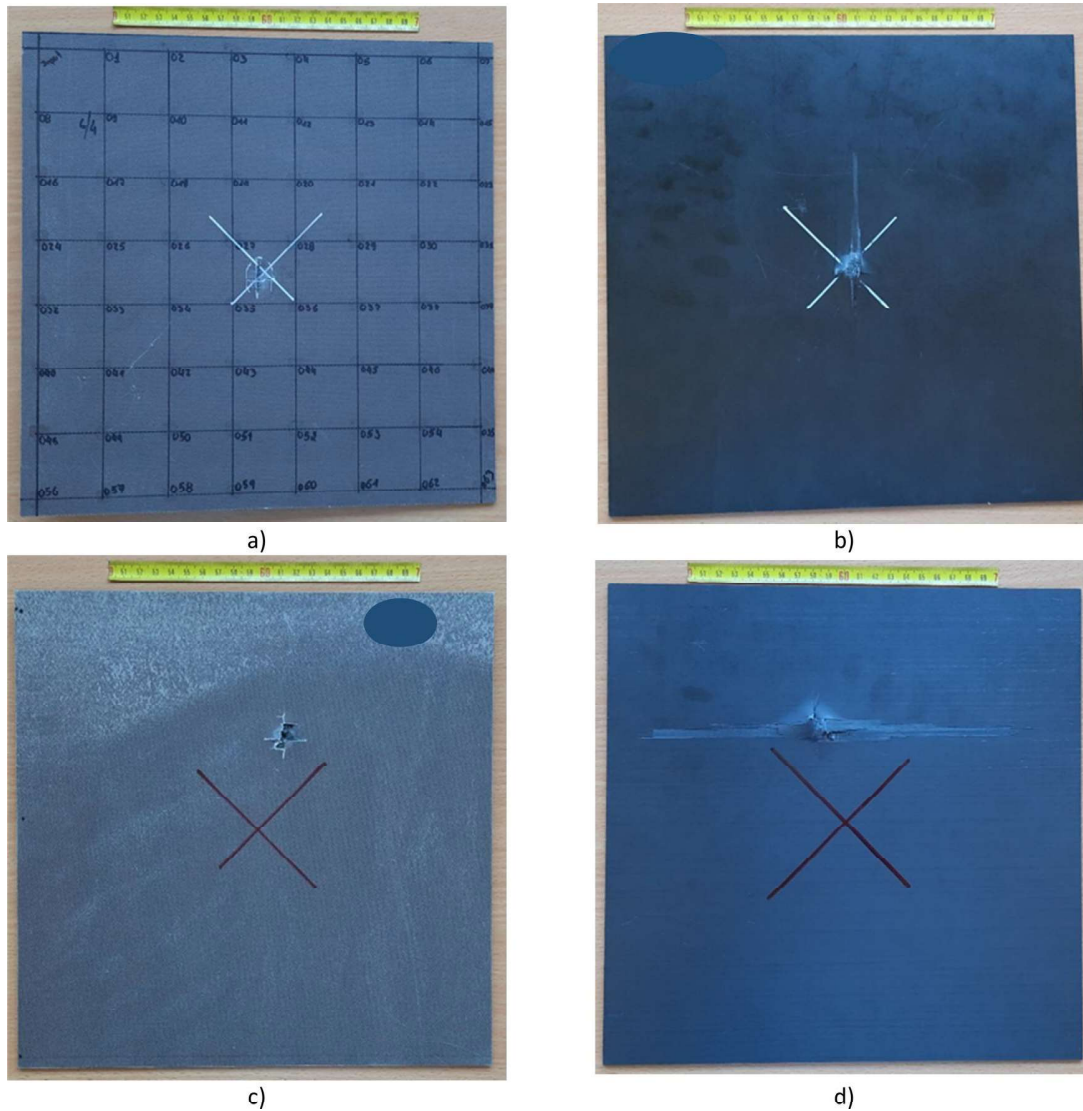


Figure 4. Front and rear view of panels SPEb after test. a) Front view 76.7 m/s; b) rear view; c) front view 173.7 m/s; d) rear view.

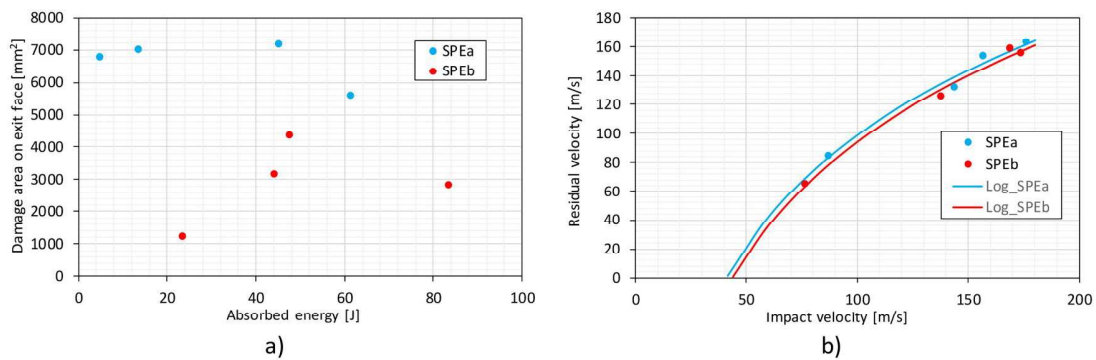


Figure 5. a) Comparison of damaged area on the exit face as a function of absorbed energy; b) residual velocity as a function of impact velocity for all tests performed.

173 m/s and 87 m/s, in the z direction with the vector pointing to the carbon composite plane specimen. For the other directions and rotations, no velocity or rotation were imposed. The velocity was imposed on all the nodes of the steel sphere as an initial, predefined field.

4.3. Interface properties

For the contact between the steel sphere and carbon composite, a classical interface with pressure and friction was chosen. For the normal behavior, classical “hard” contact was used, in combination with shear friction with a

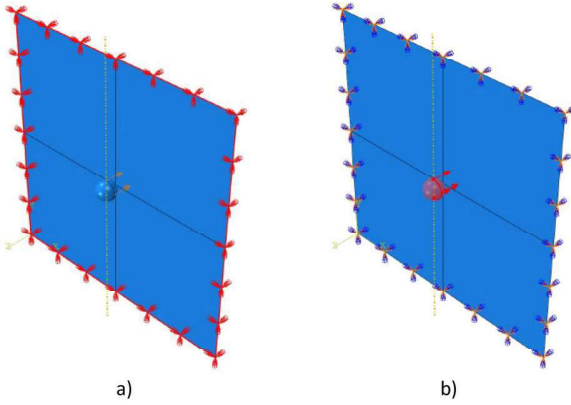


Figure 6. a) Admitted boundary conditions for the carbon composite shell specimen, with all displacements and rotations constrained, b) imposed velocities in the domain of the steel sphere.

coefficient of 0.4. For the surface contact, the coordinates of the real thickness of the carbon composite were used with the steel sphere. Since an explicit solver is being used, a general contact surface was employed, meaning every node of both surfaces can be in contact even after deformation with and between them.

4.4. Material properties and constitutive relations

For the steel spheres, an average material property was adopted: $E_s = 210\text{GPa}$; $\nu = 0.2$; $f_y = 500\text{MPa}$. For the carbon composite, it was admitted the orthotropic elastic, resistance and toughness describe before in Table 1. For the steel, the classical Von-Mises plasticity with no hardening was used to simplify the non-linearity in the spheres. Although it was observed that there were no plastic strains in the steel spheres in all models. For the carbon composite the classical Hashin failure criterion was adopted, using classical MTL orthotropic damage evolution [43]. It is beyond the scope of this work, but a full detail and description of this damage formulation and its application for progressive damage modeling in fiber-reinforced materials can be found in [8, 44,45]. This failure criterion has successfully simulated orthotropic damage evolution in composites [46,47] using crack band theory.

4.5. Type of analysis

In order to simulate the fast dynamic impact of colliding objects, it is preferable to use explicit step-by-step time integration algorithms [48], in which no iterative procedure is necessary. For this reason, ABAQUS explicit solver with an automatic stable time increment was adopted to achieve a stable solution. Due to the existence of high strains and displacements that occurred mainly in the carbon composite shells after the penetration of the spheres, geometrically non-linear analysis is adopted. The total time period used for the explicit analysis is 0.35 milliseconds, which is more than twice the experimental measured value post penetration.

4.6. Numerical stabilizations

4.6.1. Automatic time stabilization

For the application of automatic time stabilization, the default value of global estimation with the improved Dt method was used. This type of analysis starts by using the element-by-element estimation method. It is then switched to the global estimation method once the algorithm determines that the accuracy of the global estimation method is acceptable. The adaptive, global estimation algorithm determines the maximum frequency of the entire model using the current dilatational wave speed.

The stable time increment is the minimum time that a full dilatational pressure (expansion and contraction) wave takes to move across any element in the model. Based on the element-by-element estimate, the stability limit can be redefined using the element length, L_e , and the wave speed of the material, c_d (2). It can be shown that the highest element frequency determined on an element-by-element basis is always greater than or equal to the highest frequency in the assembled finite element model [49].

$$\Delta_{stable} = \frac{2}{f_{max}} \quad (2)$$

In which $\dot{\lambda}$ and $\dot{\mu}$ are the effective Lamé's parameters (3).

$$\Delta_{stable} \leq \frac{L_e}{c_d} = L_e \sqrt{\frac{\rho}{\dot{\lambda} + 2\dot{\mu}}} \quad (3)$$

For an isotropic elastic material with null coefficient of Poisson it can be rewritten as (4):

$$\Delta_{stable} \leq \frac{L_e}{c_d} = L_e \sqrt{\frac{\rho}{E}} \quad (4)$$

$$\Delta_{stable} \leq \begin{cases} \Delta t_{FRP} = 1.0 \sqrt{\frac{1.5}{74}} \times 10^{-6} = 1.42 \times 10^{-7} s \\ \Delta t_{steel} = 1.0 \sqrt{\frac{7.8}{210}} \times 10^{-6} = 1.93 \times 10^{-7} s \end{cases} \quad (5)$$

$$\Delta_{stable} \leq L_e \sqrt{\frac{\rho}{E(c_{ij}^p, d)}} \left(\sqrt{1 + \xi_{max}^2} - \xi_{max} \right) \quad (6)$$

Due to the material properties adopted for carbon composites and steel, for a unitary square finite element of 1.0mm, the stable time increment must be below 2×10^{-7} seconds (5) and (6). This value may even be lower, if damping is applied for the material or if the stiffness decreases (in cases of plasticity and/or damage).

4.6.2. Viscosity stabilization

In this study, two types of viscosities were used to allow some numerical stabilization during high rate volumetric strain. Their purpose is to improve the modeling of high-speed dynamic events.

The first was linear bulk viscosity pressure p_{bv1} , which is applied to all elements and is introduced to dampen the ringing effect at the highest element frequency. This

damping is sometimes referred to as truncation frequency damping, which is a function of the volumetric strain rate $\dot{\epsilon}_{vol}$. It generates a bulk viscosity pressure that depends linearly on the volumetric strain rate. The default value for the damping coefficient $b_1 = 0.06$ was adopted (7).

$$p_{bv1} = b_1 \rho c_d L_e \dot{\epsilon}_{vol} \quad (7)$$

The second was the quadratic bulk viscosity pressure p_{bv2} , which is applied only to solid continuum elements. It depends quadratically on the volumetric strain rate $\dot{\epsilon}_{vol}$, and the default value of the damping coefficient $b_2 = 1.20$ was adopted. Its purpose is to smear the shock over several elements (8).

$$p_{bv2} = \rho (b_2 L_e \dot{\epsilon}_{vol})^2 \quad (8)$$

The purpose of adding the quadratic bulk viscosity pressure is to keep elements from collapsing under very high velocity gradients by spreading a shock front across multiple elements.

For example, admitting in a 1D one-element problem where the nodes are fixed on one side and at the beginning velocity is in the direction of the fixed nodes on the other side. Without the quadratic bulk viscosity, the element would collapse to zero volume in a single time increment if the beginning velocity equaled the material's dilatational wave speed. This is because the stable time increment size is exactly the transit duration of a dilatational wave across the element. The element won't collapse because of the resistive pressure that the quadratic bulk viscosity pressure will introduce.

Since the bulk viscosity pressure is solely meant to have a numerical impact and is not thought to be a component of the material's constitutive relation, it is not included in the material point stresses. The dilatational mode of each constituent serves as the basis for the bulk viscosity pressures, in which for every element, the dilatational mode's fraction of critical damping is provided by (9).

$$\xi = b_1 - b_2^2 \frac{L_e}{c_d} \min [0, \dot{\epsilon}_{vol}] \quad (9)$$

No material damping was used since the natural damping of carbon composite and steel is low. Also, their consideration would provide lower stabilization time increments. Although material damping can reduce the noise in the first

numerical derivatives, it will always reduces the stability limit in ABAQUS explicit solver. This is contrary to engineering intuition, and has been pointed out in previous research work [50].

4.6.3. Mass scaling stabilization

Since this is a full dynamic problem, the authors chose to use no mass scaling since this could alter the true dynamic response due to contradicting the physical nature of the material. This increased the computational cost of the total time analysis.

4.7. Adopted mesh

For the carbon composite, a shell element with 4 nodes and full integration S4 was used, using 7 Simpson integration points in the thickness Figure 7 a and b. The size of the mesh varies between 9.6, 4.8 and 2.4 mm (less than 1/20 of the boundary perimeter), in order to test mesh dependency [51], and guaranteeing it does not influence the structural response. It was also admitted that there is the possibility of element deletion for damages that range from 0.1 to 0.99. For the carbon composite, two extra types of finite elements were also used, to assess the mesh dependency: (i) the 4 node element S4R with reduced integration; (ii) the 3 node element S3R with constant strain field, which is a degenerated version of S4R. For the steel sphere, to improve the contact of the ball curvature with the carbon composite plates, the element 10 node modified quadratic tetrahedron (C3D10M) was adopted, with a maximum element size of 2.1 mm Figure 7c. Finite membrane deformations were admitted, but with no second order accuracy since no real revolute was observed during the experimental campaign.

5. Numerical model validation

In this section, the first results are presented in order to validate the numerical model, by providing proof of no mesh dependency or sensitivity. Due to the heavy computations, it was decided to use double precision in all the explicit solver computations, but the nodal output saved only single precision results, to reduce the final size of the output data base file. The numerical validation was performed by verifying

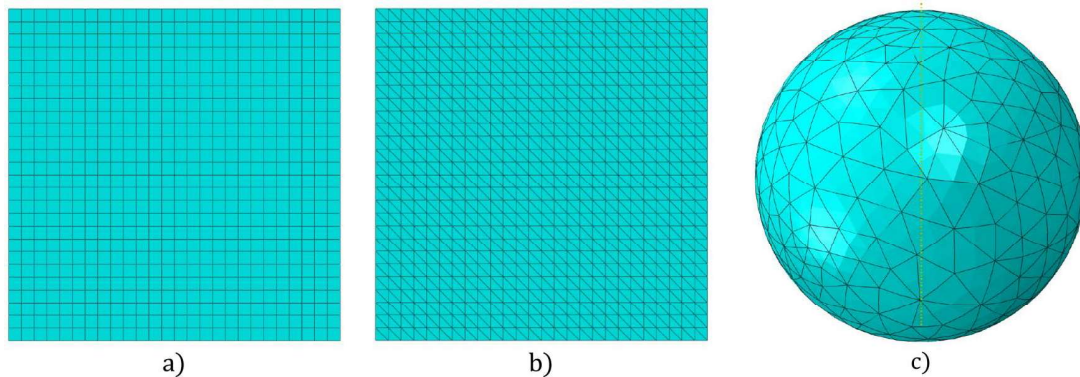


Figure 7. Mesh discretization of 9.6 mm, with a) S4 and S4R elements and b) S3R elements. c) C3D10M elements with 2.1 mm discretization.

the stabilization of the residual speed after penetration for all models during the $-h$ mesh refinement.

5.1. Assessing meshes

For the mesh dependency, $-h$ refinement was undertaken, in which the size of the FE was reduced and it was verified it didn't affect the structural response. For the mesh sensitivity the order of integration and the type of elements were changed and again, it was verified that there was a very low change in the structural response.

For the steel sphere, a maximum element size of 2.1 mm was chosen for all tests, which is a value smaller than the

maximum allowed element size in the carbon composite mesh. From Figure 8 to Figure 10, the velocity variation of the sphere for the carbon composite shell with sizes 9.6, 4.8 and 2.4 mm is presented. These numerical tests were accomplished with the maximum velocity used in the compressed air cannon of 176 m/s. Due to the existence of noise during the numerically measured velocity, a structural response using a moving average for a period of 0.05 s is used, to better understand the difference in the velocity outputs during the time analysis. For these tests, it was admitted that the maximum allowed damage was 0.9 before the element was deleted from the model during the incremental analysis.

Through Figures 8–Figure 10, it can be observed that for all three $-h$ refinements the final value of the residual

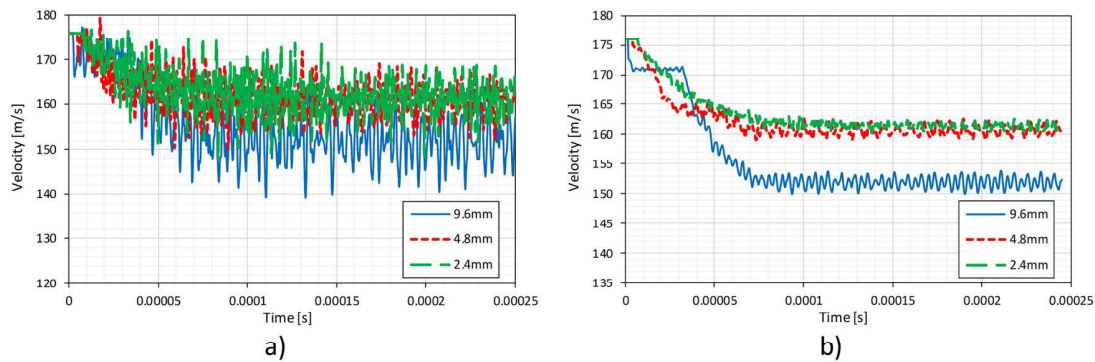


Figure 8. a) Sphere velocity during the time analysis; b) Sphere moving average velocity during the time analysis. Results for $-h$ refinement for S4 finite element in the carbon composite.

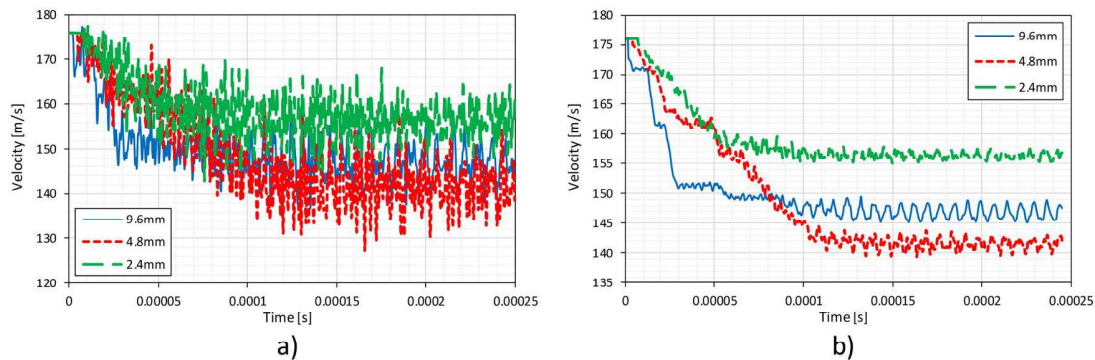


Figure 9. a) Sphere velocity during the time analysis; b) Sphere moving average velocity during the time analysis. Results for $-h$ refinement for S4R finite element in the carbon composite.

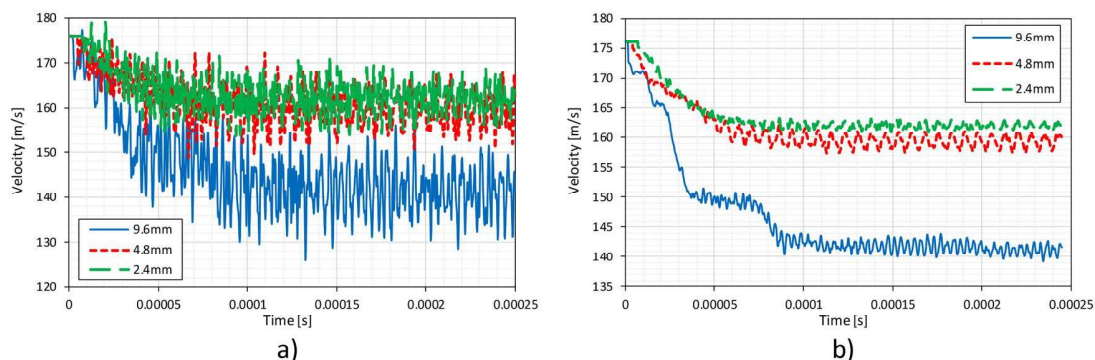


Figure 10. a) Sphere velocity during the time analysis; b) Sphere moving average velocity during the time analysis. Results for $-h$ refinement for S3R finite element in the carbon composite.

velocity after penetration tends to stabilize, therefore, it is not expected to have mesh dependency. In Figure 11, it's presented as the best approximation for the S4, S4R and S3R with the maximum shell mesh size of 2.4 mm. As expected, the S4 and S3R both with full integration, presented similar residual speeds after penetration for the same mesh size. However, the S4R, with reduced integration, presented a lower residual velocity after the penetration of the carbon composite. This may be due to different characteristic length calculations when the element is switched from full to reduced integration. Although there is no mesh dependency during the $-h$ refinement, when changing the integration procedure in the shell plane, some mesh sensitivity is expected.

It is depicted in Figure 12, the final shear damage evolution, which takes into account the combination of fiber and matrix damage from Hashin failure criterion. Element S4 and S4R present similar damage distribution in the vertical direction, just like it was observed in the experimental campaign in 3.3. For the S3R element, the damage distribution is no longer elongated in the vertical direction but more localized around the sphere penetration perimeter.

5.2. Assessing maximum damage

Another influential aspect of the mesh dependency is the maximum allowed damage before the element is deleted. This one can indeed have some impact on the absorbed fracture energy, during the damage process. In any case, with damage varying from 0.1, 0.3, 0.7, 0.9 to 0.99 when

using the Q4 element for the carbon composite shell, there was no abundant variation in the final residual velocity, since 0.99 and 0.9 values presented the upper and lower bound respectively Figure 13.

When comparing the final shear damage distribution, there is a big difference when adopting lower maximum allowed damage (Figure 14a). For example, with $d_{\max} = 0.1$, the damage as expected is lower, and the concentration of damage is prescribed around the contact surface of the sphere. However, for maximum allowed damage between $d_{\max} = 0.7$ and $d_{\max} = 0.9$ (Figure 14c) and Figure 12a) respectively), the final shear damage distribution is more widespread. Although, for values of $d_{\max} = 0.99$, the more concentrated final damage final distribution is again recovered, which does not align with what was seen in the experimental campaign.

According to recent research, the correct value of maximum allowed damage before element deletion, may be dependent on the adopted constitutive relation (plasticity or damage) and on the type of material [52,53]. There is no clear value in the scientific community, about the correct value adopted for maximum allowed damage before total finite element failure, but for this work, it was admitted, that after an element loses 90% of its stiffness, it is no longer viable to have resistance, therefore, the maximum allowed damage adopted for this work is 0.9. This value is indeed acceptable, since the upper and lower bound of the final residual velocity is achieved for values between 0.9 and 0.99. For these reasons, it is adopted for the rest of the numerical campaign to use $d_{\max} = 0.9$.

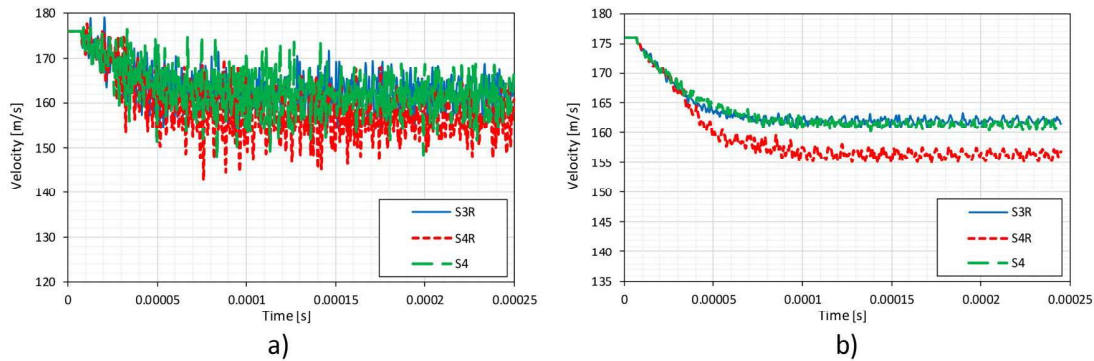


Figure 11. a) Sphere velocity during the time analysis; b) Sphere moving average velocity during the time analysis. Results for S4, S4R and S3R finite element with a maximum size of 2.4 mm.

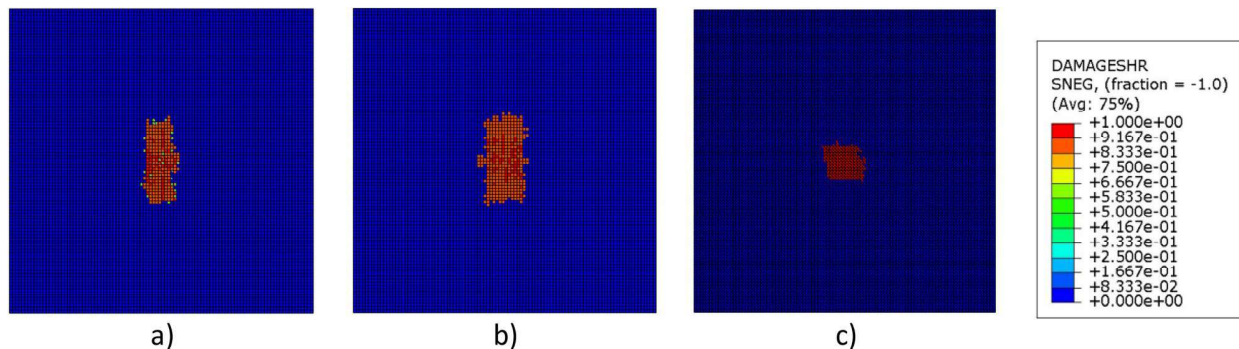


Figure 12. Final shear damage distribution after the full penetration in the undeformed shape: a) Element S4; b) element S4R; and c) element S3R.

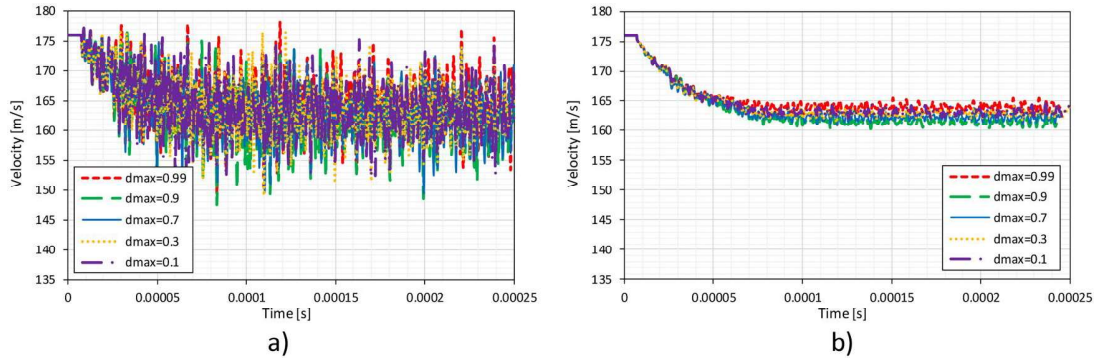


Figure 13. a) Sphere velocity during the time analysis; b) Sphere moving average velocity during the time analysis. Results for S4 with a maximum size of 2.4 mm using different maximum allowed damage.

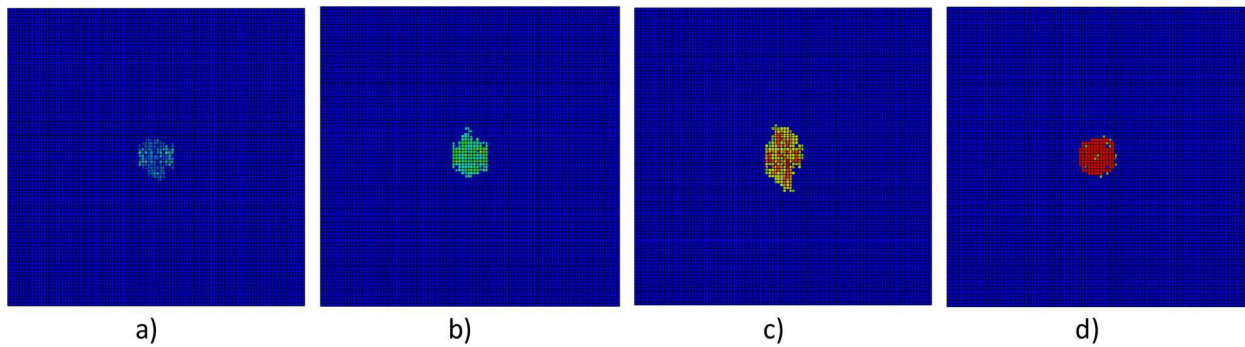


Figure 14. Final shear damage distribution with a) $d_{\max} = 0.1$, b) $d_{\max} = 0.3$, c) $d_{\max} = 0.7$ and d) $d_{\max} = 0.99$.

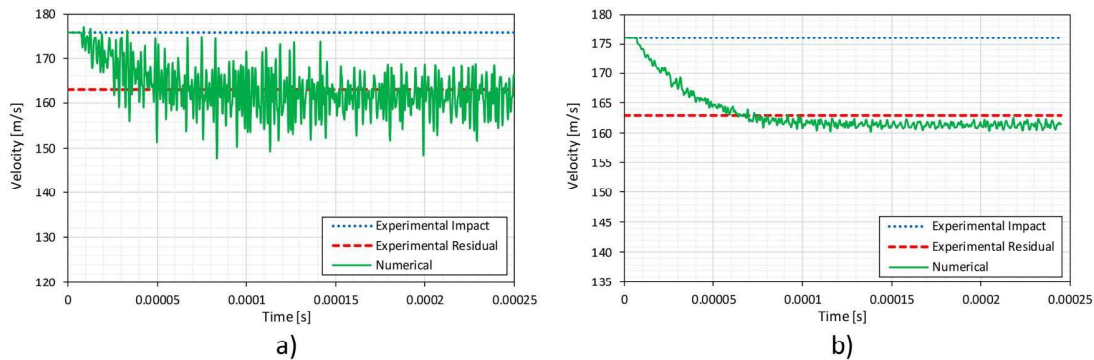


Figure 15. a) Sphere velocity, b) Sphere moving average. Experimental velocities before and after penetration compared with numerical velocity for SPEa at 176 m/s.

6. Numerical validation

Due to the final shear damage distribution and good stable residual velocity after penetration during the model validation, only the S4 element will be used to compare with the results from the experimental campaign for the SPEa.

It is possible to observe in Figure 15 that the numerical velocity is near the upper and lower bond of the experimental measured velocity, which are related to the impact and residual velocity before and after penetration, respectively. This is mainly valid for a higher impact velocity of 176 m/s for the SPEa. For lower impact velocities of 87 m/s the numerical model presents a lower residual speed (Figure 16).

Although it was not possible to observe the full deformation during the penetration process, it is depicted in Figure 17 that the numerical model is able to correctly present the

deformed shape with large displacements and element deletion. It is therefore reasonable to assume that the numerical model is indeed validated and capable of reproducing the structural results of SPEa, when the carbon composite endures the impact of different projectiles at high speed.

For the SPEb, some of the values in Table 1 are similar. The main difference is the increase in: (i) stiffness $E_1 = E_2 = 122 \text{ GPa}$; and (ii) fracture energy $G_I = 1.5 \text{ MPa}\cdot\text{mm}$. These are approximately equal for both directions in the plane. The conclusions for the accuracy of the numerical results for SPEb in the case of high and low impact velocities are the same as the ones for SPEa. This can be observed in Figures 18 and 19, in which there are some differences for the residual velocities and lower initial impact velocities.

One important conclusion is that all numerical models presented penetration for either high or low impact speeds,

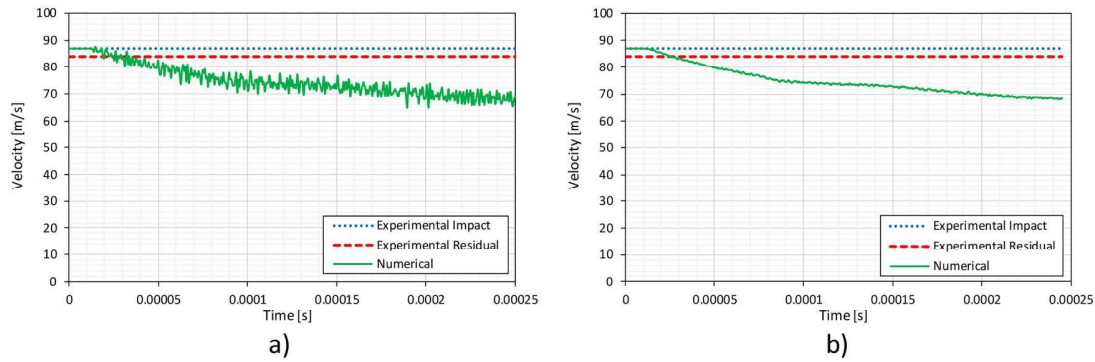


Figure 16. a) Sphere velocity, b) Sphere moving average. Experimental velocities before and after penetration compared with numerical velocity for SPEa at 87 m/s.

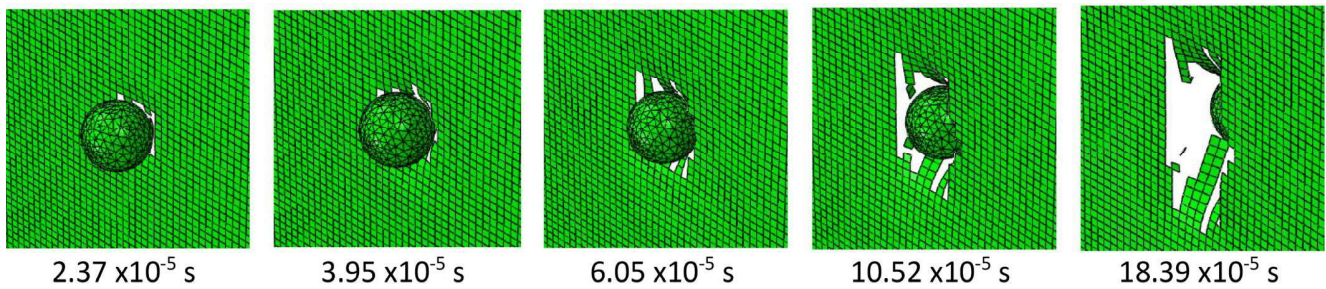


Figure 17. Sphere penetration during the time analysis for SPEa.

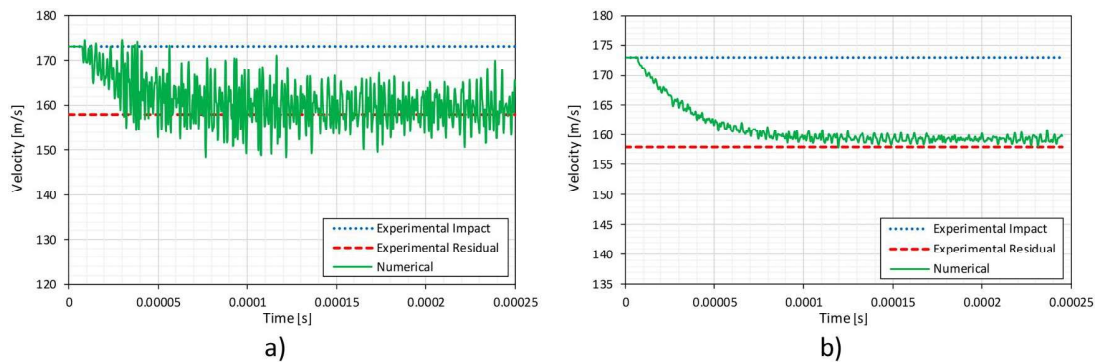


Figure 18. a) Sphere velocity, b) Sphere moving average. Experimental velocities before and after penetration compared with numerical velocity for SPEb at 173 m/s.

Table 3. Residual velocities after penetration of the carbon composite specimens.

Plate Specimen	Residual Velocities [m/s]	
	Numerical	Experimental
SPEa	161	163
SPEb	159	158

independent of the carbon composite manufacture being SPEa or SPEb, which aligns with what was observed in the experimental campaign. Also, for both SPEa and SPEb, for lower impact velocities, the residual velocity takes longer to stabilize, it is therefore expected, that for values below these, there will be a low possibility of penetration. Another important remark, is that SPEb presented less damage area, and the residual speed for lower impact velocities took longer to stabilize [Figure 19b](#)), meaning its performance is indeed superior to that of SPEa. In [Table 3](#) the values of the residual velocities after penetration for both SPEa and SPEb

specimens are depicted. It is possible to state that these are similar for experimental measured values and numerical models.

7. Evaluation for hailstorm strike

For this section, both materials, SPEa and SPEb, are numerically tested using different impact bodies, with the same specimen and test setup geometry. The main purpose of these tests, is to assess the impact efficiency of the carbon composite materials on real world scenarios of hail impact.

For the simulation of hail, it is used as an equivalent of an ice sphere. Although for low velocity impacts, ice does present some ductility, for high velocity impacts, the material becomes brittle [\[54\]](#). For this reason, ABAQUS brittle cracking with fracture energy regularization is used to simulate the constitutive relation of the ice.

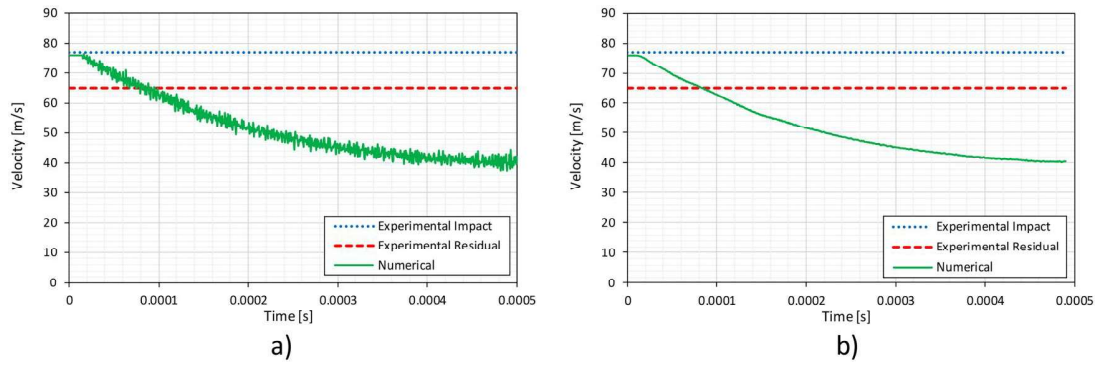


Figure 19. a) Sphere velocity, b) Sphere moving average. Experimental velocities before and after penetration compared with numerical velocity for SPEb at 76 m/s.

This model is useful in applications where the brittle behavior dominates, and it is adequate to assume that the material is linear elastic in compression. A simple Rankine criterion is used to detect crack initiation, in which a crack forms when the maximum principal tensile stress exceeds the tensile strength of the brittle material. The cracking process in this model is irreversible, meaning that once a fracture starts, it remains for the duration of the numerical computation. Nonetheless, the normal directions of the crack surface may cause cracks to close and reopen. This model uses directly Mode I cracking and indirectly Mode II due to shear cracking, using the shear retention model.

The elasticity of ice presents strong variations depending on the ambient testing temperatures and load frequency, ranging from 3.0 GPa to 11.2 GPa [55,56]. In the case of the Poisson coefficient, the disparities are also considerable, fluctuating from 0.29 to 0.32. For the maximum tensile stress, the variability is very high, ranging from 0.7 MPa to 3.1 MPa as reviewed by [56]. In the case of mode I fracture energy, the unpredictability is even higher, as recent studies [57] provided values from 1.0 N/m to 8.2 N/m.

Due to the high variability of the material properties for the ice, average values were used: for the Young's modulus elasticity and Poisson coefficient, it was adopted $E_{ice} = 7.1 \text{ GPa}$ and $\nu = 0.305$; tensile resistance $f_{t,ice} = 1.9 \text{ MPa}$; tensile fracture energy $G_{I,f} = 4.6 \text{ N/m}$; and a standard ice density of $\rho = 917 \text{ kg/m}^3$.

For the hailstorm strike testing, guidelines will be adopted similar to the standard [29], in which an ice sphere of 25 mm and 50 mm diameter is used with an impact speed of 180.6 m/s and 97.2 m/s respectively. Due to element deletion of the hail sphere because of cracking, when using general contact formulation for explicit analysis, it is necessary to generate an interior surface automatically, to take into account exposed faces of interior finite elements that have not failed. This was performed using a static interior surface for the finite elements of the hail sphere. With this method, the surface definition includes all faces of the specified elements that are not on the model's outer surface. The automatic production of an inner surface is comparable to generating a surface with all of the elements' faces and then deleting their free surfaces.

In both numerical models, the ice balls will collapse/explode for SPEa and SPEb at an impact velocity of

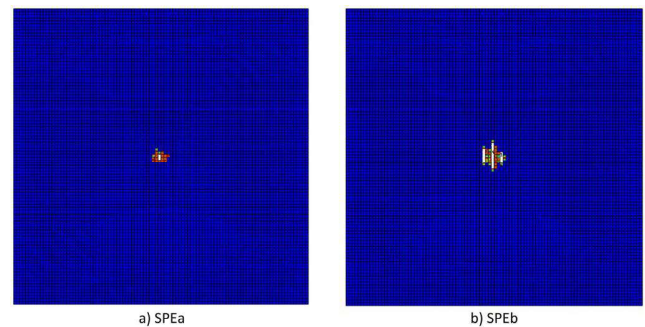


Figure 20. Final shear damage distribution, and element deletion for an ice ball with 25 mm of diameter at an impact velocity of 180.6 m/s.

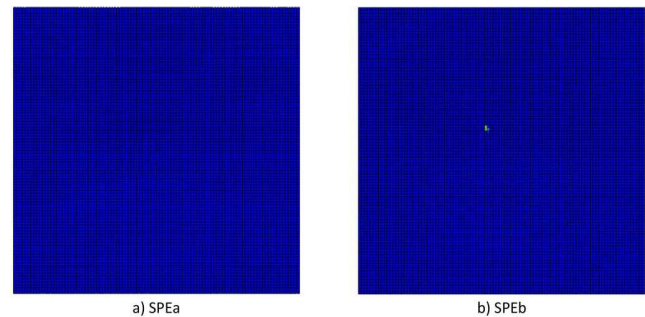


Figure 21. Final shear damage distribution, and element deletion for an ice ball with 50 mm of diameter at an impact velocity of 97.2 m/s.

180.6 m/s (diameter of 25 mm), but the last one presented more visible damage [Figure 20](#). Due to the higher stiffness in the SPEb specimen, more elements were deleted due to the damage being higher than 0.9 [Figure 20b](#)). Even though both presented some element deletion, none of the ice elements penetrated the specimens. For the ice ball, with an impact velocity of 97.6 m/s (diameter of 50 mm) the SPEa presented no damage after the explosion of the ice ball. The SPEb presented some minor damage due to the debris of the ice ball after its collapse/explosion, but no element deletion was observed [Figure 21b](#)).

In [Figure 22](#), structural responses, for the dynamic impact force and measured displacement in the carbon composite plate are presented, once the hail sphere collapses/explodes due to cracking. The SPEa specimen presented lower stiffness as expected in [Figure 22a](#)), resulting in higher

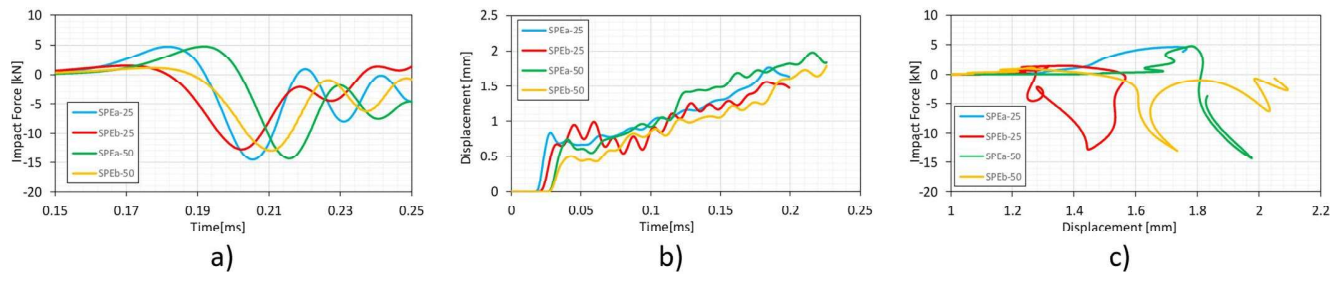


Figure 22. Dynamic structural response: a) impact force vs time; b) mid span displacement in the plate vs time; c) impact force vs mid span displacement in the plate.

Table 4. Maximum displacement in the carbon composite plate after the ice sphere collapsed.

Hail Sphere	Maximum Displacement [mm]	
	SPEa	SPEb
25mm	1.76	1.57
50mm	3.21	2.48

Table 5. Maximum and minimum impact force measured.

Hail Sphere	Maximum Force Impact [kN]		Minimum Force Impact [kN]	
	SPEa	SPEb	SPEa	SPEb
25mm	4.69	1.52	−14.55	−12.76
50mm	4.72	3.66	−14.39	−12.99

measured mid span displacements Figure 22b). Regarding the constitutive relation in terms of impact force vs displacement, it is not possible to assert using Figure 22c), which one presents higher energy dissipation. Nevertheless, it can be stated that SPEb, due to higher damage, may have indirectly increased energy dissipation.

In addition, by analyzing Tables 4,5 it is possible to state that, generally, the impact on SPEa presents a higher mid span displacement, but as observed in Figures 19 and 20, the level of final damage distribution is lower. This is mainly due to the lower stiffness of SPEa. Another important conclusion is that SPEa was able to withstand higher impact force loads in absolute terms, avoiding higher damage.

Using both of these tests, it was concluded that a hailstorm strike may not produce penetration in the carbon composite specimens, but some final damage distribution occurs for both specimens, and some repair is expected for these materials. Also from Table 5, the absolute maximum impact force is lower in SPEb, therefore, there may be some higher energy dissipation, as previously stated.

8. Conclusions

In the present work, a numerical study was undertaken to inquire about the penetration speed of steel spheres in two different carbon composite specimens. An experimental results database was used to calibrate and validate the adopted material constitutive relations, which would be used in a non-linear explicit analysis. Next, a numerical campaign was carried ou, to test the performance of the carbon composite specimens when subjected to the action of a hailstorm strike, using the standard testing conditions provided by

ASTM. It was concluded from the numerical campaign that a hailstorm strike may produce some damage to the carbon composite specimens, but no penetration was observed for both specimens. It was also observable in the numerical campaign of the hailstorm that the SPEb carbon composite specimens presented a larger damage distribution but a lower impact force. This was expected since the existence of damage can indeed reduce the overall stiffness, therefore indirectly promoting some artificial energy dissipation.

8.1. Future developments

It is expected to start a new experimental campaign for both hailstorm and bird strike for the new type of materials, using the necessary standard testing conditions provided by ASTM. To better improve the numerical simulation, a new VUMAT for composite orthotropic damage materials is being studied, to allow full 3D penetration throughout thicker materials. It is expected to simulate an enhanced large 3D finite element of a wing structure that could be discretized with shell and solid elements, to simulate different impact testing. In this case, the wing would be composed of ribs, stinger, skin, front and rear spar cap.

8.2. Limitations of the study

The numerical simulation of the hail storm impact presented some assumptions regarding the properties of ice. Extra information from experimental studies may be necessary to determine the standard values of fracture energy for ice since its variability is very high in the scientific literature. For this reason, the extrapolation of these results to similar carbon composites should be taken with caution and a critical mindset.

Disclosure statement

No potential conflict of interest was reported by the author(s)

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