

1 **Low- and high-fidelity modeling of sandwich-structured composite response to**  
2 **bird strike, as tools for a digital-twin-assisted damage diagnosis.**

3 E. Giannaros<sup>1</sup>, A. Kotzakolios<sup>1</sup>, V. Kostopoulos<sup>1,\*</sup>, G. Sotiriadis<sup>1</sup>, R. Vignjevic<sup>2</sup>, N. Djordjevic<sup>2</sup>, M.  
4 Boccaccio<sup>3</sup>, M.Meo<sup>3</sup>

5 ~~\*corresponding~~ **Corresponding** author, e-mail: [kostopoulos@mech.upatras.gr](mailto:kostopoulos@mech.upatras.gr)

6 **Keywords:** Bird strike, Composites, Sandwich panel, FEM-SPH, LS-DYNA, Soft body impact,  
7 FBG sensors, Digital Twin

8 **Abstract**

9 The constant requirement of aerospace industry to enhance the structural efficiency has driven to the  
10 usage of high-performance composite materials, either monolithic or sandwich. However, aerospace  
11 composite structures are prone to damage due to high-velocity impact events such as bird strike, hail  
12 impact, etc. These impact events can result in extensive damage including structure perforation,  
13 which will eventually degrade its post-impact residual strength. Therefore, the early detection of  
14 damage in composite structure is imperative to avoid catastrophic failure. This paper develops the  
15 computational models which predict the dynamic behaviour of a helicopter composite sandwich  
16 structure undergoing a bird strike. The models are aimed to be used as virtual tools for a future  
17 digital-twin-assisted fault detection technique. Firstly, a high-fidelity (HF) FE/SPH model was  
18 developed in LS-DYNA, and it was validated against the soft body impact experiments. Afterwards,  
19 a computationally efficient low-fidelity (LF) model was developed and correlated with the high-  
20 fidelity model. It was concluded that the high-fidelity model can sufficiently accurately predict the

---

<sup>1</sup> Applied Mechanics & Vibrations Laboratory, Department of Mechanical Engineering and Aeronautics, University of Patras, Greece (AML, UPAT).

<sup>2</sup> Mechanical and Aerospace Engineering, Brunel University London, United Kingdom (UBRUN).

<sup>3</sup> Materials and Structures Centre, Department of Mechanical Engineering, University of Bath, United Kingdom (MAST, UBATH)

21 strain history experimentally recorded by the FBG sensors, and that size of the predicted delamination  
22 area at the front face of the sandwich structure agrees very well with the experimentally observed  
23 delamination area. It was also shown that the LF model can rapidly predict the global dynamic  
24 response of sandwich panel under the impact loading, through the good agreement between the  
25 numerical strain histories with the FBG measurements. Consequently, the LF model can be used as a  
26 quick numerical guide for the identification of the loading condition, whereas the HF model can be  
27 used as virtual damage detector and estimator of damage extension before the scheduled inspection.

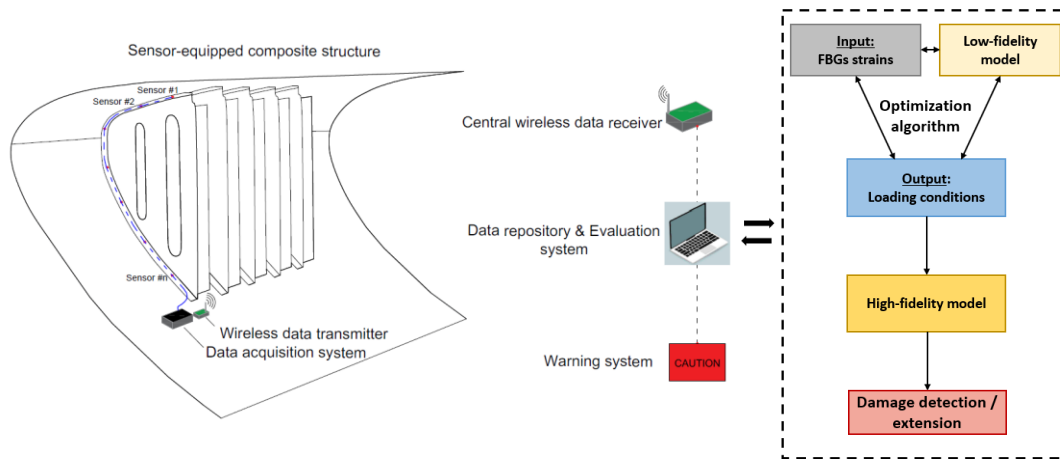
## 28 **1 Introduction**

29  
30 For the last forty years, an escalating usage of composite materials has been observed in aerospace  
31 industry both in the monolithic and the sandwich configuration. Initially, they were utilized in non-  
32 safety critical components and, recently, in primary components for structural efficiency reasons.  
33 Nevertheless, both monolithic and sandwich-structured composites are quite prone to high-velocity  
34 impact events. One common and major importance threat in aviation is the bird strike, which can  
35 occur during the aircraft take-off, landing or low-altitude flight. Although, according to airworthiness  
36 requirements, the aircraft structures exposed to bird strike should be designed to assure capability of  
37 continue safe flight and landing, several accidents have been recorded the last decades. On November  
38 10, 2008, Ryanair Flight 4102 from Frankfurt to Rome suffered multiple bird strikes during landing  
39 [1]. Both engines and port side landing gear were damaged. In 2009, the US Airways Flight 1549 was  
40 landed into Hudson river after the loss of both engines caused by bird strike [2]. In January 2009 in  
41 Louisiana, Sikorsky S-76C helicopter crashed, and 8 people died. A 1.1 kg red-tailed hawk fractured  
42 the windshield and inserted to the engine fuel control system causing a sudden engine power loss [3].  
43 Also, according to the airworthiness regulations, such as CS -29 for helicopters [4], the compliance  
44 to relevant code must be shown by tests, or by analysis based on tests carried out on sufficiently  
45 representative structures of similar design. In this field, several studies have been carried out  
46 demonstrating and comparing various modeling approaches (Eulerian-Lagrangian approach,

47 Lagrangian or Smoothed Particle Hydrodynamics) for bird strike simulation [5], [6], [7], [8], [9],  
48 [10]. Other investigations have focused on the soft-impact response of monolithic composites or  
49 sandwich structures [11], [12], [13], [14]. Also, several works have paid special attention to the  
50 experimental tests for the explanation of soft-body impact event [15], [16], [17], [18]. Despite the  
51 extensive published work in this field, according to the authors' knowledge, the numerical-  
52 experimental combined studies on the response of composite structures, particularly the sandwich-  
53 structured ones, to soft body impact loading are relatively limited; whereas the detection and  
54 estimation of composites damage using digital twin (DT) technology is a brand-new technique that  
55 has not been widely used in aerospace industry yet. A few attempts for the integration of digital twin  
56 concept to aerospace and space industry are shown below. Tuegel et al. [19] presented a conceptual  
57 model using digital twin to predict the aircraft structural life prediction and to assure the structural  
58 integrity in flight conditions. Later, in 2012, Glaessgen and Stargel [20] proposed a digital twin  
59 paradigm for the prediction of the health and the remaining life of future NASA and U.S air force  
60 vehicles. Despite all of that, the digital twin technology is not mature enough for use in the aerospace  
61 industry and more development is required.

62 In the current study, the dynamic response of a curved composite sandwich structure subjected to soft  
63 body impact is investigated numerically and experimentally. In particular, the focus is on a segment  
64 of front transmission fairing of a helicopter. The objective of the current study is the preparation of  
65 two computational models with different fidelity levels and computational costs, aiming at  
66 development of a future digital-twin-assisted damage diagnosis. The low- and high-fidelity models  
67 are intended to be developed as virtual tools for the early damage detection and the estimation of  
68 damage extension during the inspection internals, i.e., before the scheduled maintenance. For the  
69 better comprehension of general idea, a schematic of digital-twin-assisted damage diagnosis concept  
70 including the function of developed models is shown in Fig. 1. In particular, the FBG sensors data,  
71 after impact event, will be collected to a data acquisition system, and then it will be transferred to a

72 data repository system via wireless communication. Afterwards, the evaluation system (onboard or  
 73 ground processor) will compare the recorded strain history with that of models. The concept is to  
 74 develop the low-fidelity model (LF) as a quick numerical guide for the identification of impact  
 75 loading conditions, based on the comparison of the numerically calculated strain history with the real-  
 76 time data from FBG sensors installed in the physical/operating environment, whereas the high-fidelity  
 77 (HF) model will be employed as virtual damage detector and estimator.



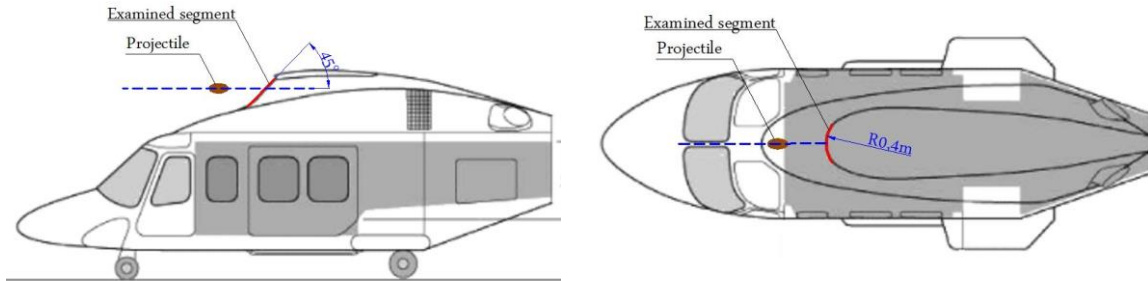
78 Fig. 1 A schematic of digital-twin-assisted damage diagnosis concept

79 The digital twin is a cutting-edge technology that has received a lot of attention, which combines  
 80 several technologies and tools, including Data transmission, collection and processing;  
 81 Communication-Interaction technology; Modeling-Simulation technology; Sensing-Measurement  
 82 technology [21]. As presented above, the modeling technology is the main research focus of this  
 83 study, nevertheless important conclusions can be extracted for the used data recording device  
 84 (interrogator) and the FBG sensing technology.

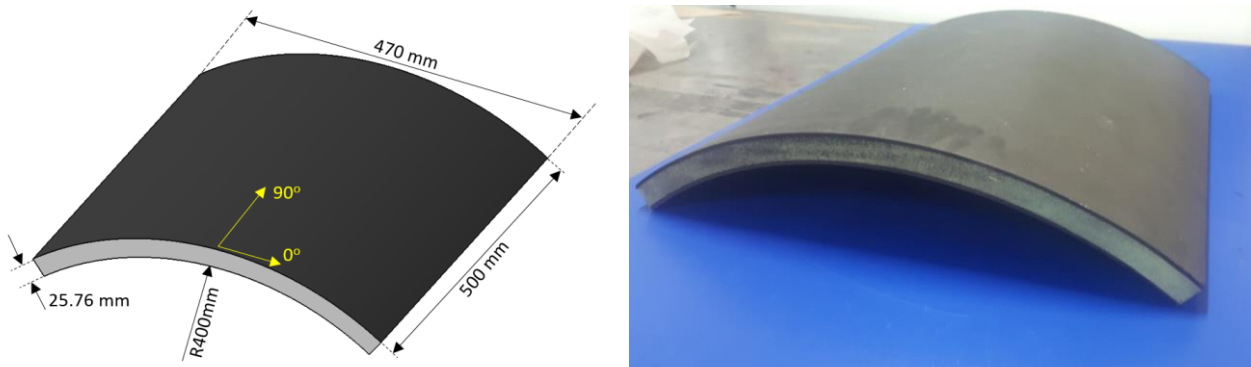
## 85 2 Panel geometry, materials & manufacturing process

86 The engine cowling system of a helicopter is geometrically complex and is divided to many curved  
 87 fairings for design, manufacturing, and maintenance reasons. The current study is focused on the front  
 88 transmission fairing since the probability of a frontal bird strike is higher than the case of an oblique  
 89 impact. In the framework of current work, for the sake of simplicity, a small representative segment

90 of front fairing is examined. The design of the cowl segment manufactured by UPAT was inspired  
91 by an existing solution used on Agusta Westland AW139. In [Fig. 2](#), the top and side view of  
92 AW139 helicopter including the examined segment of cowling system is given.



93 Fig. 2 Top and side view of AW139 helicopter [22] including the examined panel.  
94 The examined sandwich panel is 500 mm in length, 470 mm in width and about 26 mm in thickness.  
95 In essence, it is a slightly curved rectangular panel with a radius of 400 mm, whilst it is assumed that  
96 it consists of two symmetric quasi-isotropic lay-up  $[(45/0/-45/90)_2]_s$  CFRP faces with 2.88 mm  
97 thickness and a 20 mm thick polymer foam layer. The geometry of investigated panel and fiber  
98 orientation system is illustrated in [Fig. 3](#).



99 Fig. 3 Composite sandwich panel and fiber orientation system  
100 For the manufacturing of outer faces of sandwich panel, the high-performance aerospace-grade  
101 unidirectional Cycom 977-2 carbon fiber reinforced polymer (CFRP) material was used [23]. The  
102 matrix constituent is a 177°C curing toughened epoxy resin, whereas the reinforcement consists of  
103 intermediate modulus carbon fibers named TohoTenax IMS60. For the characterization of Cycom  
104 977-2 composite, basic quasi-static mechanical and fracture tests have been performed in-house using  
105 the UPAT servo-hydraulic machines INSTRON 8872 and 8802. All tests were carried out according

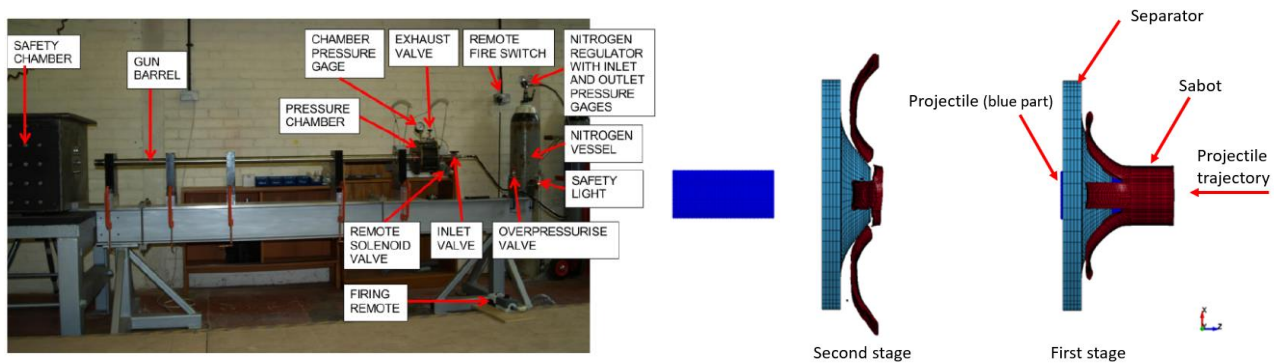
130 the proposed standards for polymer matrix based composite materials [24]. The derived lamina and  
131 interlaminar properties of Cycom 977-2 are illustrated in [Table 2](#)~~Table-2~~, whereas more information  
132 is provided in [24]. Regarding the material of panel core, a closed cell, cross-linked polymer foam  
133 with relative high stiffness and strength and low density (AIREX C70.75) is used. According the  
134 AIREX manufacturer, it is ideally suitable for a wide variety of lightweight sandwich structures  
135 subjected to both static and dynamic loads. The properties of used foam are given in [Table 2](#)~~Table-2~~.  
136 For the bonding of CFRP skins with foam core, the structural thermal resistant AS 89.1 adhesive  
137 supplied by ELANTAS was utilised, which is an aerospace qualified two component epoxy system  
138 with good fracture toughness [25]. The shear strength by tension according to ASTM D 1002 (single-  
139 lap-joint bonded metals) ranges from 27 MPa to 33 MPa, whereas the tensile strength according to  
140 ASTM D 638 is between 50 MPa and 60 MPa [25].

141 Concerning the manufacturing process of panel, secondary bonding between skins and foam was  
142 adopted. Initially, Prepreg/Autoclave process was followed for the preparation of CFRP faces. The  
143 manipulation and cutting of prepregs and other consumables, as well as the lamination procedure,  
144 were carried-out in a controlled environment of 21°C and 70% relative humidity (R.H). After the final  
145 vacuum bagging arrangement, the curved tool was placed in the AML autoclave equipment. In  
146 parallel, the supplied AIREX foam sheet was cut to specific dimensions and was thermoformed  
147 following the proposed procedure by manufacturer. Then, the CFRP faces and the thermoformed  
148 foam sheet were bonded using the epoxy based AS 89.1 adhesive. Afterwards, post-curing process at  
149 60 °C for 2hrs was followed. Finally, C-scan ultrasonic inspection of two panels was executed to  
150 identify that the commonly encountered defects, such as ply delamination, porosity, and resin-rich  
151 zones, do not exist.

### 152 **3 Soft body impact tests**

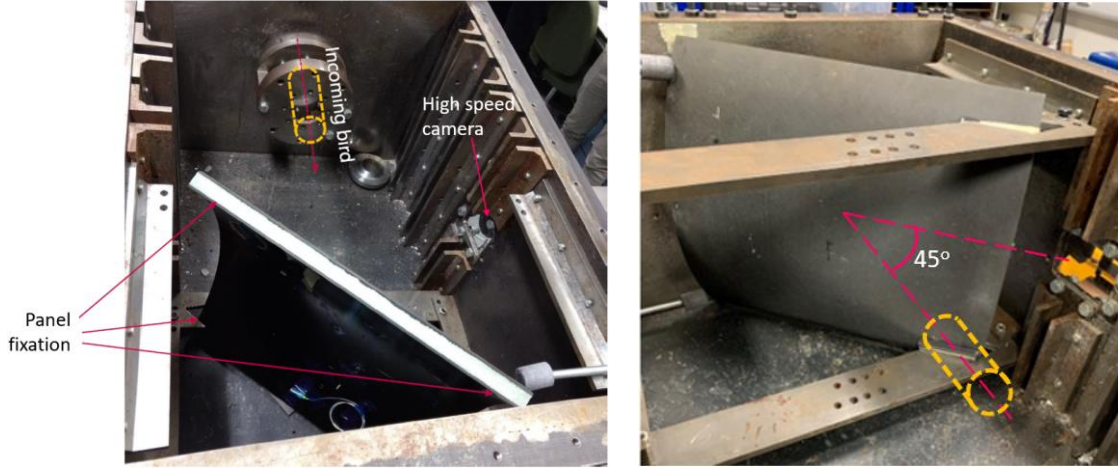
153

174 Two soft body impact tests were conducted with instrumented composite cowl structures using the  
 175 gas gun test facility shown in [Fig. 4](#). The gun consists of gas supply, trigger system, pressure  
 176 vessel, barrel, sabot stripper and target chamber. The barrel used for this testing is 3m long with a  
 177 bore 62 mm, which allowed for the maximum diameter of the projectile representing bird to be 50  
 178 mm. The impact tests were conducted using artificial gelatine projectiles, manufactured following  
 179 the M. Lavoie [17], with 10% porosity. The projectiles were cylindrical in shape with 50 mm diameter  
 180 and 120 mm length. The average mass of projectiles used in the tests was 233g; whilst the material  
 181 density was  $988 \frac{kg}{m^3}$ . The projectiles were launched with, for this purpose designed, sabots. When the  
 182 gas gun was fired, the projectile-sabot assembly was accelerated towards the end of the barrel, where  
 183 the sabot was stripped of, so that the only projectile continued flying towards the target plate ([Fig.](#)  
 184 [4](#)). The projectile/sabot system was aligned with the sabot stripper so that the projectile was  
 185 released appropriately, with a linear velocity in the impact direction without any angular momentum.  
 186 Projectile speed just before the impact was determined using the images from high-speed camera  
 187 shown in [Fig. 5](#); whilst a camera outside the target chamber was used for recording the impact  
 188 event. The measured impact velocity of projectile in front of the target in two experiments was equal  
 189 to 150 m/s. The sequence of the high-speed camera frames of the projectile impact from the initial  
 190 projectile-target contact until the complete disintegration of projectile is given in section 4.4.3, where  
 191 it is compared with the numerical results.



192 Fig. 4 Gas gun test facility used for the bird strike tests (left), and demonstration of projectile-sabot  
 193 separation during the test (right)

210 Both instrumented cowl structures were tested in the same impact conditions, with the target position  
 211 relative to the impact direction and the boundary condition shown in [Fig. 5](#)~~Fig. 5~~. The impact  
 212 occurred in the middle of the target structure and at angle of 45° relative to the target surface normal.



213 Fig. 5 Panel position and fixture in the target chamber

214 For the recording of strain histories during the impact event, three Fiber Bragg Grating (FBG) sensors  
 215 were installed on the back face of the target structure. Each sensor was aligned with specific material  
 216 direction and was carefully attached to the target face, using a low-viscosity two component epoxy  
 217 resin. Immediately after the application of the resin, a piece of adhesive tape was placed on the top  
 218 of the sensor to prevent any resin leakage. The position of sensors is illustrated in [Fig. 6](#)~~Fig. 6~~. All  
 219 sensors were interrogated with the Technobis supergator©, with 19.2 kHz sample rate. The strain  
 220 component was calculated from the sensor wavelength using Equation 1:

$$221 \quad \varepsilon = \frac{1}{p_e} \frac{\Delta\lambda}{\lambda_B} \quad \text{Eq. 1}$$

222 where  $\Delta\lambda$  is load-induced Bragg wavelength shift,  $\lambda_B$  is the Bragg wavelength reflected from the FBG  
 223 sensor,  $p_e$  is the elasto-optic coefficient and  $\varepsilon$  the applied strain along sensor direction. The  
 224 interrogator measured the average strain along the sensor length which was 5 mm. The experimental  
 225 strain measurements used for the model validation are showed in Section 4.4.2.



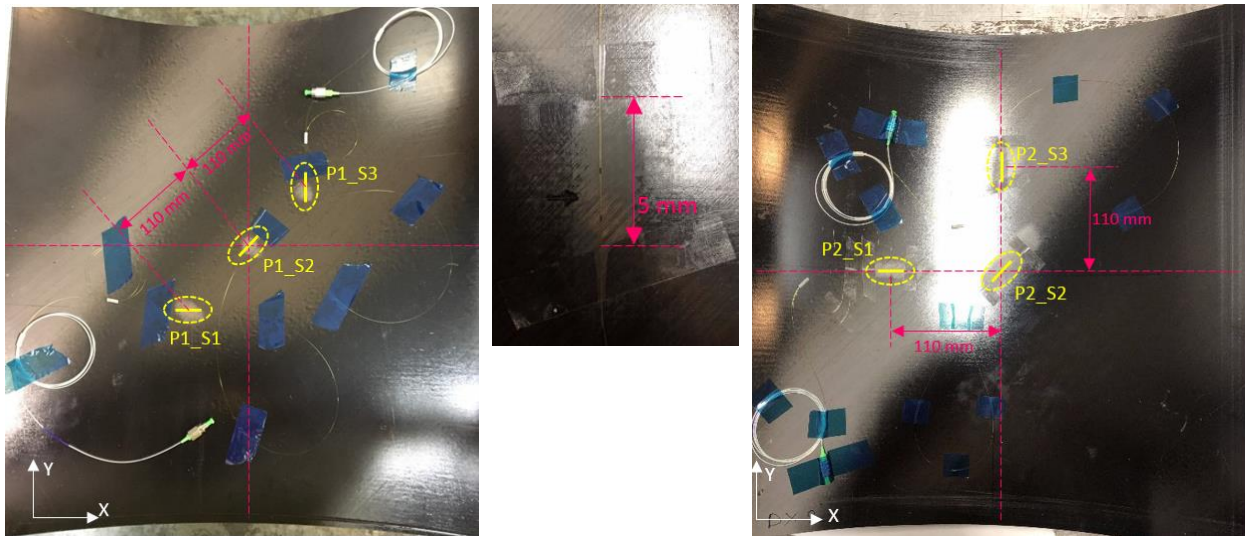


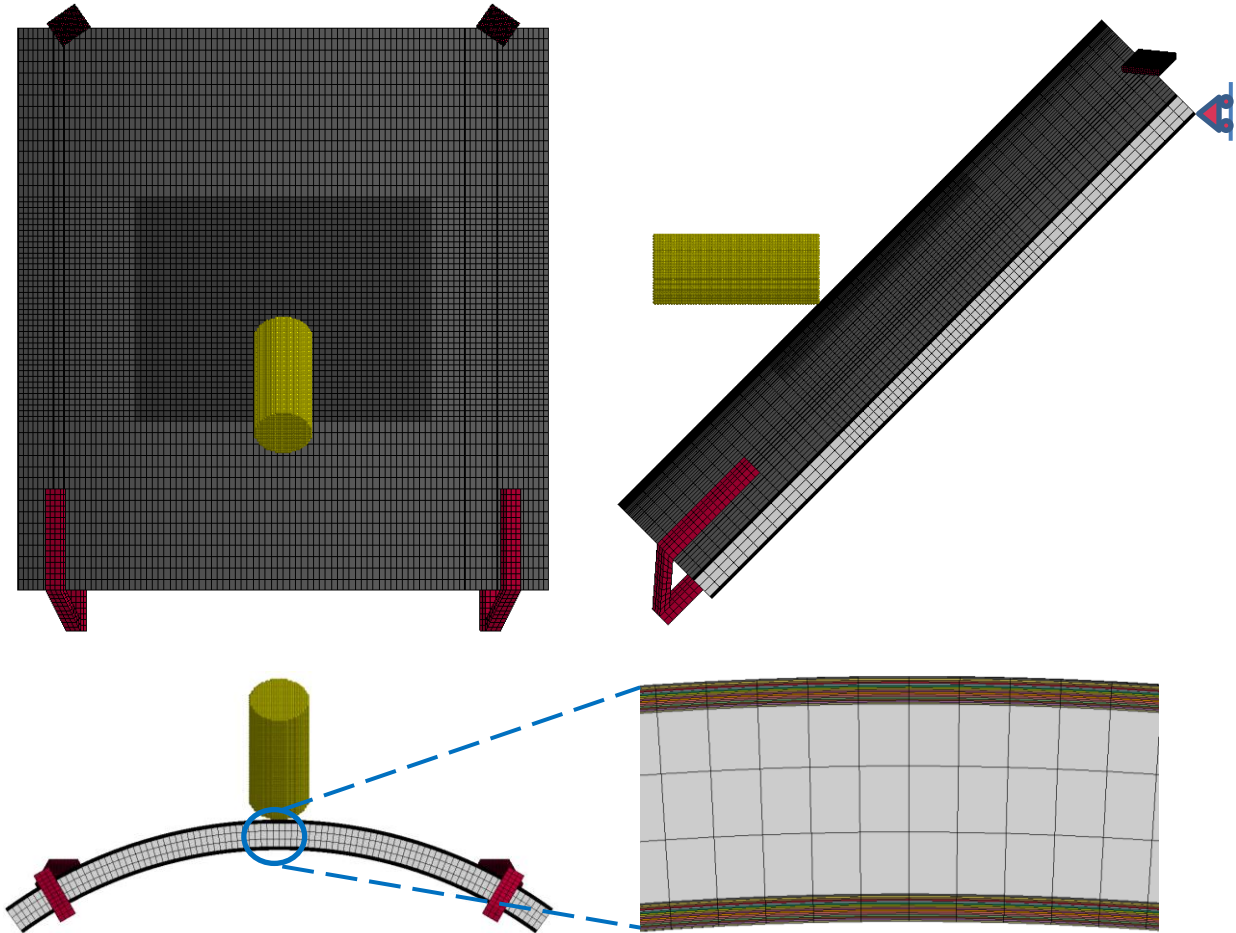
Fig. 6 FBG sensors position (left: panel1, right: panel 2, center: sensor length)

226  
227

#### 228 4 Development and validation of high-fidelity (HF) model

229 In the last decades, several numerical approaches have been used for the simulation of soft body  
 230 impact. These are the Lagrangian approach, the Arbitrary Lagrangian-Eulerian (ALE) method and  
 231 the Smoothed Particle Hydrodynamics (SPH). The major drawback of Lagrangian approach is the  
 232 difficulty to handle the highly distorted elements during the soft body impact, which lead either to  
 233 prohibitive computational time or to unstable numerical solution. On the other hand, the coupled  
 234 Lagrangian-Eulerian method can eliminate all difficulties associated with the extreme bird mesh  
 235 distortion since it allows the finite element to be fixed in space while the material flows through their  
 236 elements [6]. Nevertheless, the SPH method has been characterized as the most promising  
 237 formulation since, first, it avoids completely the mesh tangling [9]; additionally, SPH method seems  
 238 to provide better results in relation to the ALE [5]. For the aforementioned reasons, the current work  
 239 uses the SPH meshless method to simulate the soft projectile, whereas the target structure is  
 240 discretized by the traditional Lagrangian finite element formulation. More information about the  
 241 theory of SPH method is provided to a previous published research of authors [26]; whereas the  
 242 developed high-fidelity model of sandwich panel is illustrated in [Fig. 7](#).

243  
244



246 Fig. 7 High-fidelity model

246

247 **4.1 Modeling of sandwich structure**

248 In general, Finite Element Method is adopted for both CFRP skins and the core of the target structure.

249 In the case of the high-fidelity model, ply-based method (stacked-solid method) with 3D solid

250 elements was adopted for the laminated skins. This technique can predict the separation of laminated

251 plies on the grounds that each lamina is explicitly modelled. Moreover, by the use of solid elements

252 instead of shell elements, no geometric and loading assumptions are required, whereas the boundary

253 conditions are treated more realistically. Also, the solid elements allow the 3D behavior of simulated

254 component to be fully captured. In particular, one fully integrated first-order element formulation

255 (elform 2) per ply was adopted; thus, no hourglass stabilization is needed. However, an elemental

256 error known as shear locking can emerge due to the poor aspect ratio of first-order solid elements in

257 out-of-plane bending conditions. In the case of foam part, for time efficiency reasons, reduced

258 integration solid element with hourglass stabilization is used. The verification of the used element  
 259 formulations is presented in Section 4.3.1. For delamination initiation and propagation modelling,  
 260 cohesive zone methodology (CZM) was applied at each interface. The main advantage of CZM  
 261 method against to the virtual crack-closure technique is that the location of delamination onset does  
 262 not need to be predefined [27], [28]. Specifically, a fracture-based contact algorithm is utilized at  
 263 each lamina interface since it eliminates the drawbacks of cohesive elements, including i.e. the  
 264 reduced time step of solution due to Courant-Friedrichs-Lewy condition, the demand for additional  
 265 contact between laminas to eliminate plies overlapping after failure of cohesive elements, and the  
 266 adjustment of their compressive stiffness [29], [30]. Regarding the meshing, relatively higher density  
 267 mesh is generated near the impact zone (elements size: 5 mm x 5 mm x single-ply-thickness), whereas  
 268 coarser mesh is adopted away from the impact point for the reduction of computational time (elements  
 269 size: 10 mm x 5 mm x single-ply-thickness). In the case of foam core modeling, the three solid  
 270 elements (elform 1: one integration point elements) in through the thickness direction are considered  
 271 adequate for capturing the bending and shear stiffness of panel (Fig. 7). The verification checks  
 272 are illustrated in section 4.3.1.

273 Regarding the constitutive laws and failure criteria, the [Table 1](#) provides the used LS-DYNA  
 274 material model for each material.

Material	Type of failure	Type of material model
CFRP	Intralaminar behavior	MAT_ENHANCED COMPOSITE DAMAGE (MAT_54)
	Interlaminar behavior	Fracture-based contact-option 9. It is based on MAT_COHESIVE_GENERAL (MAT_186)
Polyvinylchloride (PVC) foam	Compression & shear failure	MAT_HONEYCOMB (MAT_026)

275 Table 1. Used material models for composite sandwich panel

276 According to LS-DYNA documentation [29], the material models of interest capable of representing  
 277 orthotropic constitutive law with the failure criteria and taking into account damage effects, are the  
 278 following: 1) Enhanced Composite Damage (MAT\_54), 2) Laminated Composite Fabric (MAT\_58),  
 279 3) Rate-Sensitive Composite Fabric (MAT\_158), 4) Composite MSC (MAT\_161) for which special

303 license required, 5) Orthotropic Simplified Damage (MAT\_221) and 6) Laminated Fracture Daimler  
 304 (MAT\_261-262). However, only MAT\_54, MAT\_161, MAT\_221, and MAT\_261-262 are  
 305 compatible with 3D solid elements. The current study focused on MAT\_54 since it provides more  
 306 simplified damage model than the others while it can capture the most significant failure modes: a)  
 307 Tensile fiber mode, b) Compressive fiber mode, c) Tensile and d) Compressive matrix mode. The  
 308 MAT\_54 includes two criteria for tensile fiber mode; the first one is the original Hashin criterion  
 309 which is activated using the  $\beta$  factor equal to 1, whereas the second one constitutes a modification of  
 310 Hashin criterion. In current study, the modified criterion is applied since the inclusion of the shear  
 311 stress term in original Hashin tensile fiber criterion underpredicts the peak failure load of cross-ply  
 312 and quasi-isotropic laminate to tension loading according to literature [31], [32]. The failure criterion  
 313 equations are the following:

314 • tensile fiber mode:  $\left(\frac{\sigma_{aa}}{X_T}\right)^2 = 1$  Eq. 2

315 • compressive fiber mode:  $\left(\frac{\sigma_{aa}}{X_C}\right)^2 = 1$  Eq. 3

316 • tensile matrix mode:  $\left(\frac{\sigma_{bb}}{Y_T}\right)^2 + \left(\frac{\sigma_{ab}}{S_{ab}}\right)^2 = 1$  Eq. 4

317 • compressive matrix :  $\left(\frac{\sigma_{bb}}{2 \times S_{ab}}\right)^2 + \left[\left(\frac{Y_C}{2 \times S_{ab}}\right)^2 - 1\right] \frac{\sigma_{bb}}{Y_C} + \left(\frac{\sigma_{ab}}{S_{ab}}\right)^2 = 1$  Eq. 5

318 where  $\sigma_{aa}$ =normal stress in longitudinal direction (fiber),  $\sigma_{bb}$ =normal stress in transverse  
 319 direction (matrix),  $\sigma_{ab}$ =in-plane shear stress,  $b$  is the parameter for failure criterion determination,  
 320  $X_T$  longitudinal tensile strength (fiber),  $X_C$  longitudinal compression strength (fiber),  $Y_C$  transverse  
 321 compressive strength (matrix),  $Y_T$  transverse tensile strength (matrix),  $S_{ab}$  shear strength in-plane. The  
 322 moduli and strength values for Cycom 977-2 are provided in [Table 2](#); whereas, the  
 323 applicability of the used orthotropic material model as well as the necessary model's parameters for  
 324 simulating the Cycom 977-2 behavior to impact loading have been thoroughly examined in a previous  
 325 research of the authors [24]. The cohesive material model in the present investigation was also based

326 on the previous study of authors [24]; in which it has been proved that the MAT\_186 using bilinear  
327 traction-separation law and 2D power law as fracture criterion can accurately represent the inter-layer  
328 behavior of Cycom 977-2 to mode I and mode II. The optimal set of cohesive model's parameters has  
329 been obtained using a multi-stage calibration routine. The traction-separation law in MAT\_186 is  
330 defined by the following parameters: a) interlaminar fracture toughness energy to mode I ( $G_{IC}$ ), b)  
331 normal peak traction ( $T$ ), c) interlaminar fracture toughness energy to mode II ( $G_{IIC}$ ), d) shear peak  
332 traction ( $S$ ), e) the normalized separation at peak traction ( $\lambda_o$ ), f) exponent of power law mixed-mode  
333 criterion ( $xmu$ ); all of which had been considered unknown parameters of calibration problem. The  
334 values of these parameters for Cycom 977-2 are shown in [Table 2](#), and they are applied  
335 indistinguishably to the developed HF model. No strain rate effects of the CFRP were considered in  
336 the framework of current work.

<b>Cycom 977-2 CFRP</b>	Value	Unit	<b>AIREX C70.75 foam</b>	Value	Unit
Elastic moduli, $E_a$ , for tension	191	GPa	Average density	80	Kg/m <sup>3</sup>
Elastic moduli, $E_b$ , for tension	8.85	GPa	Compressive modulus	80	MPa
Elastic moduli, $E_a$ , for compression	121	GPa	Shear modulus	50	MPa
Elastic moduli, $E_b$ , for compression	8.73	GPa	Tensile modulus	50	MPa
Poisson's ratio $\nu_{ab}$	0.258	-	Compressive strength	1.10	MPa
Poisson's ratio $\nu_{bc}$	0.33	-	Shear strength	1.0	MPa
In-plane shear modulus, $G_{ab}$	4.41	GPa	Shear elongation at break	10	%
Out of plane shear modulus, $G_{ac}$	4.22	GPa	Tensile strength	1.6	MPa
Tensile strength in fiber axis ( $X_T$ )	3325	MPa	<b>AS 89.1 epoxy adhesive</b>		
Compressive strength in fiber axis ( $X_C$ )	910	MPa	Shear strength (SFLS)	27-33	MPa
Tensile strength in matrix axis ( $Y_T$ )	68	MPa	Tensile strength (NFLS)	50-60	MPa
Compressive strength in matrix axis ( $Y_C$ )	170	MPa			
Shear strength in ab plane ( $S_{ab}$ )	81.0	MPa			
Nonlinear shear stress parameter (ALPH)	0.01	-			
Fracture toughness energy to mode I	347.1	J/m <sup>2</sup>			
Fracture toughness energy to mode II	571.0	J/m <sup>2</sup>			
Peak traction in normal direction (T)	2.0	MPa			
Peak traction in tangential direction (S)	16.94	MPa			
Normalized separation at peak traction	0.5	-			
Exponent of power law mixed-mode	2.0	-			

337 Table 2. Mechanical properties of used materials and models' parameters [23], [33], [25]

338 Regarding the material model of polymer foam, LS-DYNA includes more than 15 material models  
339 for the simulation of a foam material response [29]. In the framework of current work, special  
340 attention was given to MAT\_HONEYCOMB (MAT\_026) since it can describe the elastoplastic  
341 behavior of foam material separately for all normal and shear stresses, and these are fully uncoupled.

366 Hence, two bilinear stress-strain curves were defined for the case of PVC foam using the datasheet  
367 properties of used foam ([Table 2Table-2](#)): one curve for three normal stresses and one for all shearing  
368 stresses considering it isotropic. This approach provides the advantage to determine the compression  
369 and shear failure strain explicitly. According to R. Wang [34], the AIREX C70.75 foam present  
370 similar modulus to both quasi-static and high strain rate compression loading (4400 /s), whereas the  
371 dynamic peak stress is slightly higher than the peak stress observed for quasi-static strain rate tests.  
372 Therefore, the strain rate dependency on foam properties was considered negligible. Also, they  
373 presented that AIREX C70.75 has similar compressive response in three directions, therefore the  
374 above consideration about the isotropy behavior is true. The verification checks for material model  
375 input data are provided in section 4.3.2.

376 Finally, surface to surface tiebreak contact algorithm is implemented between the interface of CFRP  
377 skin and foam for the skin-core debonding modeling. In particular, the interface nodes are initially  
378 tied until the stress-based failure criterion is met. The tiebreak failure criterion has normal and shear  
379 component according to the following equation:

$$380 \quad \left(\frac{|\sigma_{nl}|}{NFLS}\right)^2 + \left(\frac{|\sigma_{sl}|}{SFLS}\right)^2 \geq 1 \quad \text{Eq. 6}$$

381 where *NFLS* is the normal strength and *SFLS* is the shear strength. The strength values are taken equal  
382 to the minimum adhesive properties given in [Table 2Table-2](#).

## 383 4.2 Modeling of gelatine projectile

384 As previously described, the gelatine bird's substitute was modelled using SPH particles. In  
385 particular, two SPH gelatine models with different particles size are created and evaluated. The first  
386 model consists of 1920 particles with initial particle distance (PD) equal to 5 mm, each having a  
387 lumped mass 0.1213 g; and the second one contains 29340 particles with 2 mm PD, each having a  
388 lumped mass of 0.00794 g ([Fig. 8Fig-8](#)). The length and diameter of cylindrical projectile are kept  
389 equal to the actual ones.

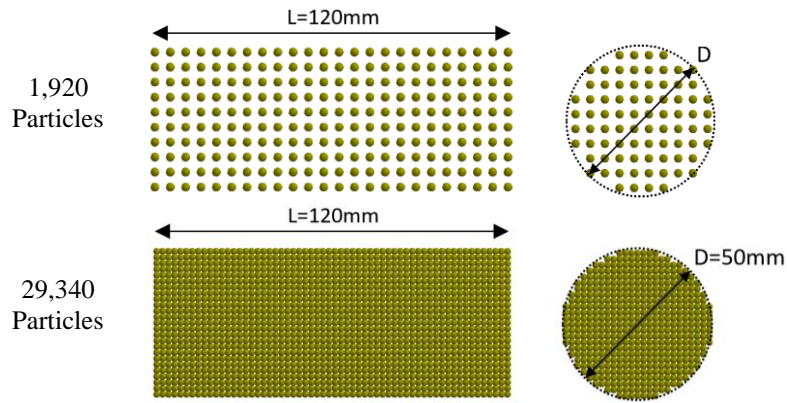


Fig. 8 SPH model of gelatine projectile

390  
 391 Regarding the SPH parameters, according to [26], the most influential parameters are: a) the  
 392 mathematical parameter (CLSH in LS-DYNA), which is a scale factor of initial particle distance, is  
 393 used for the calculation of initial smoothing length and influences the support domain of smoothing  
 394 function; and b) the FORM parameter which defines the particle approximation theory [35]. It has  
 395 been shown that a high CLSH value (1.5÷2.0) can eliminate the numerical failure due to tensile  
 396 instabilities, whereas the renormalized approximation theory (FORM=1) improves the accuracy of  
 397 derivative estimation of a field function [26], [36], [37]. In the framework of current work, the  
 398 influence of these parameters was examined below using a virtual compression test; and it is  
 399 concluded that the above consideration is true and useful for the validity of model. Regarding the  
 400 constitutive equations for birds, much effort and significant progress have been made to develop  
 401 representative material models according to the review paper [38]. It has been found that the real  
 402 birds behave as fluids during the impact at velocities larger than 75 m/s, and a gelatine substitute with  
 403 10 percent porosity is recommended [15], [39]. In our case, the percent porosity of gelatine substitute  
 404 is about 1% and the density is equal to  $988 \frac{kg}{m^3}$ , therefore it can be modelled as water. Investigating the  
 405 relative literature, several modeling approaches have been applied during the last decades [9], [6],  
 406 [38], [40], [8]: a) the bird can be assumed to be inviscid fluid (such water) using an equation of state  
 407 (linear, polynomial, tabulated, Gruneisen or Murnaghan EOS), b) modeling of the soft body as  
 408 elastoplastic material [13], c) using viscous fluid models combined with an EOS. This study was

409 focused on the first modeling approach using a hydrodynamic material model (\*MAT\_null of LS-  
410 DYNA) without viscosity and linear EOS, where the pressure is defined as:

$$411 \quad P = K \times \left( \frac{\rho}{\rho_o} - 1 \right) \quad \text{Eq. 7}$$

412 where  $P$  is the pressure,  $K$  is the bulk modulus (2,200 MPa for water [38]),  $\rho$  is the density of material  
413 and  $\rho_o$  is the reference density at which the material has no pressure ( $988 \frac{kg}{m^3}$  for water). The null  
414 material model relates the stress and strain of the bird by the following:

$$415 \quad \sigma_{ij} = -P\delta_{ij} + 2\mu\dot{\epsilon}_{ij} \quad \text{Eq. 8}$$

416 where  $P$  is the fluid pressure,  $\delta_{ij}$  is the volumetric stress tensor and  $2\mu\dot{\epsilon}_{ij}$  the deviatoric part which  
417 is computed for nonzero dynamic viscosity  $\mu$ . In this study, the second part of equation is neglected  
418 as the water viscosity is considered relatively low. The verification of used modeling approach is  
419 demonstrated in the section 4.3.4.

## 420 **4.3 Verification of numerical uncertainties**

### 421 **4.3.1 Panel stiffness verification**

422 This section presents the verification check of the sandwich panel stiffness. It targets to investigate  
423 the influence of used mesh density and element formulation on the maximum deflection of sandwich  
424 structure. A 300 N uniformly distributed load along the beam width was applied in the middle of the  
425 beam, subjecting it to 3-point bending. The investigated geometry constitutes a small representative  
426 sample of sandwich panel model with the same length and thickness ([Fig. 9](#)).



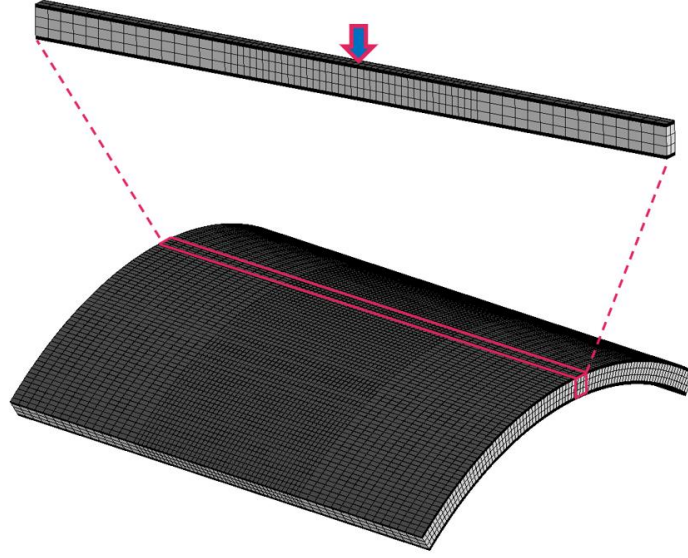


Fig. 9 Representative sample examined for panel stiffness verification

For the calculation of numerical deflection, the implicit nonlinear static solver of LS-DYNA was used; whilst the maximum theoretical deflection for the simply supported beam,  $\delta_{tot}$ , to 3-point bending can be found by the following equations. According to sandwich beam theory, if the core is weak,  $E_c \ll E_f$ , then the transverse shear stress can be approximated as constant in the core [41]. The above assumption is valid since the maximum divergence between numerical results and theoretical ones is 3% approximately (Table 3Table-3).

$$\delta_{tot} = \delta_b + \delta_s \quad \text{Eq. 9}$$

$$\delta_b = \frac{P \cdot l^3}{48 \cdot (EI)_{eq}} \quad \text{Eq. 10}$$

$$(EI)_{eq} = E_f \frac{bt_f^3}{6} + E_f \frac{bt_f d^2}{2} + E_c \frac{bt_c^3}{12} \quad \text{Eq. 11}$$

$$\delta_s = \frac{P \cdot l}{4 \cdot (AG)_{eq}} \quad \text{Eq. 12}$$

$$(AG)_{eq} = \frac{b \cdot d^2 \cdot G_c}{t_c} \quad \text{Eq. 13}$$

where  $\delta_s$  is the deflection due to shear,  $\delta_b$  the deflection due to bending,  $P$  the applied load,  $l$  the length of beam,  $b$  the beam width,  $E_f$  is the Young's modulus of faces,  $E_c$  is the Young's modulus of core,

463  $G_c$  is the shear modulus of core,  $t_f$  the thickness of faces,  $t_c$  is the thickness of core and  $d$  is the total  
 464 thickness of beam.

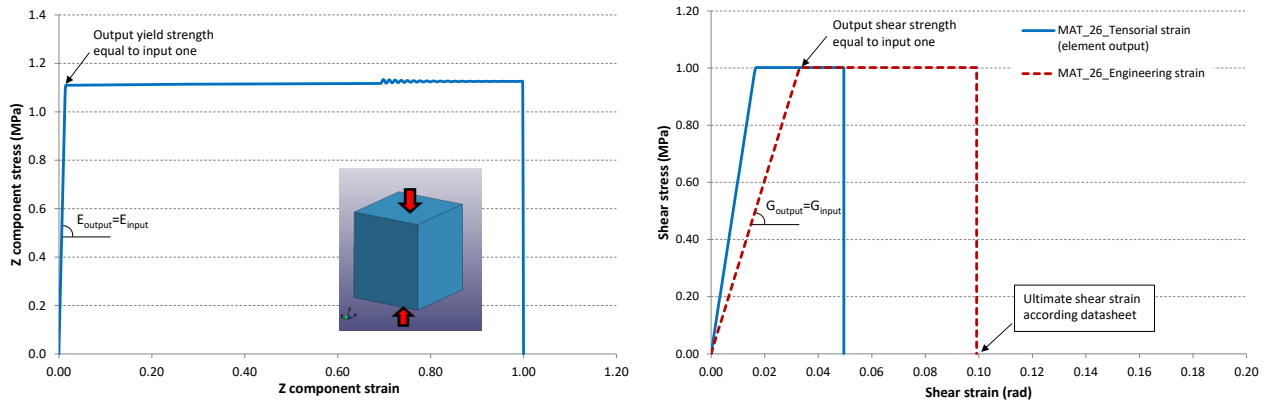
No	Calculation Method	Elem. formulation of skin (thick shell or solid)	Element size to mm (skin)	No of elem. / skin thickness	Elem. formulation of foam (solid)	No of elem. / core thickness	Deflection (mm)
-	Theoretical	-	-	-	-	-	6.75
1	FEM	Thick shell 5		1	elform 2	3	6.75
2	FEM					6	6.78
3	FEM	Thick shell 3	Min: $5 \times 5 \times t_f$ Max: $10 \times 5 \times t_f$	2			6.82
4	FEM						6.73
5	FEM	Thick shell 5		1	Reduced integration linear solid (elform 1)	3	6.75
7	FEM	Fully integrated linear solid (elform 2)	Min: $5 \times 5 \times t_{ply}$ Max: $10 \times 5 \times t_{ply}$	16	elform 2		6.89
8	FEM			16			6.89

465 Table 3. Comparison of numerical and theoretical deflection

466 Observing the summary results in [Table 3](#), firstly, it is inferred that no shear locking exists  
 467 using fully integrated linear solid elements since the simulation provides a 2% overprediction of  
 468 theoretical value (No 8). Secondly, the deflection of one thick shell formulation 5 through the skin  
 469 thickness is line with the theory (No 1, 6); therefore, it is a promising element formulation for the  
 470 case of low-fidelity model as it significantly reduces the computational time. Based on this simulation  
 471 programme, the mesh influence on maximum deflection is negligible.

### 472 4.3.2 Verification of foam material model (MAT\_26)

473 In this paragraph, the numerical tests for the verification of foam material model were illustrated. A  
 474 single reduced integration solid element was tested to compressive and shear loading up to the  
 475 ultimate failure. The output stress-strain responses in both cases were recorded; and the initial  
 476 stiffness, yield strength and ultimate strain were compared with the input values derived by material  
 477 datasheet ([Table 2](#)).



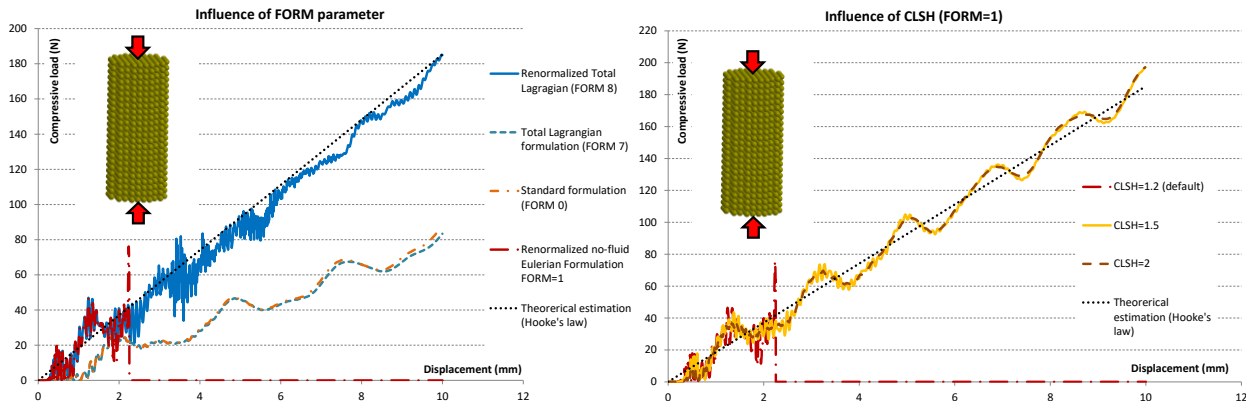
479 Fig. 10 Stress-strain response of a single element subjected to compressive and shear loading.

480 Comparing the ~~Fig. 10~~ results with the input data (~~Table 2~~), it is concluded that the  
 481 material model combined with reduced integration element formulation is capable of capturing the  
 482 elastoplastic behavior of foam material. In particular, excellent agreement on initial stiffness, yield  
 483 point and ultimate strain exists between the input and output data. This step was necessary to  
 484 demonstrate that the user-defined input curves (stress-strain) has been set appropriately.

### 485 4.3.3 Verification of SPH parameters

486 This section is devoted to the identification of the values of the most influential SPH parameters  
 487 presented above (CLSH and FORM). In the framework of current work, the influence of these  
 488 parameters was examined to uniaxial compression loading since it is the main loading condition  
 489 during the impact event. Essentially, a cylindrical elastic body modelled from uniformly distributed  
 490 SPH particles was chosen to examine. The dimensions, the number of particles and the distance  
 491 between them were assumed equal to the first gelatine model of ~~Fig. 8~~. The aim, here, is to  
 492 determine the values of SPH parameters which eliminate the numerical failures while an elastic  
 493 constitutive law with no failure criteria has been adopted. ~~Fig. 11~~ shows the influence of each  
 494 parameter on the compressive stiffness of model. It is observed that the renormalized formulation  
 495 with Eulerian kernel (FORM 1) is close to both the renormalized Lagrangian formulation (FORM 8)  
 496 and the theoretical curve. For small values of CLSH parameter, numerical failure (red dashed line) is  
 497 observed, whereas, considering a value of 1.5 or higher one, the numerical results improvement is

498 visible. For the rest of this paper, the renormalized no fluid Eulerian formulation (FORM 1) with  
499 CLSH equal to 1.5 is adopted.



500 Fig. 11. Influence of FORM and CLSH parameter on compressive stiffness

501

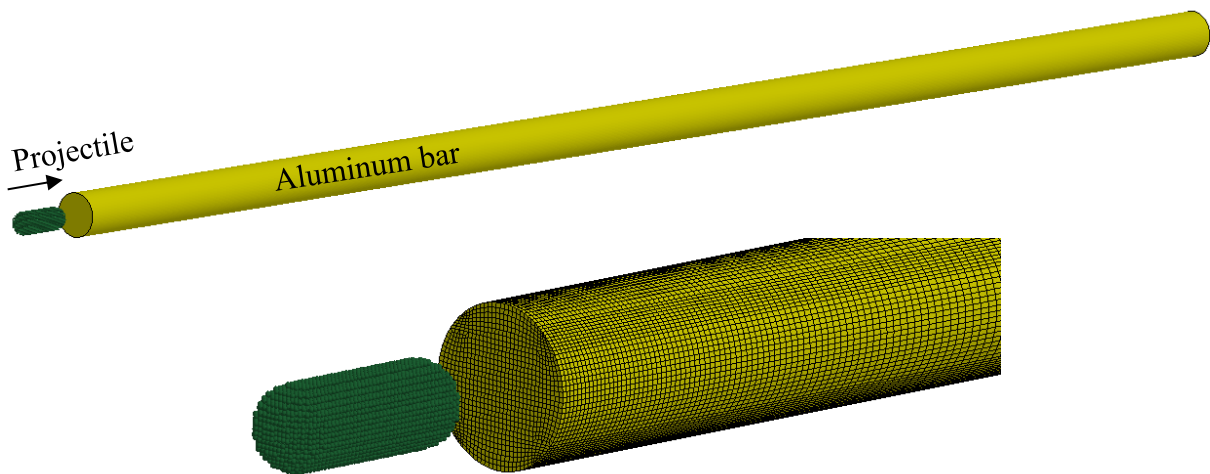
#### 502 4.3.4 Evaluation of bird's substitute model

503

504 As mentioned previously, a hydrodynamic material model combined with linear EOS is applied for  
505 the modeling of gelatine bird model. The majority of numerical works [9], [40], [12], [10], [7], [42]  
506 has been focused on the experimental pressure profile at the centre of impact, published by [16], as  
507 means of verification. However, the pressure profile is highly affected by the resonant excitation of  
508 transducers providing high-frequency noise [16]; and therefore, the used experimental data can lead  
509 to erroneous setting of numerical model. According to the authors, the estimation of force-time  
510 history which is exerted by bird impact is of greater importance than the central pressure profile. The  
511 maximum force and the total transferred momentum (or impulse) obtained by integrating the force  
512 history provide better insight of loading condition. For this reason, the current study was based on the  
513 experimental study of [15], where the peak force and the impulse have been calculated for four  
514 different impact velocities of a 600 g bird. According to the authors' knowledge, this experimental  
515 data has been already used in [43] for the validation of a tabulated EOS. Therefore, the V.Walvekar's  
516 results will be taken into consideration for the evaluation of current study's model.

517 In the framework of present work, a more geometrically representative model using linear EOS is  
518 examined in relation to that of V.Walvekar [43]. In parallel, both the literature experimental results

538 [15] and the numerical results [43] are correlated with the simulation results derived from the current  
539 approach. In particular, experimental tests published in [15] were numerically replicated using the  
540 LS-DYNA software. A 600 g bird's substitute modelled by SPH particles was launched against the  
541 end of a 4.8m long, circular, aluminium bar with 127 mm diameter (Fig. 12Fig. 12); and four different  
542 analyses were carried out at impact velocity levels of 192 m/s, 219 m/s, 249 m/s and 269 m/s. The  
543 projectile is modelled as a hemispherical-ended cylinder with 9520 uniformly distributed particles  
544 with 4 mm PD (Fig. 12Fig. 12), whereas its assumed diameter ( $d=68\text{mm}$ ), length ( $l=192\text{ mm}$ ) and  
545 density ( $\rho=973\text{ kg/m}^3$ ) are obtained by certain empirical formulas provided in [44]. Regarding the  
546 results, it should be mentioned that no filtering is applied to the numerical force-time responses  
547 presented below.



548 Fig. 12. Model of bird strike on aluminum target.

549 The Fig. 13Fig. 13 provides a comparison of the numerical force-time profiles for linear (section 4.2),  
550 polynomial and Gruneisen EOS in the case of 192 m/s (graph 'a'); it shows the numerical force  
551 histories for all impact velocities adopting the linear EOS and it cites the theoretical stagnation force  
552 and the theoretical impact duration in graph 'b'. Also, it correlates the non-dimensional peak force  
553 and non-dimensional impulse derived from current simulation study with the numerical and  
554 experimental ones taken from literature in graphs 'c' and 'd' of Fig. 13Fig. 13. The constants of  
555 polynomial and Gruneisen formulas was obtained from [11] and [9], [40], respectively. The non-  
556

Form

570 dimensional peak force and impulse were calculated by the following generic equations for rigid  
 571 target:

572 
$$\text{Non-dimensional Peak Force} = \frac{\text{Numerical peak value}}{\text{Theoretical stagnation value}} = \frac{F_p * l}{m * v^2 * \sin\theta}$$
 Eq. 14

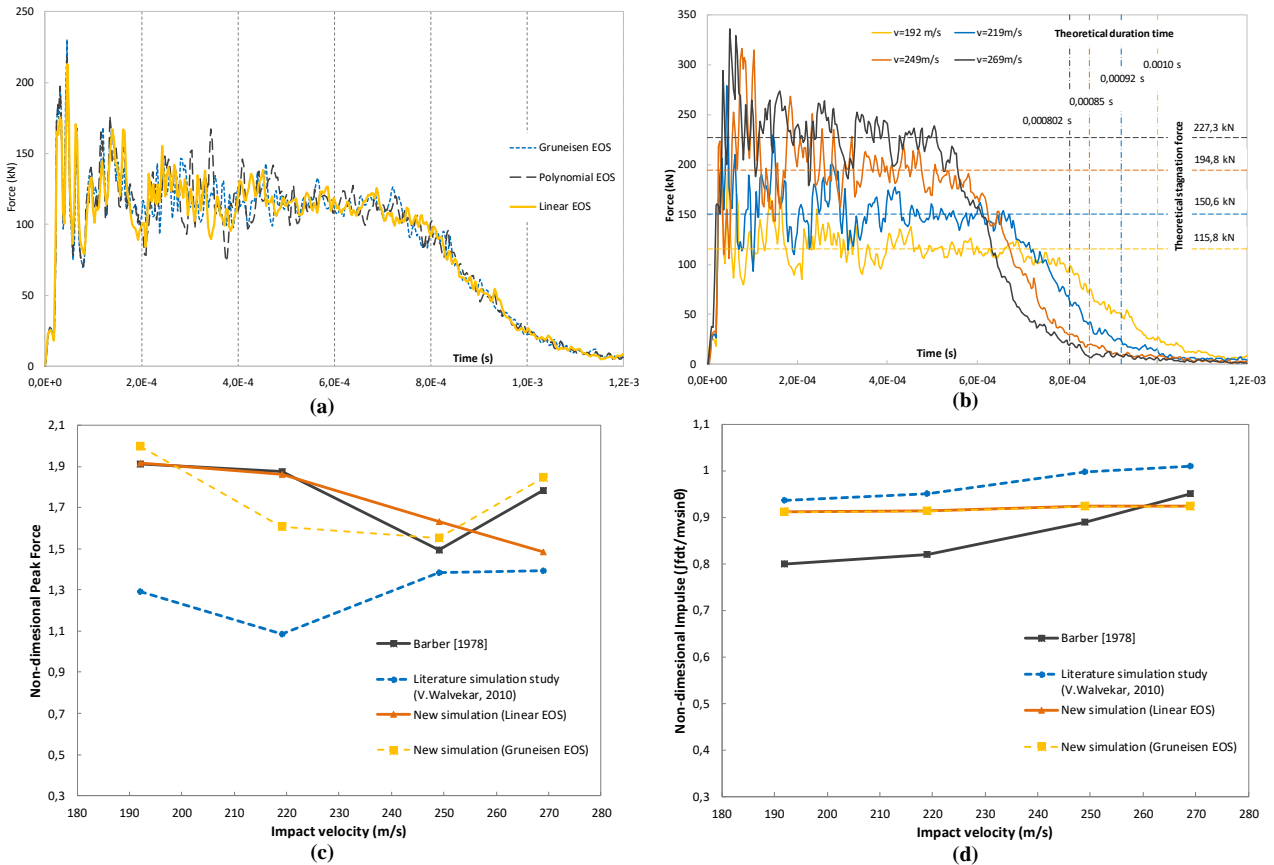
573 
$$\text{Non-dimensional Impulse} = \frac{\text{Numerical Impulse}}{\text{Theoretical Impulse}} = \frac{\int F dt}{m * v * \sin\theta}$$
 Eq. 15

574 where  $F_p$  is the peak force obtained by graph 'b' of Fig. 13,  $m$  is the mass of bird,  $v$  is the  
 575 impact velocity,  $l$  is the length of bird and  $\theta$  is the impact angle. The theoretical impact duration and  
 576 stagnation force were calculated by Eq.16 and Eq.17, respectively:

577 
$$\text{Theoretical impact duration} = \frac{l}{v}$$
 Eq. 16

578 
$$\text{Theoretical stagnation force} = \frac{m * v^2 * \sin\theta}{l}$$
 Eq. 17

579



580 Fig. 13. Numerical force histories (a, b), Non-dimensional Peak Force (c) and Impulse (d).

581 Observing the Fig. 13, the following conclusions are inferred: a) the trend of numerically  
 582 derived impact force history does not change by the type of EOS (graph 'a'); b) the simulated and the

608 experimental non-dimensional peak forces are identical in the cases of 192 m/s and 219 m/s when  
609 linear EOS is used, however the simulation significantly diverges from tests for impact velocities  
610 higher than 250 m/s (graph 'c'); c) unlike, the Gruneisen EOS presents better results for impact  
611 velocities equal to 249 m/s and 269 m/s; d) the simulations executed in current study showed  
612 remarkably better results, in terms of non-dimensional peak force and impulse, in relation to  
613 V.Walvekar's numerical work (graph 'c' and 'd'); e) the influence of EOS formula on the non-  
614 dimensional impulse is negligible according the graph 'd' of [Fig. 13](#)~~Fig. 13~~; h) finally, the numerical  
615 stagnation force and impact duration using the linear EOS are in good agreement with theoretical  
616 ones for all impact velocities (graph 'b'). In this section, it was proved that the hydrodynamic material  
617 model combined with zero dynamic viscosity, linear EOS and the proper SPH parameters presents  
618 excellent results in terms of peak force for impact velocities lower than 219 m/s; whereas the  
619 numerically predicted impulse is closer to experimental one, in any impact condition, in comparison  
620 with the V.Walvekar's prediction.

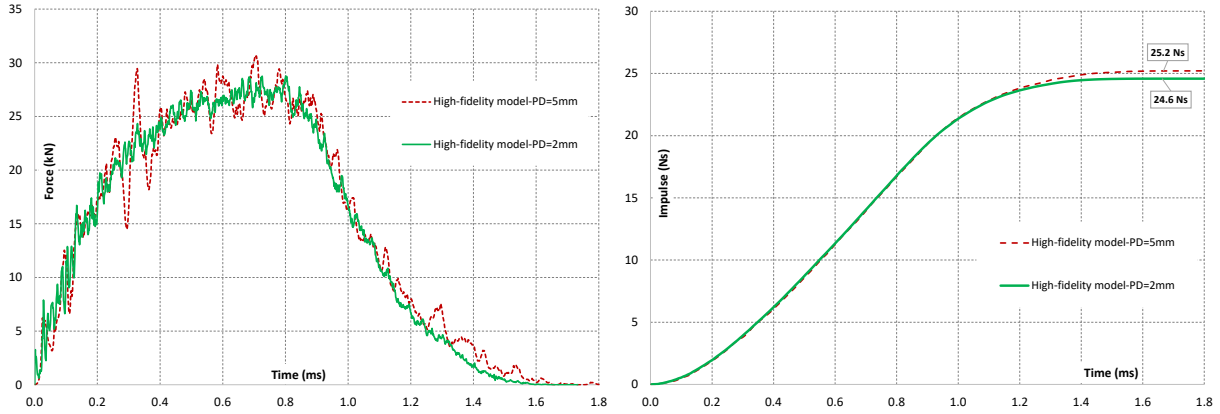
#### 621 **4.4 Validation of the HF model results against the Experiments**

622 In this section, the numerical results of HF model using the two different SPH configurations are  
623 presented. Initially, the numerical contact force histories are discussed; then, the numerical strain  
624 histories are compared with the results derived by test campaign; afterwards, the numerical and  
625 experimental evolution of projectile's deformation are correlated; and finally, the interlaminar  
626 damage of CFRP faces is assessed.

##### 627 **4.4.1 Numerical force-time response**

628 The [Fig. 14](#)~~Fig. 14~~ illustrates the numerical contact force histories obtained from the simulation with  
629 the HF model using the two different projectile's models. Initially, it is observed that the model with  
630 5 mm PD provides larger oscillations at contact force-time diagram in relation to 2 mm PD model,  
631 which is definitely linked with the contact gap formed by the particles distance. The peak force is  
632 found equal to 30.6 kN and 28.8 kN for 5 mm and 2 mm PD model, respectively. However, the

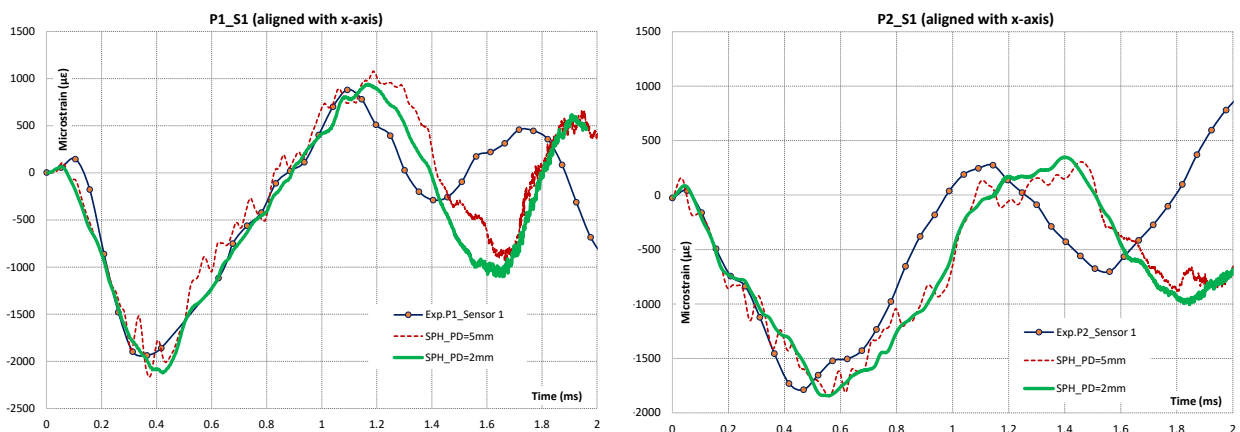
633 impulse constitutes a more reliable indicator for model verification than the maximum force due to  
 634 the aforementioned numerical oscillations. The difference between impulse values of two models is  
 635 within 2.4%, which is acceptable range.



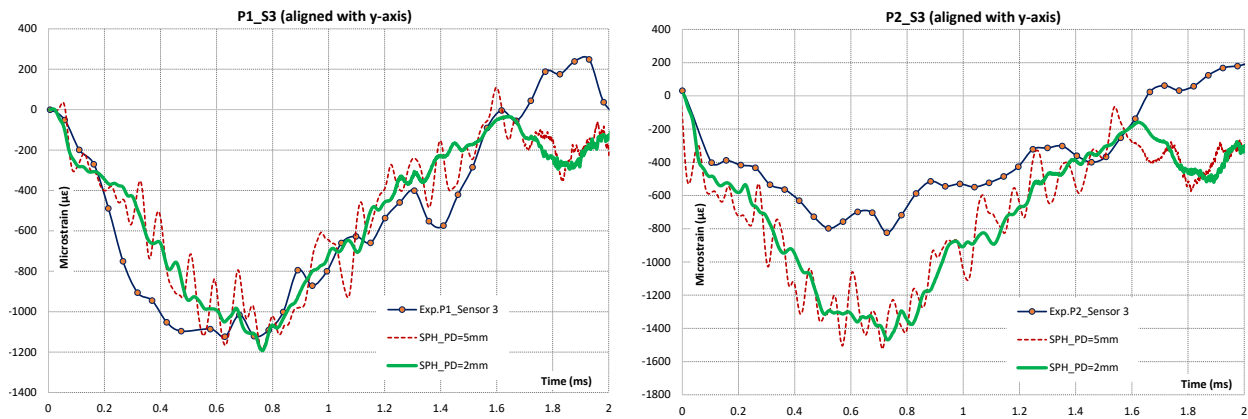
636 Fig. 14 Numerical contact force histories and impulse values (HF model)

637 **4.4.2 Numerical and experimental strain histories**

638 Fig. 15 depicts the experimentally measured strain histories obtained from four FBG sensors (P1\_S1,  
 639 P1\_S3, P2\_S1 and P2\_S3) and the corresponding numerical results computed from HF model. The  
 640 locations of measurements were clearly specified in Fig. 6. From numerical point of view, each  
 641 numerical strain-time profile was calculated from the output strain data of corresponding solid  
 642 element located on the back side of target structure.







643  
644  
645

Fig. 15 Correlation of the HF numerical strain-time profiles with the experimental ones (based on global coordination system of Fig. 6 Fig-6).

646  
647  
648  
649  
650

It should be mentioned that the numerical strain data is an average obtained from the 8 integration points of solid element since fully integrated element formulation has been used for the composite skins modeling. The data recorded by sensors P1\_S2 and P2\_S2, which are located at the centre of the target back skin, were excluded here due to the signal loss emerged from the sensor-panel interface failure.

651  
652  
653  
654  
655  
656

It is observed in Fig. 15 that high-frequency oscillations arose in the strain histories of 5 mm PD model due to the sparse distribution of SPH particles. However, both the strain amplitude and the period of strain histories of the coarse model (dashed red curves) are very close to the fine model (continuous green curve). Therefore, in terms of strain histories, the coarse SPH model of gelatine projectile (Fig. 8 Fig-8) can successfully substitute the finer model reducing the computational time from 18 hours to 14.5 hours (-19.4%).

657  
658  
659  
660  
661  
662  
663

The correlation of simulations with the impact tests was focused on the time interval from the impact initiation ( $t=0$  ms) and the full projectile disintegration when the force drops to zero level ( $t=1.8$  ms); whereas it should be mentioned that the vibration monitoring of panels after the impact completion is out of the purpose of current work. The assessment of model is based on the following five evaluation criteria: a) the initial slope of strain curve which describes the strain rate in elastic regime, b) the oscillation amplitude which shows the maximum strain during the impact event, c) the rise time at which the maximum strain occurs, d) the decay time which is the time of strain decline from

664 maximum value to zero level and e) the trend of strain history up to 1.8 ms. Table 4 illustrates the  
 665 results of evaluation study for the four examined locations.

666 Starting from the last sensor, P2\_S3, it is observed that the numerical model can successfully capture  
 667 the initial strain rate ( $t < 0.05$  ms), the rise time and the trend of experimental strain history. But  
 668 significant divergence on strain amplitude is noted. The flattening of the strain signal at 0.12 ms might  
 669 be due to FBG being partially detached. At that moment, any relaxation of strain is not expected since  
 670 the impact has not been finished and no damage was observed. Therefore, the P2\_S3 sensor's data is  
 671 not valid to be used for the verification of HF model results. But the trend matching and the  
 672 synchronization between the experimental and numerical strain history are powerful qualitative  
 673 indicators that the actual panel response is similar with the numerical one. Unlike, the virtual strain  
 674 sensor at location P1\_S3 seems to predict excellently the maximum strain and rise time regardless  
 675 the problem's complexity, since the corresponding calculated errors are 6.1% and 3.5%, respectively.  
 676 Also, even though the interrogator's sampling frequency is marginal, the model's accuracy in the  
 677 prediction of strain variation with time is very good.

	Evaluation criteria				
Location	Accuracy on initial strain rate (qualitative)	Maximum strain ( $\mu\epsilon$ )	Rise time (ms)	Decay time (ms)	Accuracy on history trend (qualitative)
P1_S1	Medium	Sim.: -2120 Exp.: -1935 error: 9.5%	Sim.: 0.42 Exp.: 0.37 error: 13.5%	Sim.: 0.47 Exp.: 0.52 error: -9.6%	High
P2_S1	Extremely high	Sim.: -1840 Exp.: -1790 error: 2.7%	Sim.: 0.53 Exp.: 0.48 error: 10.4%	Sim.: 0.6 Exp.: 0.52 error: 15.3%	High
P1_S3	High	Sim.: -1190 Exp.: -1121 error: 6.1%	Sim.: 0.76 Exp.: 0.734 error: 3.5%	Sim.: 0.73 Exp.: 0.79 error: -7.5%	High
P2_S3	High	sensor debonding	Sim.: 0.73 Exp.: 0.72 error: 1.4%	Sim.: 0.96 Exp.: 0.86 error: 11.6%	High

678 Table 4. Evaluation criteria for the HF model validity

679 In the case of sensor P2\_S1, it was observed that the model's performance is extremely high in terms  
 680 of initial strain rate and the maximum strain. In particular, the virtual sensor has precisely recorded  
 681 the strain variation from  $t=0$  ms to  $t=0.32$  ms; whereas, the first two strain spikes have been computed  
 682 in a great extent. However, the numerical strain history presents a low hysteresis of 0.05 ms starting

683 from  $t=0.32$  ms, which increases to 0.26 ms for  $t>1.2$  ms. By the examination of model was noted  
684 that this temporal lag is induced due to the instant softening of panel structure caused by the upper  
685 skin damage enlargement. Nevertheless, it is concluded the aforementioned panel's softening does  
686 not seem to be of high significance since the peak strain values have not been changed remarkably in  
687 relation to experimental ones. A significant difference between the experimental and numerical  
688 damage would lead to a dramatic change to the panel deformation and, therefore, to the strain field at  
689 the rear face. Finally, concerning the P1\_S1 sensor, excellent matching between the experimental and  
690 numerical maximum strains (1<sup>st</sup> and 2<sup>nd</sup> peak values) is noted; the corresponding errors are 9.5% and  
691 5.4%, respectively.

#### 692 **4.4.3 Evolution of soft body deformation**

693  
694 In this section, the experimental observed and the numerically obtained history of deformations of  
695 the soft body are correlated. The sequence of the frames captured by the high-speed camera, starting  
696 from the initial contact until the gelatine projectile disintegration, are shown in Fig. 16. The snapshots  
697 demonstrate clearly the hydrodynamic behavior of gelatine projectile during the impact, which is  
698 consistent with the published experimental results in [17], [18]. More importantly, quantitative  
699 correlation between the numerically obtained and experimentally observed projectile diameter in  
700 contact with the target is clearly demonstrated in Fig. 16. The maximum difference between the  
701 numerical and the actual values is 5.5% for  $t=0.5$  ms, which is a very good agreement. Also, it can be  
702 concluded that the simulation results for the diameter raise rate agrees with the experiment and that  
703 the radial pressure distribution has been simulated properly. Finally, the final snapshot in Fig. 16  
704 suggests that no significant fibre and matrix damage exists on the outer ply of front skin since no  
705 visible cracks have developed. The same conclusion is validated by the simulation results.

706

707

708

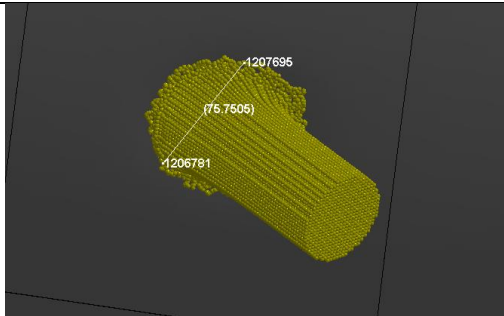
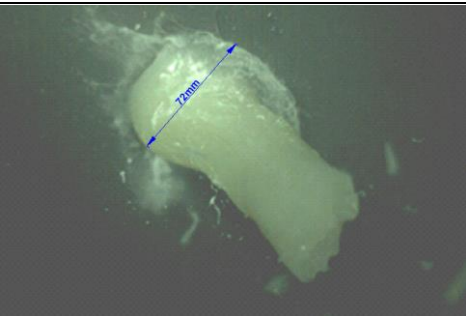
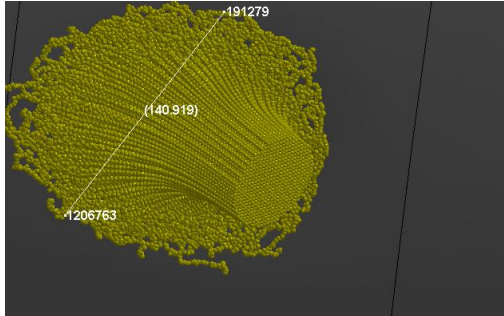
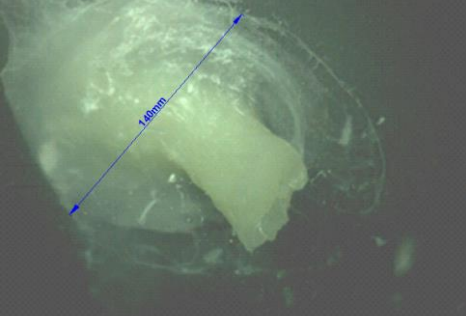
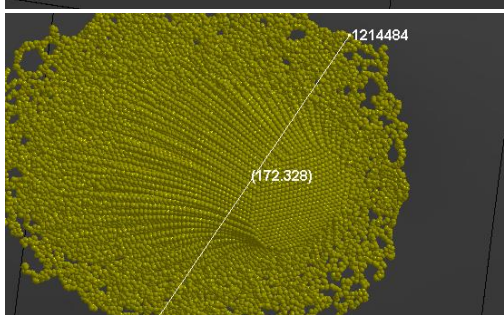
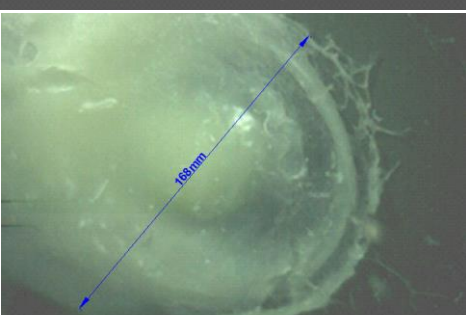
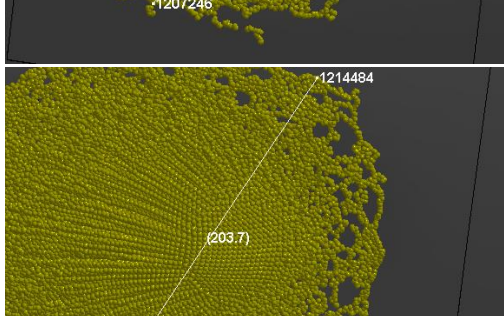
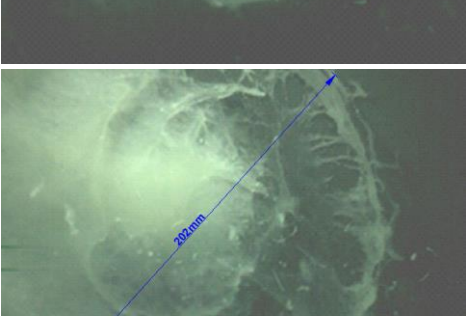
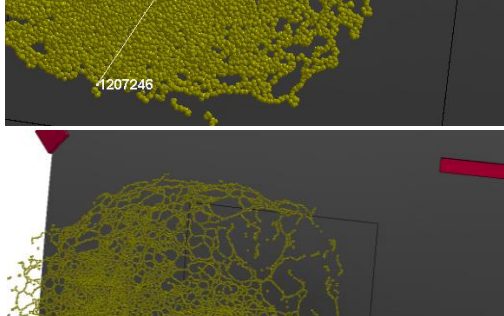
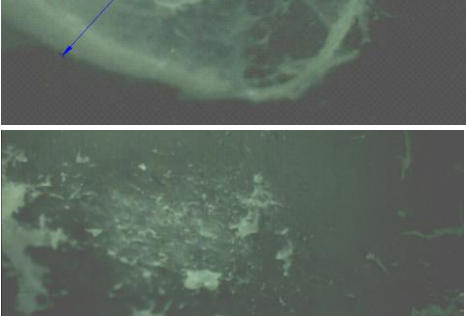
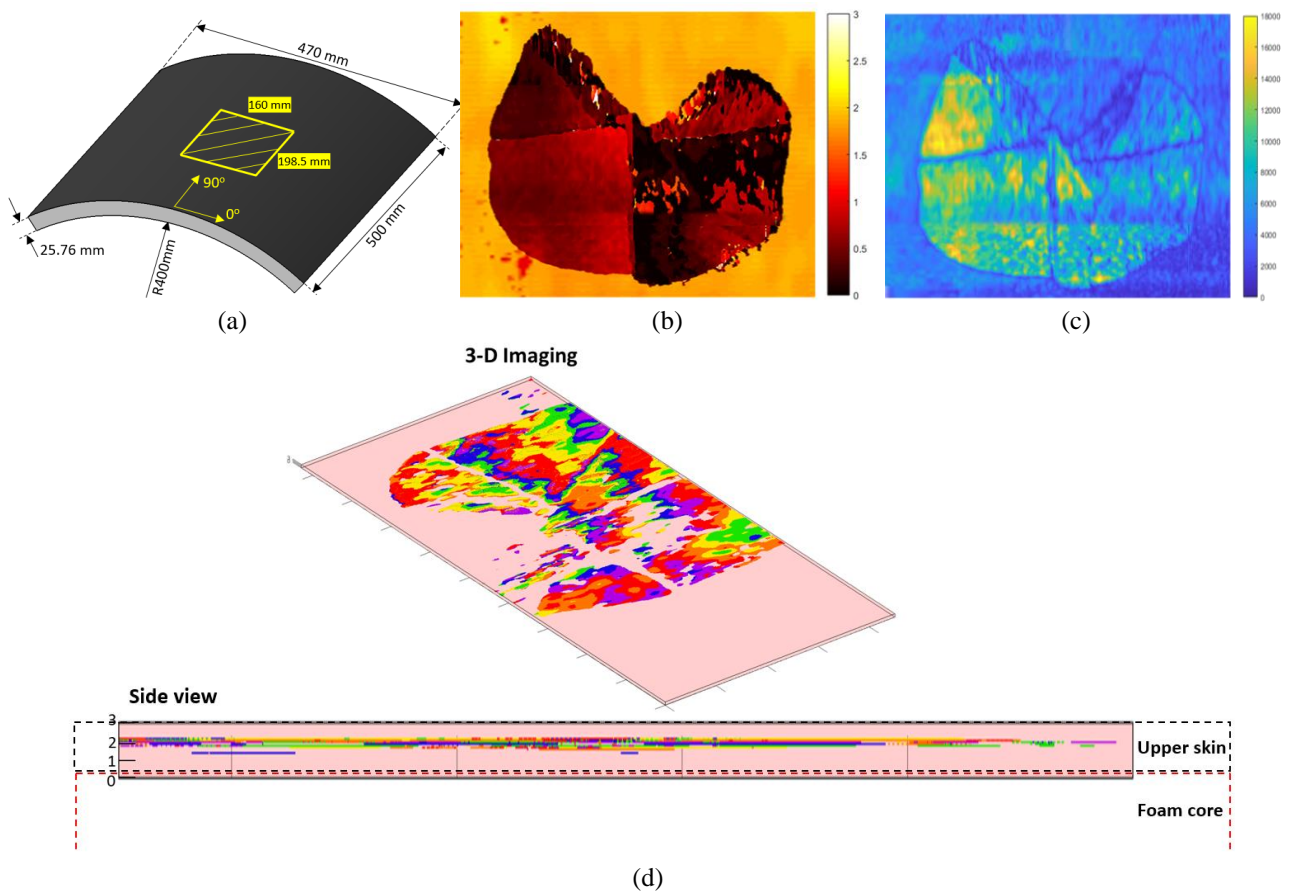
Time	HF model-2mm PD	Test	Comparison
0.50 ms			Num: 76 mm Exp: 72 mm
0.70 ms			Num: 141 mm Exp: 140 mm
0.90 ms			Num: 172 mm Exp: 168 mm
1.10 ms			Num: 204 mm Exp: 202 mm
1.80 ms			Num: - mm Exp: - mm

Fig. 16 Snapshots of impact taken from the HF simulation and the experimental test.

710 **4.4.4 Damage evaluation**

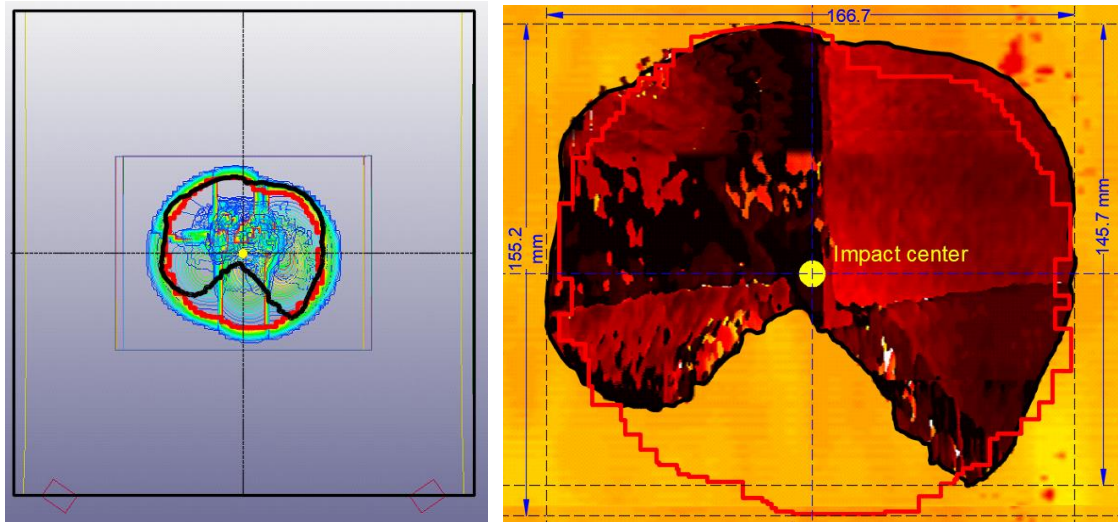
711 To characterize the internal damage occurred after the impact test, Phased Array Ultrasonic Testing  
712 (PAUT) investigations were conducted by UBATH. PAUT methods employ a pulser-receiver piezo-  
713 electric elements array to steer an ultrasonic constructed wave trough the material and capture any  
714 anomalous refractions or phase shift of the wave propagating back, due to internal defects. A 5MHz  
715 128 channels Phased-Array transducer was used to perform ultrasonic inspection of the impacted  
716 area. The scanned area of 198.5 mm x 160 mm is shown in Fig. 17a.



717 Fig. 17. Inspection results: (a) Scanned area, (b) Time of flight (TOF) C-scan damage imaging, (c)  
718 Amplitude C-scan, (d) 3-D damage imaging and side view.

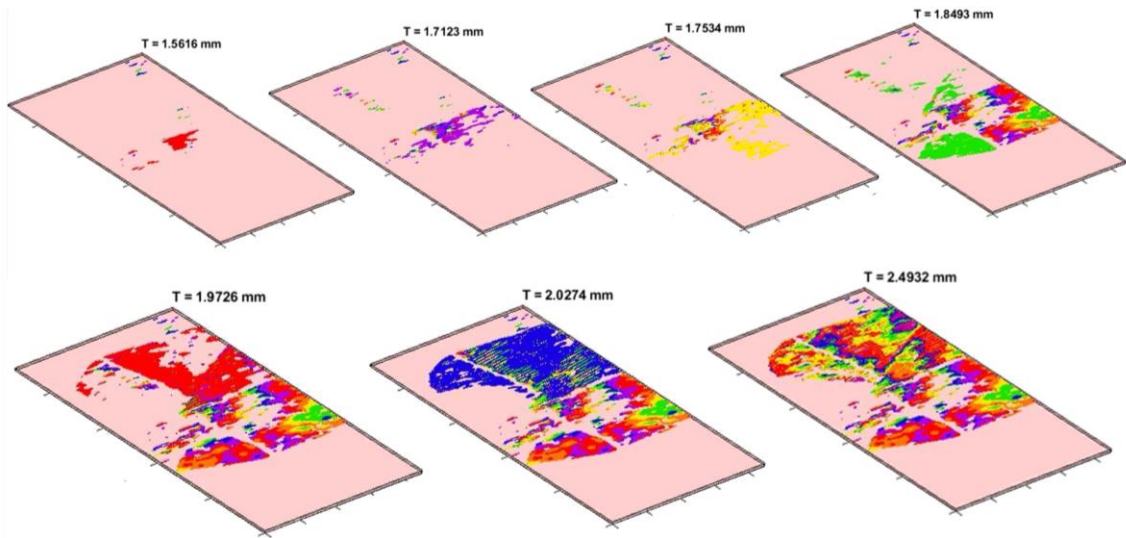
719  
720 The complex nature of an impact damage in sandwich panel is presented in the Time-of-flight (TOF)  
721 C-scan in Fig. 17b, where the color scale denotes the damage position inside the thickness; whereas,  
722 the amplitude of ultrasonic signal is shown in Fig. 17c. The amplitude peaks location can be related  
723 to the damage locations through the panel thickness. The total delaminated area by non-destructive  
724 evaluation is 16,875 mm<sup>2</sup>, whereas the cumulative numerical damage is slightly larger and equal to

725 19,228 mm<sup>2</sup> (Fig. 18). This discrepancy might be due to the asymmetric delamination pattern  
726 observed in the experimental results. A heterogeneity of gelatine projectile and an imperfection in the  
727 target material might be possible reasons. However, the extent of experimental damage in both  
728 directions have been precisely captured by the HF model (Fig. 18).



729 Fig. 18. Comparison of total numerical delamination damage (red line) with the experimental one (black  
730 line).

731 As shown in Fig. 17d and Fig. 19, impact cracks penetrate down to a depth of 1.439 mm (i.e. 1.561  
732 mm from the reference bottom  $T=0$  mm), and delamination only grew in the upper part of the skin,  
733 at a depth of 1.150 mm (i.e. 1.849 mm from the reference bottom  $T=0$  mm). Therefore, it can be  
734 concluded that the impact damage has been confined at the upper skin of panel, and no interfacial  
735 damage between foam and upper skin exists ( $T=0.12$ mm).

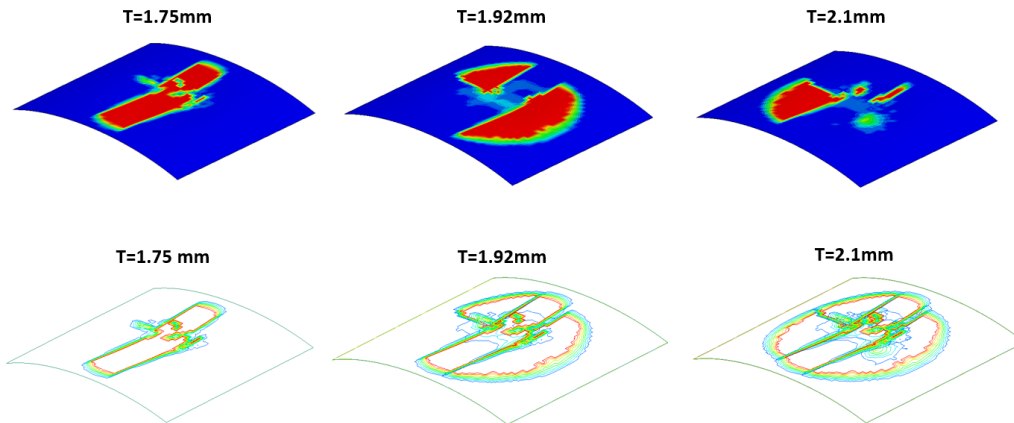


736

737

738

Fig. 19 Framed 3D-view of experimental damage at different thickness position T



739

740

741

Fig. 20 Framed 3D-view of numerical interlaminar damage at upper skin: per interface for 3 sequential interfaces (top), cumulative damage (bottom).

742

743

744

The same conclusion can be also extracted by the numerical simulation, where the interface damage occurs only to three interfaces at the middle of upper skin. Fig. 20 illustrates the numerical interlaminar damage at the different positions.

745

## 5 Development of low-fidelity (LF) model

746

### 5.1.1 LF modeling approach

747

748

749

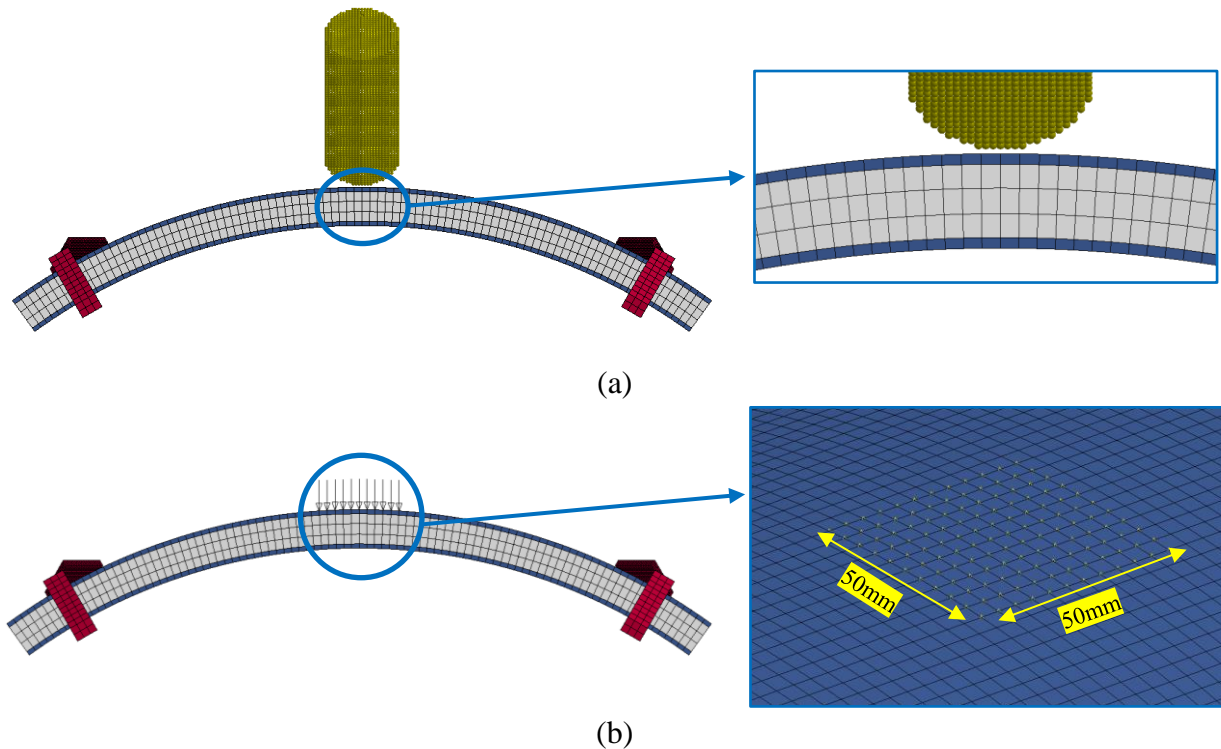
LF model targets to reduce the high computational time of HF one keeping the modeling accuracy to an acceptable level. It is well known that the most representative the model is, the most time-consuming it is. Therefore, for the sake of computational efficiency, the current approach is focused

750 on the relegation of accuracy of damage modeling applying layered solid elements for skins. The 8-  
751 node layered solid elements in LS-DYNA use one integration point per layer for computational  
752 efficiency, and no limit exists to the number of integration points though the thickness [35]. The  
753 present study was based on the thick shell form 5 of LS-DYNA, where the through-thickness  
754 orientation of the integration points provides the ability to the element to model the stacking sequence  
755 and to capture the bending stiffness and the through the thickness stresses like a 3D solid element  
756 [29]. The validity of this element type was verified to 3-point bending loading in [Table 3](#)~~Table-3~~.  
757 From the material model point of view, the same constitutive and failure law (MAT\_54) were applied  
758 as previously. The second modification in relation to the HF simulation approach is that no cohesive  
759 elements are implemented into the CFRP faces for the capturing of interlaminar damage. Thus, the  
760 running time was significantly reduced. Regarding the foam material model and the interface between  
761 that and faces, no modification is done. The LF model of sandwich panel with high-density SPH  
762 model of gelatine projectile is shown in Fig. 21. All simulations were executed using an Intel Xeon  
763 3.5 GHz processor and a 16 GB RAM, and the running time is given in Table 5.

764 In parallel, a model with impulse-equivalent loading was built for the further mitigation of  
765 computational time (Fig. 21) and the estimation of its divergence from the LF/SPH model since it  
766 will constitute the LF numerical tool of digital-twin methodology. Essentially, it was based on the LF  
767 modelling approach for the case of sandwich panel, and it uses a time-variable, distributed, and  
768 impulse-equivalent load for the representation of bird impact force. The equivalent load is applied on  
769 a rectangular area, whose the edge length is equal to projectile's diameter (50 mm), and which  
770 represents the main contact area during the impact. It is observed that the shape of loading for 45°  
771 oblique soft-body impact, shown in [Fig. 22](#)~~Fig. 22~~, looks like a symmetrical bell; therefore, it can be  
772 simplified as a tri-linear loading curve. The equivalent loading curve is created keeping the values of  
773 impulse and maximum force equal to those of HF model and adopting similar loading and unloading  
774 rates. The applied equivalent load is presented in 'a' graph of [Fig. 22](#)~~Fig. 22~~, whilst the computational



775 time of three different models is presented in Table 5. Using LF/equivalent model, a 68% decrease  
 776 of running time was achieved in comparison to the LF/SPH model.



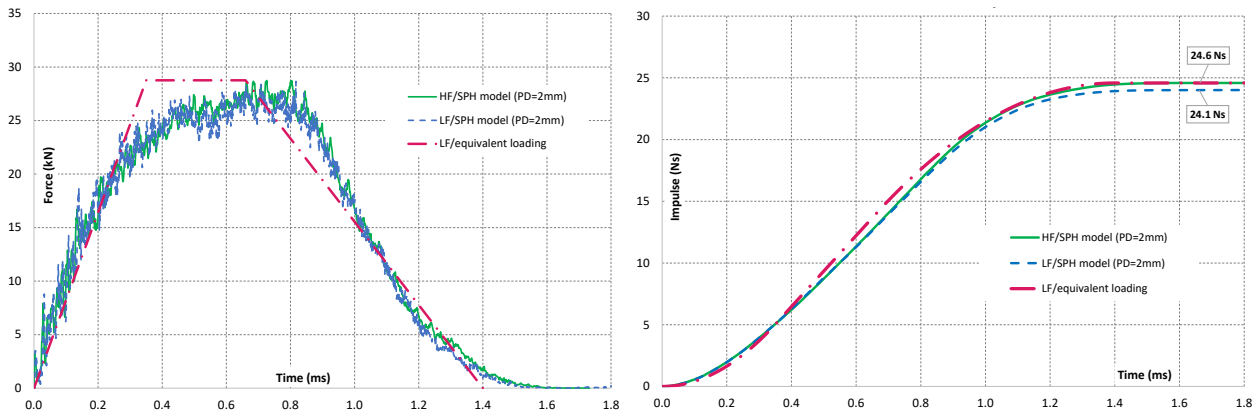
777 Fig. 21 LF model using a) SPH projectile (LF/SPH) and b) impulse-equivalent loading (LF/equivalent)  
 778

Model	Computational time
HF/SPH model (2 mm PD)	18.0 hr
HF/SPH model (5 mm PD)	14.5 hr
LF/SPH model (2 mm PD)	19 min
LF/equivalent	6 min

779 Table 5. Running time of models (up to the instant of 2.0 ms)

780 **5.1.2 Models comparison**

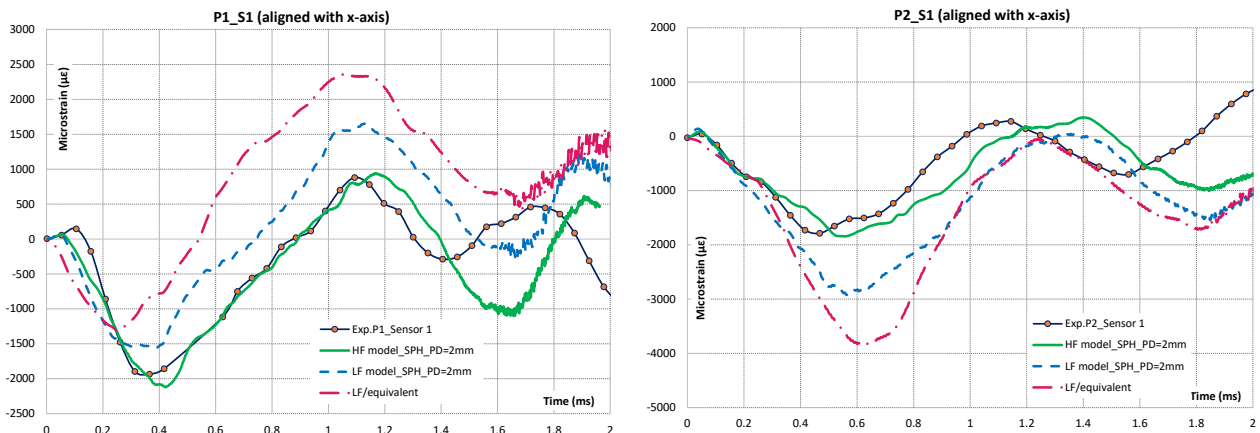
781 Looking the results of HF/SPH and LF/SPH models in [Fig. 22](#), it is noted that the impulse of  
 782 latter one diverges only by 2.0% from that of former. Essentially, it slightly underestimates the  
 783 impulse of HF model. Regarding the force-time history, it is inferred that the difference is almost  
 784 negligible since the peak force error between the two analyses is equal to 0.36%; whereas, the impact  
 785 durations are identical.



786 Fig. 22 HF/SPH vs. LF/SPH vs. LF/equivalent contact force histories and impulse values

787 As far as the strain histories are concerned, it was observed that both LF/SPH model and LF model  
 788 with impulse-equivalent loading are in good agreement with the trend of strain history derived from  
 789 HF model (Fig. 23). The LF models might be limited in predicting the actual strain rate of  
 790 loading and unloading as well as the actual peak strain values due to their inability to simulate  
 791 accurately the skins' damage. However, in Fig. 23, the rise and decay times are relatively close  
 792 to those of HF model, which are quantitative indicators that the global vibrational response of panel  
 793 structure can be estimated. The LF model is headed in the right direction, therefore it can be used as  
 794 a quick tool for the identification of the impact loading correlating the numerical strain histories with  
 795 experimental ones. The LF model should be capable of estimating the required loading data, such as  
 796 loading magnitude, the impact duration, the total impulse, the impact angle, and the position of  
 797 impact, which is obviously unknown during the digital-twin assisted damage diagnosis.

798



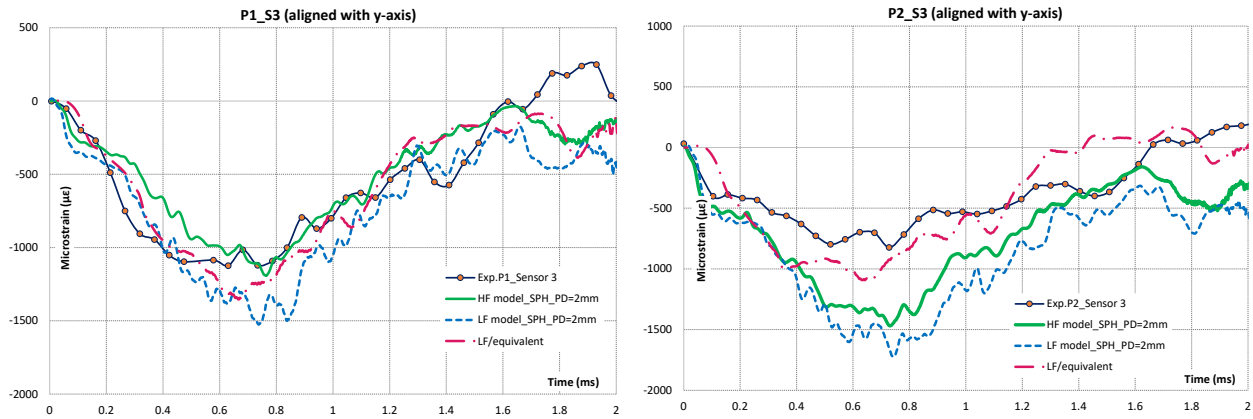
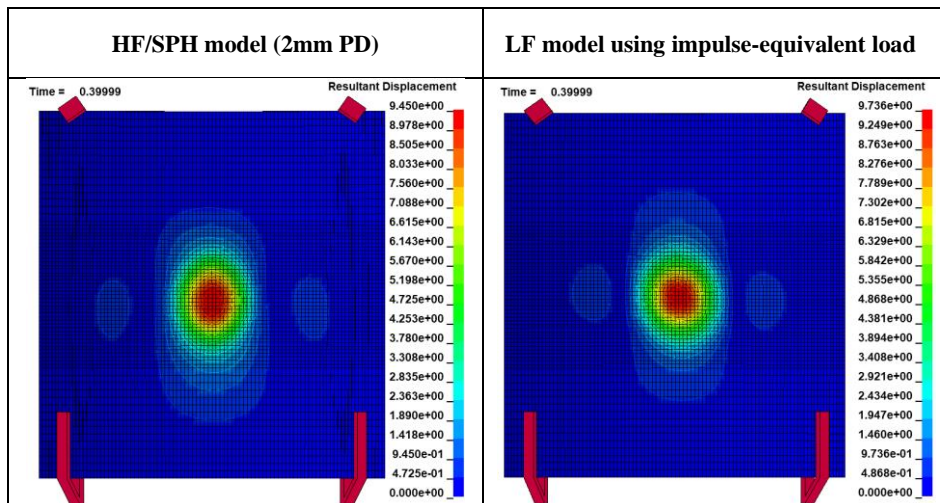
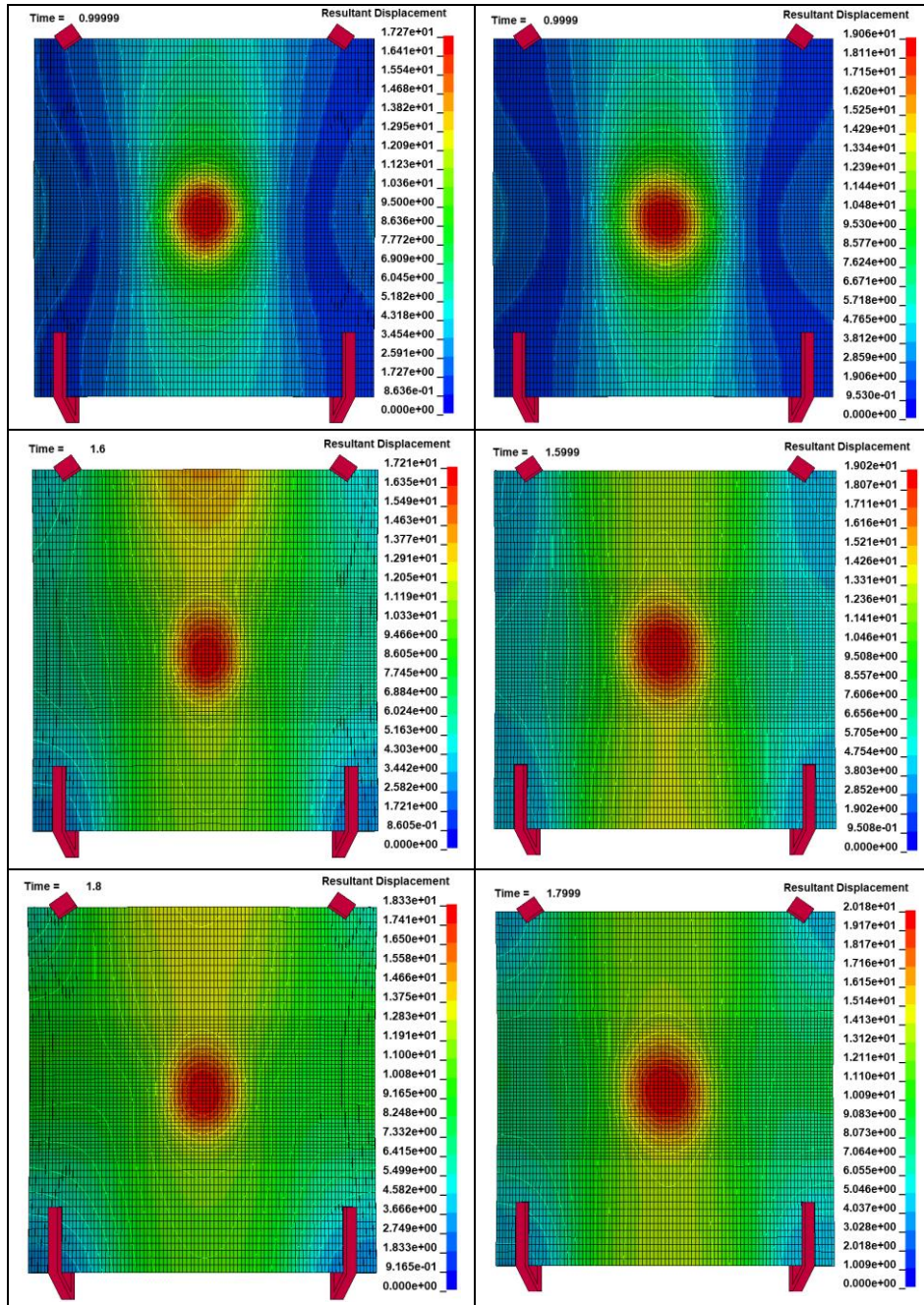


Fig. 23 HF/SPH vs. LF/SPH vs. LF/equivalent numerical strain-time profiles

799 The Fig. 24 compares the fringes of resultant displacements of the HF/SPH model with those of the  
 800 LF/equivalent model at three different time instants. The contour lines display graphically the  
 801 boundaries of displacement change. In general, similarity between the two models can be observed  
 802 in terms of the displacement magnitude, the rate of deformation and the deformed area. Small  
 803 differences on the extension of the deformed area and the maximum displacement can reasonably  
 804 exist, and they are acceptable due to the difference on the method of load application. In the case of  
 805 HF model, the load is both temporally and spatially variable using a moving SPH projectile, whereas,  
 806 in the case of LF with impulse-equivalent loading, it is only temporally variable. In conclusion, it can  
 807 be inferred that the LF model can sufficiently approximate the HF model deformation.  
 808  
 809





810 Fig. 24 Fringe of resultant displacements of HF/SPH model and LF/equivalent model (mm)

811 **6 Conclusions**

812 The dynamic response of a curved composite sandwich panel subjected to soft body impact was  
 813 investigated both numerically and experimentally. In particular, two computational tools with  
 814 different simulation fidelity and computational cost were developed. Firstly, a high-fidelity FE/SPH  
 815 model was established, and it was validated using the soft body impact experimental results.  
 816 Afterwards, a time-efficient low-fidelity model was created and was correlated with the former. It

817 was concluded that the high-fidelity model can sufficiently approximate the experimental strain  
818 histories recorded by the FBG sensors, with the experimental delamination area accurately predicted.  
819 On the other hand, the LF model can rapidly predict the global vibrational response of sandwich  
820 panel; further transformation of the LF model based on applying an impulse-equivalent loading  
821 instead of SPH model of the soft projectile, reduced the total computational time by 68%.  
822 In conclusion, the LF model can be used as a quick numerical guide for the identification of impact  
823 loading conditions comparing the numerical results with the real-time strain histories taken from FBG  
824 sensors. Knowing the loading condition, HF model can employ as virtual detector and estimator of  
825 sandwich panel damage avoiding the traditional fault diagnosis techniques. The development of  
826 digital-twin-assisted damage diagnosis method using the current numerical tools will be presented in  
827 a future paper. Finally, the assessment of influence of strain rate and the impact conditions (i.e.,  
828 impact velocity and angle) constitutes future work.

## 829 **Acknowledgements**

830 This work has been supported by EU, H2020 Smart, Green and Integrated Transport, Aviation  
831 program under the acronym EXTREME (Project reference 636549). The authors would like to  
832 specially thank Technobis TFT-FOS for its support on the processing of experimental data.

## 833 **Bibliography**

834

- [1] Accident Report. Accident occurred to the Boeing B737-800AS aircraft, Italian Agency for Fight Safety, Rome, November 2018
- [2] Accident Report. Loss of Thrust in both engines after encountering a flock of birds and subsequent ditching on Hudson river US Airways Flight 1549, National Transportation Safety Board, Washington, May 2010
- [3] Hereson H. EASA views on Rotorcraft bird strike threat. European Aviation Safety Agency, 2016.
- [4] Certification Specifications and Acceptance Means of Compliance for Large Rotorcraft CS-29-Amendment 4, European Aviation Safety Agency, November 2016.

- [5] Lavoie MA, Gakwaya A, Ensan M, Zimcik D. Review of existing numerical methods and validation procedure available for bird strike modeling. *Int. Conf. Comp. Exper. Eng. Sci.*, 2007; 2:4. doi:10.3970/icces.2007.002.111
- [6] Smojver I, Ivancevic D. Bird strike damage analysis in aircraft structures using Abaqus/Explicit and coupled Eulerian Lagrangian approach. *Comp. Sci. Technol.*, 2011; 71:4. <https://doi.org/10.1016/j.compscitech.2010.12.024>
- [7] Jang JH, Ahn SH. Bird-strike damage analysis and preliminary design of composite radome structure using smoothed particle hydrodynamics. *Appl. Compos. Mater.*, 2019; 26. doi.org/10.1007/s10443-018-9750-9.
- [8] Guida M, Marulo F, Meo M, Russo S. Certification by birdstrike analysis on C27J fullscale ribless composite leading edge. *Int. J. Impact Eng.*, 2013; 54. <https://doi.org/10.1016/j.ijimpeng.2012.10.002>
- [9] Hedayati R, Ziaei-Rad S. A new bird model and the effect of bird geometry in impacts from various orientations. *Aerosp. Sci. Technol.*, 2013; 28:1. <https://doi.org/10.1016/j.ast.2012.09.002>
- [10] Hedayati R, Ziaei-Rad S. Effect of bird geometry and orientation on bird-target impact analysis using SPH method. *Int. J. Crashworthiness*, 2012; 17:4. <https://doi.org/10.1080/13588265.2012.674333>
- [11] Vignjevic R, Campbell J, Hughes K, et al. Soft body impact resistance of composite foam core sandwich panels with unidirectional corrugated and tubular reinforcements. *Int. J. Impact Eng.*, 2019; 132. <https://doi.org/10.1016/j.ijimpeng.2019.103320>
- [12] Heimbs S, Bergmann T. High-velocity impact behavior of prestressed composite plates under bird strike loading. *Int. J. Aerospace Eng.*, 2012; 372167. doi:10.1155/2012/372167, 2012.
- [13] Eren Z, Taratoglu S, Balkan D, Mecitoglu Z. Modeling of bird strike on a composite helicopter rotor blade. *J. AIAA*, 2017. <https://doi.org/10.2514/6.2017-1991>, 2017.
- [14] Qiu J, Wang D, Chuanjun L, Chen L, Huang H, Sun Q. Dynamic response of bird strike on honeycomb-based sandwich panels of composite leading edge. *Int. J. Crashworthiness*, 2020. <https://doi.org/10.1080/13588265.2020.1718466>.
- Barber J, Taylor H, Wilbeck J. Bird Impact Forces and Pressures on Rigid and Compliant
- [15] Targets. Technical Report AFFDL-TR-77-60, University of Dayton, Ohio, 1978.
- [16] Wilbeck J. Impact behavior of low strength projectiles. Technical Report AFML-TR-77-134. Wright-Patterson Air Force Base, Ohio, 1978.

- [17] Lavoie MA, Gakwaya A, Ensan M, Zimcik DG, Nandlall D. Bird's substitute tests results and evaluation of available numerical methods. *Int. J. Impact Eng.*, 2009; 36:10-11. <https://doi.org/10.1016/j.ijimpeng.2009.03.009>
- [18] Pernas-Sanchez J, Artero-Guerrero J, Varas D, Lopez-Puente J. Artificial bird strike on Hopkinson tube device: experimental and numerical analysis. *Int. J. Impact Eng.*, 2020; 138. <https://doi.org/10.1016/j.ijimpeng.2019.103477>
- [19] Tuegel E.J, Ingrassia A, Eason T, Spottswood S.M. Reengineering aircraft structural life prediction using a digital twin. *Int. J. Aerospace Eng.*, 2011; 154798. <https://doi.org/10.1155/2011/154798>
- [20] Glaessgen E, Stargel D. The digital twin paradigm for future NASA and U.S. Air Force Vehicles. 53rd AIAA/ASME/ASCE/AHS/ASC Conf., Honolulu, Hawaii, April 2012. <https://doi.org/10.2514/6.2012-1818>
- [21] Qi Q, Tao F, Hu T, Anwer N, Liu A, Wei Y, Wang L, Nee A. Enabling technologies and tools for digital twin. *J. Manuf. Syst.*, 2019. <https://doi.org/10.1016/j.jmsy.2019.10.001>
- [22] Drawing Database. Agusta Westland AW 139, <http://drawingdatabase.com>
- [23] Technical data sheet CYCOM 977-2 epoxy resin system. Rev:01, Cytec Engineering Materials, 2012.
- [24] Giannaros E, Kotzakolios A, Sotiriadis G, Kostopoulos V. A multi-stage material model calibration procedure for enhancing numerical solution fidelity in the case of impact loading of composites. *J. Compos. Mater.*, 2020; 55:1. <https://doi.org/10.1177/0021998320944992>
- [25] Elan-tech AS 89.1/ AW 89.2 material data sheet, ELANTAS Italia S.r.l.
- [26] Giannaros E, Kotzakolios A, Kostopoulos V, Campoli G. Hypervelocity impact response of CFRP laminates using smoothed particle hydrodynamics method: Implementation and Validation. *Int. J. Impact Eng.* , 2019; 123. <https://doi.org/10.1016/j.ijimpeng.2018.09.016>
- [27] Moura M.F.S.F, Campilho R.D.S.G, Goncalves J.P.M. Pure mode II fracture characterization of composite bonded joints. *Int. J. Solids Struct.*, 2009; 46:6. <https://doi.org/10.1016/j.ijsolstr.2008.12.001>
- [28] Tsokanas P, Loutas T, Kotsinis G, Brink W.M., Nijhuis P. Strain energy release rate and mode partitioning of moment-loaded elastically coupled laminated beams with hygrothermal stresses. *Compos. Struct.*, 2020; 113237. <https://doi.org/10.1016/j.compstruct.2020.113237>, 2020.

- [29] Hallquist J. LS-DYNA theory manual, 2006. Livermore Software Tehnology Corporation.
- [30] Tabiei A, Zhang W. A zero thickness cohesive element approach for dynamic crack propagation using LS-DYNA. 15th Int. LS-DYNA Users Conf. , Dearborn, Michigan, 2018.
- [31] Knight N. Factors influencing progressive failure analysis predictions for laminated composite structure. J. AIAA, 2008; 092407. <https://doi.org/10.2514/6.2008-2108>
- [32] Gu J, Chen P. Some modifications of Hashin's failure criteria for unidirectional composite materials. Compos. Struct., 2017; 182. <https://doi.org/10.1016/j.compstruct.2017.09.011>.
- [33] AIREX C70 material Data sheet EN 2010 V02, 2010.
- [34] Wang R, Proust G, L. Shen. Characterizations of mechanical properties of two polymer foams under quasi-static and impact loading, University of Sydney, School of Civil Engineering, 2017. <https://poster.quantumfi.net/images/posters/2017/pdf/characterizations-of-mechanical-properties-of-two-polymer-foams-under-quasi-static-and-impact-loading.pdf>
- [35] Hallquist J. LS-DYNA keyword user's manual R8.0. Livermore Software Tehnology Corporation.
- [36] Fatehi R, Manzari M.T. Error estimation in smoothed particle hydrodynamics and a new scheme for second derivatives. J. Comput. Math. Appl., 2011; 61:2. <https://doi.org/10.1016/j.camwa.2010.11.028>
- [37] Lacomme J. SPH formulations: New developments in LS-DYNA. 7th European LS-DYNA users conference, Salzburg, 2009.
- [38] Abrate S. Soft impacts on aerospace structures. Prog. Aerosp. Sci. 2016;81. <https://doi.org/10.1016/j.paerosci.2015.11.005>
- [39] Challita A. Validation of a bird substitute for the development and qualification of aircraft transpareciles. Technical Report AFWAL-TR-80-3098. University of Dayton, Ohio, 1980.
- [40] Hedayati R, Ziaei-Rad S, Eyvazian A, Hamouda A. Bird strike analysis on a typical helicopter windshield with different lay-ups. J. Mech. Sci. Technol., 2014; 28. <https://doi.org/10.1007/s12206-014-0125-3>
- [41] Zenkert Dan. An introduction to sandwich structures. Student Edition, Stockholm, December 2005.
- [42] Johnson A, Holzapfel M. Modeling soft body impact on composite structures. Compos. Struct., 2003; 61:1-2. [https://doi.org/10.1016/S0263-8223\(03\)00033-3](https://doi.org/10.1016/S0263-8223(03)00033-3)
- [43] Walvekar V, Chandrashekhar T, Bhonge P, Lankarani H. Birdstrike analysis on leading edge of an aircraft wing using a smooth particle hydrodynamics bird model. ASME Int. Mech. Eng. Congr., 2012; IMECE2010-37667. <https://doi.org/10.1115/IMECE2010-37667>
- [44] The hazared posed to aircraft by brids. Australian Transport Safety Bureau (ATSB), 2002. ISBN 1 877071 23 4

835

836