Prediction of Lightning Strike-Induced Damage of Composite Aircraft Structures

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1. ABSTRACT

The present study provides a short overview of the aircraft lightning strike phenomenon as well as a modelling and simulation approach for lightning-induced damage of protected CFRP structures. The intra-laminar and inter-laminar lightning-induced damage and the mechanical response of protected CFRP structures are numerically calculated. Structural FE calculations are performed using the commercial software Abaqus/Explicit. The progressive damage analysis, including inter-laminar damage simulation by means of a cohesive zone formulation at the interface between UD CFRP plies, is used to assess the effect of damage on the response of the CFRP laminates subjected to lightning strike loads. It is shown that taking damage mechanics into account is essential in the representation of the dynamic response of the laminated CFRP plate subjected to a transient lightning strike event. The obtained numerical results using the developed numerical approach agree well with experimental data and provide a new insight on the physics of lightning-induced damage of protected CFRP structures subjected to a lightning strike.

2. INTRODUCTION

With the growing interest to construct more efficient aircraft, more components are designed out of lightweight composite structures. Composites like CFRP are however poorer electrical and thermal conductors than aluminium alloys that have been used in aircraft and aerospace construction as the principal material. Without additional design features, CFRP structures are susceptible to severe damage in the event of a lightning strike. Moreover, electromagnetic fields can penetrate through less electrical conducting CFRP regions inside the airplane. Therefore, there is a need for lightning protection and electromagnetic (EM) shielding measures of aircraft CFRP structures. In this context it is, first of all, necessary to understand the underlying physical mechanisms of lightning strike-induced damage. Furthermore, theoretical models for the interpretation of experimental tests as well as for reliable numerical prediction of lightning-induced damage of composite structures have to be developed.

At the arc attachment areas, several lightning direct effects can be distinguished, such as thermal effects caused by the electric arc, thermal and electrodynamic effects induced by circulation of the lightning current, and mechanical effects from air and surface shock waves. To prevent hazardous events such as catastrophic structural damage, electrical shocks to occupants, loss of flight control capability, or ignition of fuel vapours, airframes need to be designed against these lightning direct effects [1] .

The damage caused by lightning strikes to protected CFRP airframe structures is usually quite severe. Its amount depends on several factors, such as the characteristics of the lightning strike protection (LSP) layer and the dielectric coating above it. While the surface weight and electrical conductivity of the material that forms the LSP layer can reduce the extent of damage caused to the composite structures (due to the reduced electrical resistivity) [2] , the thickness (or strictly speaking the surface weight) and the mechanical properties of the dielectric coating (i.e. the paint) affect adversely the thermal-mechanical damage of composite structures. This effect can be explained by the generated Joule heat that leads to a quick rise of temperature in the material confined by the paint coating covering the structure up to an explosion phase. This inertial confinement leads to an enhanced overpressure generated on the surface, before the paint is ejected at a later stage [3] [4] . It may also prevent the arc from consuming the LSP layer (by melting or vaporization), forcing the current to penetrate into the carbon plies [5] . Consequently, if the paint is too thick, the effectiveness of the LSP layer may become compromised [6] [7] .

Different types of damage can be distinguished according to their main source [8] [9] . Surface damage is mainly attributed to the thermal effects, involving heating, melting or vaporization of the metallic protection and possibly degradation (tufting, burning) of the first carbon plies. Fibre fracture in the outer plies, longitudinal splitting and bulging around the lightning strike area are characteristic lightning-induced damage footprints that tend to be concentrated over the top plies [1] [10] . Electrical and thermal models are used to evaluate the energy injected in the material by Joule effect and by heat transfer from the arc root. The amount of predicted pyrolyzed material and liquid or vapour phases due to melting or vaporization of the protection and underlying material provides a time dependent damaged area in good agreement with test results.

Bulk damage (including debonding, micro-cracking, fibre damage, and, more importantly, delamination), on the other hand, is mainly attributed to the mechanical effects due to the forces generated at the surface of the sample [6] , particularly important during the current peaks [11] . The main contributor is usually the surface explosion induced by the vaporization of the metallic protection and first carbon plies. In painted structures, the overpressure of the vaporizing materials is enhanced by the confining effect of the paint [6] . Unlike thermally-induced surface damage, bulk damage can remain visually undetectable and still extend well beyond the visible damage zone, bearing it extremely important in the analysis of the tolerance of composite structures to lightning strikes. The present work is focused on the analysis and simulation of this type of lightning-induced damage.

The direct effects of lightning strikes on protected and unprotected composite structures are currently assessed by means of complex and costly physical tests [3] , where samples are subjected to discharges to simulate natural lightnings. From the analysis of the induced damage, it is then decided whether the material is adequately protected against lightning strikes or not [3] . This approach is not only highly empirical, but it can also be sensitive to physical and mechanical factors, making tests conducted at different organisations often incomparable and hindering the right judgement of the lightning resistance of composite structures and of the effectiveness of different LSP layers [12] .

So far, experimental observations show that, while the lightning parameters defining the impulse waveform have a strong effect on certain damage modes [13] , the effect of sample size and geometry is only moderate [13] [14] . In fact, due to the reduced time and space domain of the lightning strike event onto the panel, the test boundary conditions do not have a great influence on the local deformation of the sample [14] , which has also been confirmed through plane-stress lightning-induced damage simulations [15] . Moreover, due to the local nature of the lightning strike event, for sufficiently large and sufficiently thick laminates such as the ones often used in simulated lightning strike testing, structural damage should not only be independent of boundary conditions and geometry, but also of laminate thickness.

Damage extension is also affected by the laminate stacking sequence [16] [17] . For example, the lightning-induced damage in unidirectional (UD) laminates, which is mostly limited to the matrix material [18] , can be substantially different from that of multi-directional laminates. In the latter, experimental evidence shows that, while the damage depth seems to be independent on the lay-up [16] , the projected damage area is not [16] [17] . Ply clustering and the relative fibre orientation (mismatch angle between consecutive plies) are apparently the most influential factors. Thicker ply blocks usually lead to a larger projected damage area [16] [19] . The relative fibre orientation, on the other hand, mainly affects the shape of the projected damage area [16] [17] , which can lead to a different damage extension.

But as the industry moves towards hybrid experimental/numerical design and certification processes, the development of reliable models that can accurately capture the main effects of lightning strikes in the context of damage tolerance criteria becomes crucial. In the present work, physically-based models of the mechanical loads induced by lightning strikes [15] are implemented into a three-dimensional (3D) FE framework and combined with a modified continuum damage mechanics model for CFRPs [20] to predict mechanical damage in composite structures subjected to this type of events. The objective is to define a robust virtual testing framework that can predict the tolerance of CFRP structures subjected to general loading conditions, including lightning strike events, and aid the design of composite aerostructures.

3. MECHANICAL LIGHTNING LOADS

The most severe mechanical damage at and around the lightning attachment area of protected and unprotected airframe composite parts occurs during the transient A or D current components (Fig. 1). The shock waves generated by the exploding vaporized materials at the arc attachment area (or within the material volume extremely heated by the generated Joule heat) confined between the composite material and the protective paint layers are the main reason for this severe damage [3] [21] [22] [23] . However, the contributions from the shock waves caused by the supersonic expansion of the hot plasma channel [23] [24] [25] [26] and the magnetic volume forces (magnetic pressures) caused by the impressed current flow in the electrically conducting structures [23] [27] cannot be neglected (Fig. 2).

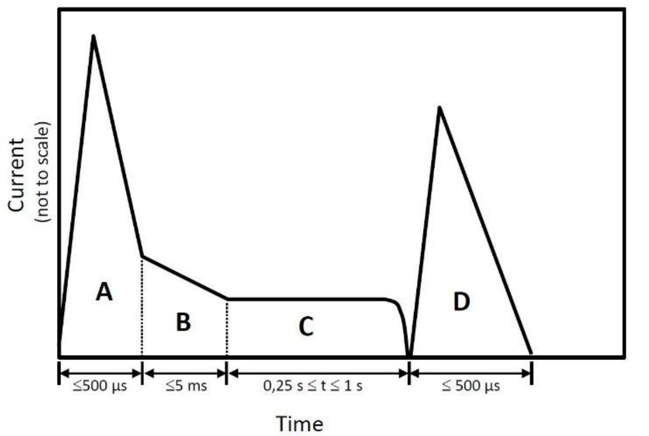


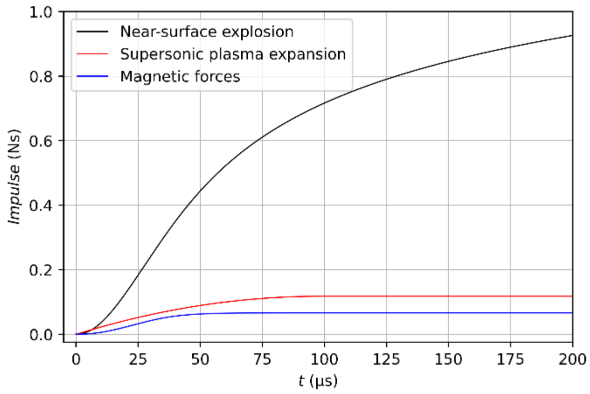
Fig. 1. Schematic illustration of the waveform for return current. It should be noted that the amplitude of the continuing current can be more than 1,000 times smaller than the peak value of the transient return stroke component, but it can last for more than 1,000 times longer.

Experimental observations show that, when the arc root expands radially, the metallic protection is continuously removed from the top of the CFRP sample [23] . In such scenario, "thermal" damage of the first UD ply is rather small [5] . Thus, in a first approximation, the thermal and thermal-mechanical effects can be neglected, and it can be assumed that the mechanical damage of the protected CFRP samples is mainly caused by the "mechanical force effects" due to transient current components.

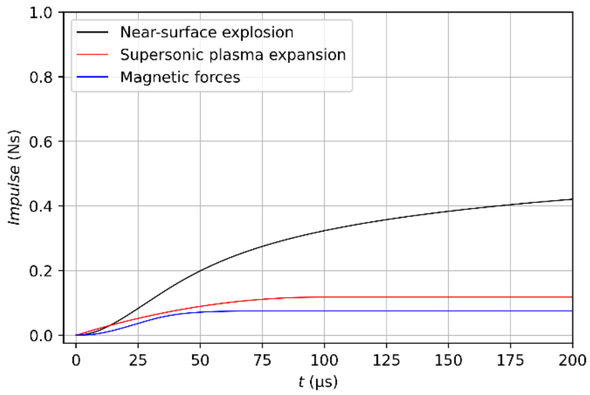
Following Karch *et al.* [15] , the mechanical forces resulting (i) from the near-surface explosion of the LSP layer, including the appropriate arc root behaviour on the surface of the sample, (ii) from the supersonic plasma expansion and (iii) from the magnetic field caused by the impressed current flow in the electrically conducting structures are taken into account. The FV approach is used to calculate the plasma arc root expansion and the FE approach is used to determine the effects of magnetic forces and of the shock waves due to supersonic lightning channel expansion.

The associated pressure distributions are implemented in the FE software Abaqus [28] using the user-defined subroutine VDLOAD. Fig. 2 shows the contributions to the impulse resulting from the different mechanical force effects for CFRP plates protected with expanded copper foil (ECF) with a surface weight of 73.3 g/m^2 – 2Cu4-100FA (ECF 73, Fig. 2a) – and 195.3 g/m^2 – 3Cu7-100FA (ECF 195, Fig. 2b) –

subjected to a transient D current waveform with a time to peak of 26.8 µs, a decay time of 48.5 µs and a peak current of 96.4 kA [3] , whose derivation is detailed in Ref. [15] .



(a) CFRP with 73.3 g/m^2 ECF and a 300 µm paint layer (ECF 73)



(b) CFRP with 195.3 g/m^2 ECF and a 300 µm paint layer (ECF 195)

Fig. 2. Contributions to the impulse resulting from the different transient mechanical force effects.

4. FE ANALYSIS

4.1 FE model

An FE model with solid elements was used in conjunction with a modification [20] to the continuum damage mechanics model proposed by Maimí *et al.* [29] [30] to represent failure of the composite plies. This damage model represents the onset of each intralaminar damage mechanism by means of appropriate damage initiation criteria [20] [29] and damage progression through appropriate damage evolution laws [20] [30] . Each ply, which is assumed transversely isotropic, must be represented explicitly in the FE model. To account for ply thickness effects, *in situ* strengths are defined as a function of ply thickness according to Camanho *et al.* [31] .

This modified version of the continuum damage mechanics model has a decoupled longitudinal tensile and compressive elastic behaviour that accounts for different stiffness in tension and in compression parallel to the fibre direction, and it uses bilinear softening laws to model longitudinal damage growth in tension and in compression, including a residual stress plateau in compression to more accurately capture crushing effects [20] .

The model proposed in Refs. [29] [30] assumed that the out-of-plane stress components are negligibly small to promote damage, and therefore only the in-plane stress components of the stress tensor activate damage. However, this assumption is not suitable for test cases where the triaxiality of the stress state is not negligible. Therefore, to improve the ability to accurately predict damage initiation and evolution in 3D test cases, the 3D invariant-based failure criterion for fibre kinking proposed in Ref. [32] is used to define an apparent in-plane shear strength that represents the effect of hydrostatic pressure on the shear response of the polymer matrix [20] . This 3D invariant-based failure criterion for fibre kinking is also used to scale the fracture toughness for longitudinal compression and the longitudinal compressive strength ratio at the inflection point as a function of the applied hydrostatic pressure [20] .

Experimental evidence also shows that the mode II fracture toughness virtually increases when the cracking faces are compressed against each other. Therefore, an effective fracture toughness for in-plane shear is defined to account for the effect of transverse compressive stresses on the fracture toughness associated with in-plane shear fracture [20] .

Finally, an energy regularization approach based on the fracture energy associated to each failure mechanism and based on the characteristic length of the FEs is used to ensure mesh independent results after damage onset [33] . This model is implemented in a user-defined subroutine VUMAT for the commercial explicit FE solver Abaqus/Explicit [28] . Element deletion is set when the damage variable corresponding to longitudinal failure mechanisms (d_1) becomes ≈ 1.

The 8-node C3D8R 3D brick elements with reduced integration are used to model each layer of the composite structure under consideration, including the LSP layer. Adjacent plies with the same fibre orientation, forming ply clusters, are modelled by a single element through the thickness. This strategy is compatible with the assumed *in situ* effect [31] . It is also assumed that the effect of the paint on the mechanical response of the composite plate is negligible, and therefore it is not explicitly represented in the FE model.

Delamination between plies is modelled using the interaction properties with cohesive behaviour available in Abaqus [28] . The cohesive interaction is defined by a bilinear traction-separation damage law accounting for mode dependency according to the B-K law [34] . Mode-dependent (mode I and mode II) strengths and fracture energies are considered. The onset of interlaminar damage is predicted by a quadratic stress-based criterion, and to account for friction between delaminating interfaces subjected to sliding movements, tangential friction is assigned to the cohesive surface interactions. A friction coefficient of 0.3 is assumed.

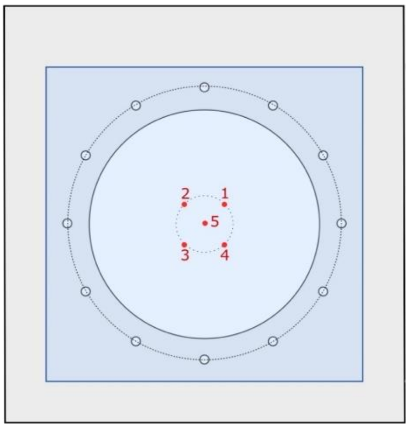
Turon *et al.* [35] demonstrated that changes in the local mode ratio in the evolution of damage during mixed mode loading might lead to erroneous calculation of the energy dissipation. For this reason, the shear strengths are not fully independent material properties, but instead they are determined as a function of the mode I and mode II fracture toughness and of the normal strength.

The pressure distributions mentioned in Sect. 3 are applied to the top surface of the composite plate. It is noted that the time scale of the induced mechanical forces/pressures caused by transient current components is rather small, of the order of 50 µs. However, the mechanical response of the CFRP laminate must be considered at the time scale of ms (taking into account the appropriate mechanical boundary conditions, e.g., the clamping of the test sample during laboratory lightning strikes).

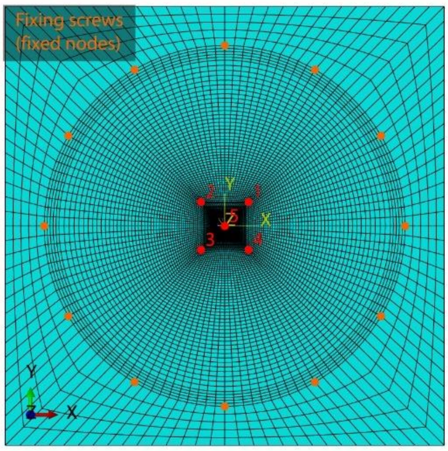
4.2 Model validation

Following Lepetit *et al.* [3] , CFRP samples made from 8 T700/M21 UD plies with a quasi-isotropic $[45/0/–45/90]_S$ lay-up are considered in this study. Each T700/M21 UD ply is 0.262 mm thick. The samples were protected using ECF 73 or ECF 195. A polyurethane paint layer with a thickness of 300 µm (including surfacing film) was applied to each sample.

The samples are square laminated plates (450 mm-long sides). For the lightning strike test, they are supported on a fixed plate with a circular opening (340 mm diameter, Fig. 3a) and fixed using screws placed along a circumference concentric with the central opening of the support plate (370 mm diameter). The CFRP sample is modelled fixing the out-of-plane displacements on the supported area outside the circular opening and fixing the nodes along the circumference where the screws are placed (Fig. 3b).



(a) Configuration of the lightning strike test support and sample



(b) Composite plate FE mesh

Fig. 3. Test support and test sample configuration and FE model.

The LSP layers are assumed homogeneous orthotropic linear-elastic solids. Following Karch *et al.* [15] , the effective properties of ECF 73 and ECF 195 filled with epoxy resin M21 were determined using a micro-mechanical FE homogenization approach.

As discussed by Karch *et al.* [15] , the VISAR deflection measurements conducted by Lepetit *et al.* [3] strongly indicate that the lightning strike did not initiate from the middle of the CFRP samples. Therefore, for the purpose of validating the proposed mechanical model, Karch *et al.* [15] used a 2D symmetric Gauss function to fit the deflection in the measurement points 1 to 5 (Fig. 3a) for different times. Karch *et al.* [15] then averaged the time dependent offset coordinates. The mechanical pressures are then centred at these offset points.

While models in the literature, either thermal(-electrical) [36] [37] [38] [39] [40] [41] [42] [43] or thermal-mechanical [44] , have been validated solely in terms of the size and shape of the lightning-induced damage detected visually from the surface of the coupons or obtained by non-destructive inspection techniques (e.g. C-scan), the predictions of the mechanical response obtained with the model proposed in this work is validated against direct measurements of the out-of-plane velocity and displacement of coupons tested by Lepetit *et al.* [3] . Fig. 4 and Fig. 5 show the comparison between the numerical results and the experimental VISAR measurements for ECF 73 and ECF 195 [3] , respectively.

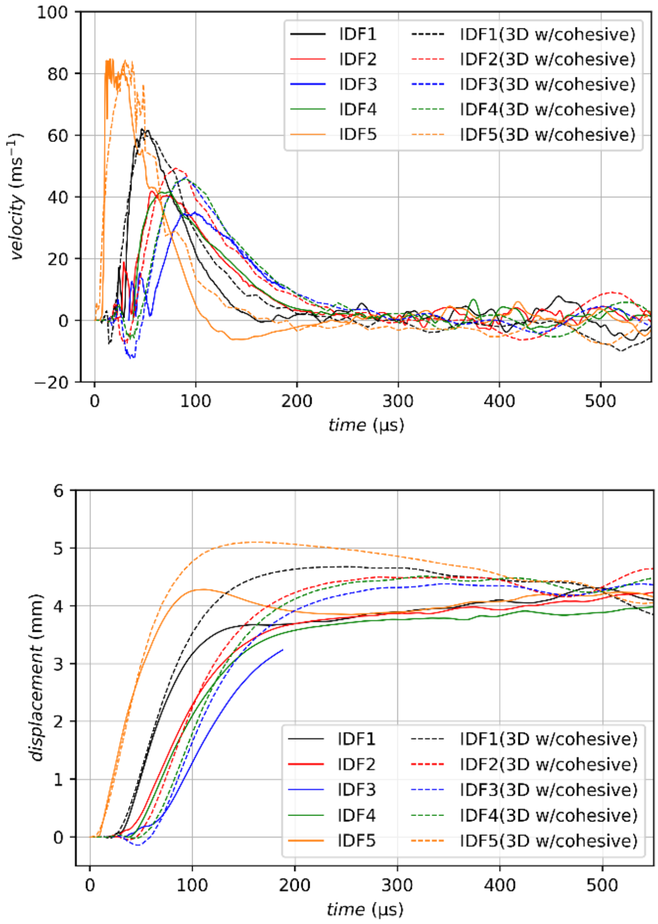


Fig. 4. FE predictions (dashed lines) and VISAR measurements (full lines) [3] for ECF 73.

As can be observed, the predictions of the out-of-plane velocity represent very well the experimental results at the different measurement points, in particular the peak velocities. Regarding the predicted deflections, although they exceed the experimental results at the initial 50-300µs, the results then converge at around 500µs.

The results in Fig. 4 and Fig. 5 indicate that the model of the lightning loads proposed by Karch *et al.* *[15]* can accurately represent the mechanical contributions from a lightning strike, in particular when used together with detailed damage models for CFRPs. This framework can then be exploited to assess the mechanical (bulk) damage induced by lightning strikes. Fig. 6 and **Error! Reference source not found.** show the contour plots of the damaged elements (intralaminar damage) and interfaces (delamination) corresponding to damage onset – (black) partially damaged elements/interfaces ($0 < d < 1$) – and full cracks – (red) fully damaged elements/interfaces ($d \approx 1$). It is noted that, with the objective of validating the proposed framework, a fine mesh was used at the lightning strike location (Fig. 3b) from where the out-of-plane velocities and deflections were extracted. Due to the sample size, a progressively coarser mesh was used as the distance to the centre of the sample increased, which renders the damage onset maps inaccurate in the coarse regions, hence the extensive areas showing damage onset and local failure, in particular for ECF 73 (Fig. 6) near the supporting edge. It should, therefore, be stressed that damage in this region is not constrained by the boundary conditions, but it is a result of the finite element mesh discretisation strategy, adopted to save computational cost. Nevertheless, the damage patterns are those usually observed on protected CFRP samples subjected to simulated lightning strikes and attributed to mechanical effects: damage tends to concentrate below the region of the initial attachment (near the centre of the plate in Fig. 6 and **Error! Reference source not found.**), including interlaminar and matrix damage at the middle and bottom plies, spreading away from the initial attachment point predominantly at the top plies.

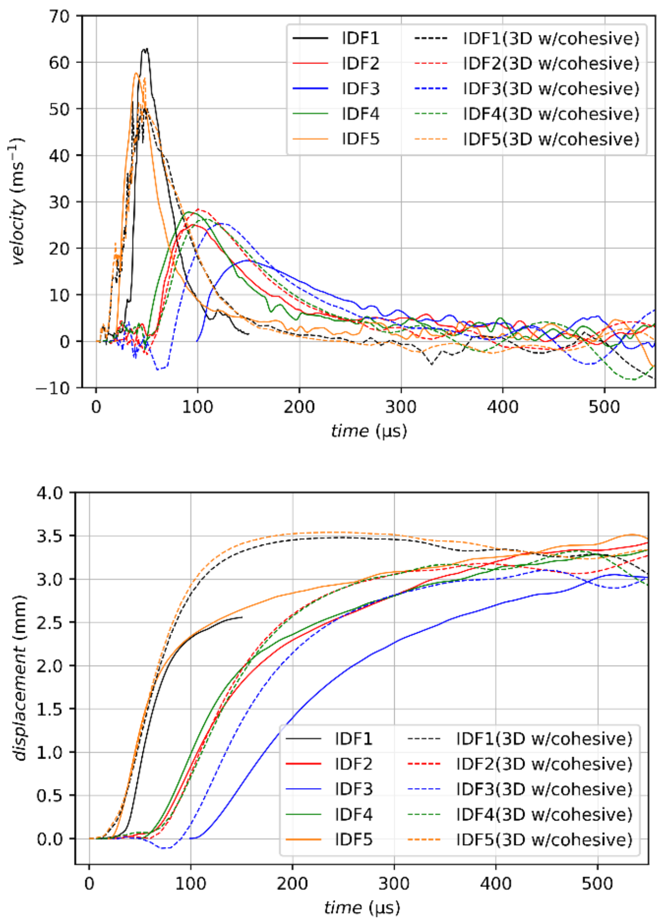


Fig. 5. FE predictions (dashed lines) and VISAR measurements (full lines) [3] for ECF 195.

5. SUMMARY AND CONCLUSIONS

The present work shows that the overall mechanical response of CFRP laminates subjected to a lightning strike can only be captured with appropriate models of the mechanical loads induced by this type of events. Combining models of the mechanical lightning loads with robust constitutive models for CFRPs provides a reliable numerical framework for virtual testing of protected CFRP panels subjected to lightning strike events, which can aid the design process and reduce the empiricism still employed on the analysis of this type of phenomena. Such general framework can also be used to assess the damage tolerance of structural CFRP panels subjected to lightning strike events by conducting post-strike tension or compression virtual tests [37] , similarly to what is currently done for mechanical impacts [45] [46] . A valid damage tolerance assessment could then support the development and optimisation of weight-effective protection solutions and the definition of reliable criteria for the severity of a lightning strike based on visual or non-destructive inspection protocols [5] .

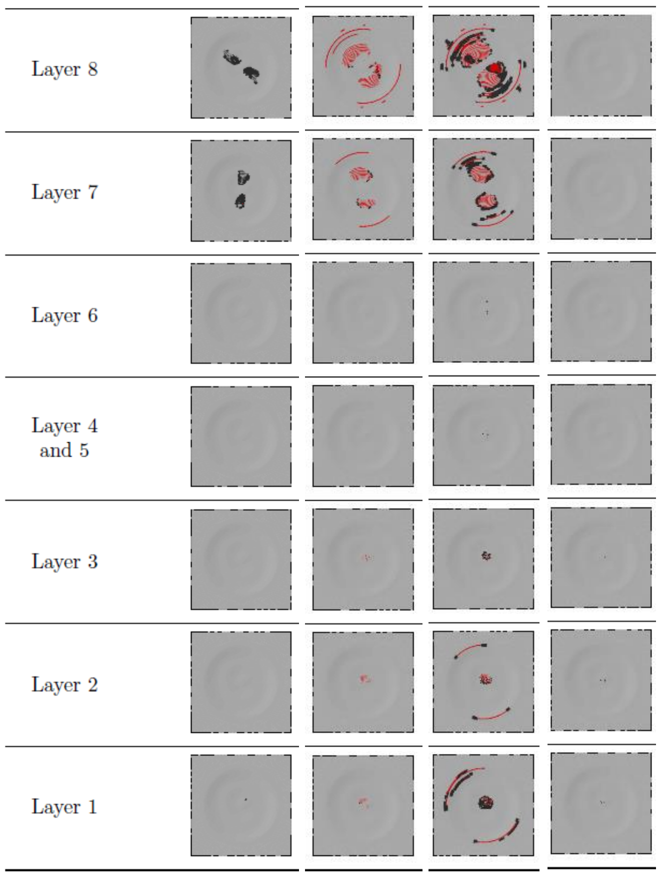


Fig. 6. Predicted contour plots corresponding to damage onset (black) and fully cracked regions (red) on ECF 73. From left to right: fibre damage; matrix transverse damage; matrix shear damage; interlaminar damage. Lightning strike (pressure distribution) applied to the top of the laminate (*Layer 8*).

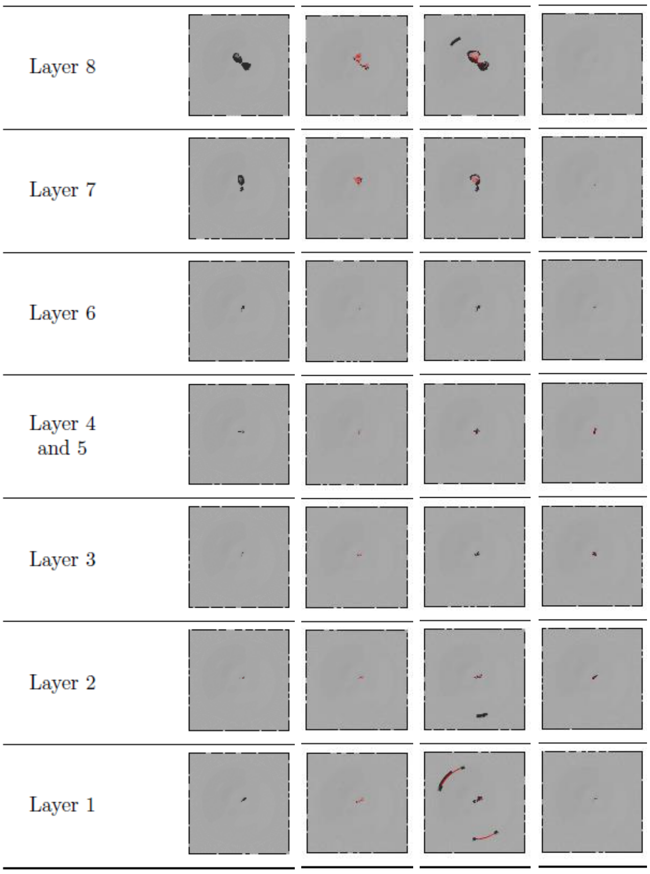


Fig. 7. Predicted contour plots corresponding to damage onset (black) and fully cracked regions (red) on ECF 195. From left to right: fibre damage; matrix transverse damage; matrix shear damage; interlaminar damage. Lightning strike (pressure distribution) applied to the top of the laminate (*Layer 8*).

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7. ACKNOWLEDGEMENTS

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