FAST SIMULATION OF STATIC STRENGTH AND DYNAMIC IMPACT BEHAVIOUR OF COMPOSITE FUSELAGE STRUCTURES

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1. INTRODUCTION

While the elastic behaviour of composite structures is well understood and the fast simulation of deformations and stresses using first order shear deformation theory has been shown to lead to good results [12], the efficient simulation of composite failure remains a difficult task.

Several failure modes exist in composite laminates. Not only failure of the fibres, which have to bear most of the loading, can reduce the strength of a composite laminate. Also inter-fibre failure such as matrix cracking and delamination can lead to a substantial loss of load bearing capacity. Recently 3D reinforced composites have been developed to reduce the threat of matrix cracking and delamination. In the following section a new macromechanical strength model is described, which takes the 3D reinforcement into account.

Even non or barely visible impact damage can cause a substantial reduction in the residual strength of composite structures. Due to a lack of fast analytical and numerical tools there often is a need to conduct time and cost intensive experiments. To reduce the amount of testing, the program CODAC is being developed, which can be used in the design phase of composites in the frame of a Concurrent Integrated Engineering Process (CIE) to quickly evaluate the impact behaviour of stringer-stiffened panels due to low-velocity impact. Section 3 gives an overview of the CODAC impact simulation.

2. A STATIC STRENGTH MODEL FOR 3D REINFORCED PLASTICS

At first a short overview of failure criteria for first ply failure prediction of fibre reinforced plastics made of unidirectional layers shall be given. Subsequently a new strength model is proposed to extend the application range of physically based criterion to a special configuration of 3D reinforced composites.

2.1. Failure Criteria for composites made of unidirectional layers

Existing failure criteria for fibre-reinforced plastics made of unidirectional layers can be separated into micromechanical and macromechanical approaches. Micromechanical methods are in fact well suited for the prediction of failure in composites, but numerous problems can occur when they are employed for practical applications. According to Hashin [5] such methods require analytical detection of successive microfailures in terms of microstress analysis and microfailure criteria, and prediction of coalescence of some of the microfailures to form macrofailures. Despite some advances have been made in this field of research and new results are presented e.g. by Voyiadjis [14], the computational effort still is too high for practical applications. Additionally it is difficult to determine micromechanical material parameters needed for microfailure criteria.

Macromechanical failure criteria use macrovariables like macroscopic stresses or strains. Several of these criteria try to predict failure of laminates as a whole [11], requiring in advance determination of the strengths limits of a respective laminate configuration. This approach is impracticable for design purposes. Additionally it provides no information on the successive growth of initially tolerable damage within the laminate. Far more beneficial is a layer-by-layer approach where the layer is considered as a defect free macro-homogeneous orthotropic continuum with macroscopic stresses / strains and average strengths.

Basically stresses and strains are linked via the material law. According to Puck [9] stress based criteria are more meaningful than strain based criteria which can become complicated and obscure due to transverse contraction effects. Additionally it is practically impossible to generate an uniaxial strain state in a test specimen to obtain the strain limits. Determination of material strengths by standard uniaxial stress tests is significantly easier.

In summary, criteria suitable for practical applications should

- have macromechanical-phenomenological character,
- be related to single unidirectional composite layers and
- use macroscopical stresses and stress based average strengths of single layers.

Such criteria exhibit reasonable computational effort, easy determination of material parameters and are useful for design purposes as well as for damage progression analysis.

Further on, experimental experiences allow for the following hypothesis:

H1: *A fibre reinforced composite layer has different failure modes: **F**ibre **F**racture (**FF**) and **I**nter **F**ibre **F**racture (**IFF**). FF and IFF are of different nature and require therefore different failure criteria [5].*

So called global failure criteria try to describe both FF and IFF with a single scalar equation, e.g. formed of a quadratic tensor polynomial, offering the advantage of mathematical elegance and ease of numerical application. The most familiar one of these criteria is the Tsai-Wu-criterion [13]. Various authors (e.g. Bergmann [1], Hashin [5]) have discussed physical shortcomings and difficulties in determination of polynomial coefficients of this criterion. It does not yield physically valid results under all stress states. Wu [15] showed that most of the previously introduced quadratic criteria are degenerated cases of the Tsai-Wu theory. Therefore the characteristics of the Tsai-Wu criterion are shared by all of them to a smaller or larger extent. In summary, fitting of polynomial coefficients can not compensate for the lack of an appropriate physical basis. Only physically based criteria taking H1 into account are able to match the real material behaviour.

For FF a solution is relatively simple. Experimental results show that FF occurs as primary failure mode of a defect-free composite layer only for normal loads in directions very close to the fibre direction (within approximately 2°). This suggests the following simple max-stress criterion for FF:

(1) $$\left|\frac{\sigma_{\parallel}}{R_{\parallel}^{(+,-)}}\right| = 1$$

where $\sigma_{\parallel}$ is the stress and $R_{\parallel}^{(+,-)}$ are the strengths of the unidirectional layer for tensile (+) and compressive (-) loads in fibre direction. Puck [10] discussed that deviations of this simple criterion to more sophisticated models, which include the influence of shear, and transverse normal stresses on FF are less than 5%. Therefore the simplest model should be used. Equation (1) is valid for composites for which the strain to failure of the fibres is less than that of the matrix. Only if this is the case FF occurs before IFF and the fibre strength and not the matrix strength dominates the strength of the layer for normal loads in fibre direction.

The derivation of a physically based criterion for IFF was more difficult. Failure of the matrix material of fibre-reinforced plastics can be looked upon as brittle. Thus a valid physical base for IFF is Mohr's fracture hypothesis for brittle materials [7]:

H2: *The fracture limit of a brittle material is governed by the stresses in the fracture plane.*

Hashin [5] postulated the idea to adapt Mohr's fracture plane concept to IFF of unidirectional composite layers. He established the following hypotheses:

H3: *IFF always occurs on a plane parallel to the fibres. On such planes no fibre fracture occurs.*

H4: *If a fibre parallel fracture plane under a fracture angle θ can be identified, IFF will be caused by the interaction of the normal stress σ_N and the shear stresses τ_{NT} and τ_{NL} in the fracture plane* (FIGURE 2). *Therefore an IFF-criterion should be formulated in stresses and strengths of the fracture plane.*

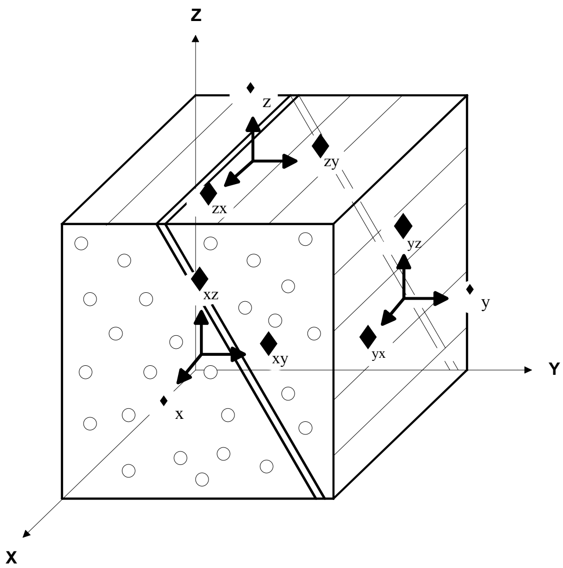

FIGURE 1. Components of the stress tensor

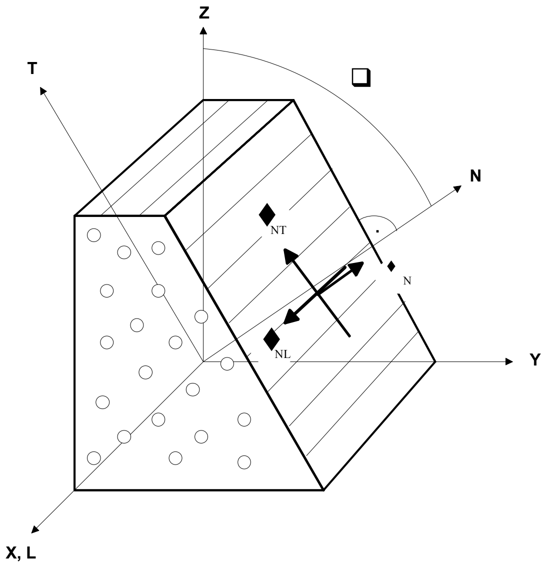

FIGURE 2. Stresses in the fracture plane

The identification of the fracture plane in this concept proved to be difficult and computationally expensive. Therefore Hashin did not pursue this approach. Instead he used the fact that unidirectional fibre reinforced plastics are transversely isotropic bodies and established a quadratic criterion based on four of the five stress invariants of transversal isotropy [5]. A criterion using all five stress invariants of this symmetry including third order stress terms has been developed by Cuntze [4]. He defines three different failure mechanisms for IFF, each governed by one basic strength. In this approach every failure mechanism has a clearly defined effective stress. Probabilistic methods are used to determine transition in mode interaction zones.

However, Mohr's fracture hypothesis is physically sounder and therefore the higher computational effort to determine the fracture plane can be justified. Furthermore, due to the rapid advancements in computational technology this additional effort does not pose a problem anymore. Consequently, Puck has taken up Hashin's idea. Based on his significant experimental experience he established the following additional hypotheses, which refine Hashin's original approach:

H5: *If $\sigma_N \geq 0$, then IFF will be caused by the simultaneously acting transverse tensile stress σ_N and the transverse shear stresses τ_{NT} and τ_{NL}.*

H6: *If $\sigma_N < 0$, then the transverse compressive stress σ_N generates an additional resistance against the fracture caused by the transverse shear stresses τ_{NT} and τ_{NL}.*

Puck [10] developed a mathematical model of a fracture body taking H2 to H6 into account and established the first failure criterion for IFF of unidirectional composite layers with brittle matrix material based on Mohr's fracture hypothesis, the **P**arabolic **C**riterion of **P**uck (**PCP**). In addition to the basic strengths of the layer, the criterion employs 4 parameters representing gradients of the fracture body at certain points. This offers high flexibility for adaptation of the criterion to experimental results, but on the other hand causes additional experimental effort to determine values for these parameters.

On the basis of a work by Jeltsch-Fricker [6], Cuntze et al. [3] studied a number of fracture bodies based on Mohr's fracture hypothesis. The different models were examined in consideration of numerical stability, flexibility for data fitting, experimental effort for determining material parameters and ease of application. Cuntze et al. [3] recommended the so-called **S**imple **P**arabolic **C**riterion (**SPC**). This criterion is in fact a simplification of the PCP in that it couples the gradients of the fracture body so that only two gradient parameters remain in the criterion. If an additional relation is used the number of parameters is further reduced to one. On the one hand this increases ease of application and reduces the experimental effort, on the other hand the necessary data fitting of experimental results can be less accurate. Since both criteria share a common physical basis and result in a practically equal failure prediction, the authors focus on the SPC as the criterion with the simpler mathematical representation and easier numerical handling. The SPC has the following mathematical form [3]:

(2) $\sigma_N \geq 0$:

$$\sqrt{\left(1-p^{(+)}\right)^2\left(\frac{\sigma_N}{R_N^{(+)}}\right)^2+\left(\frac{\tau_{NT}}{R_{NT}^{(+)}}\right)^2+\left(\frac{\tau_{NL}}{R_{NL}^{(+)}}\right)^2}+p^{(+)}\frac{\sigma_N}{R_N^{(+)}}=1$$

(3) $\sigma_N < 0$:

$$\sqrt{\left(p^{(-)}\right)^2\left(\frac{\sigma_N}{R_N^{(-)}}\right)^2+\left(\frac{\tau_{NT}}{R_{NT}^{(-)}}\right)^2+\left(\frac{\tau_{NL}}{R_{NL}^{(-)}}\right)^2}+p^{(-)}\frac{\sigma_N}{R_N^{(-)}}=1$$

The criterion is formulated solely in stresses and strengths of the fracture plane and forms an ellipsoid for $\sigma_N \geq 0$ (presumed $0 < p^{(+)} < 0.5$) and a paraboloid for $\sigma_N < 0$. A quadratic-additive interaction between the fracture plane stresses is assumed. There is no physical rationale for this, it is simply the most effective way to match experimental results by curve fitting [5].

The stress tensor $\sigma = (\sigma_x, \sigma_y, \sigma_z, \tau_{yz}, \tau_{xz}, \tau_{xy})^T$ in the (x, y, z)-system has to be transformed into the stress tensor $\sigma = (\sigma_N, \sigma_T, \sigma_L, \tau_{NT}, \tau_{NL}, \tau_{TL})^T$ in the (L, N, T)-system (FIGURE 2). Both the (x, y, z)- and the (L, N, T)-system are orthonormal coordinate systems. The axes of the (x, y, z)-system correspond to the material axes of the layer. Axis L of the (L, N, T)-system is equal to the fibre direction x of the layer, N represents the normal direction of the fracture plane and T is orthonormal to L, N. Due to H3 only a rotation of the normal vector of the fracture plane about the fibre axis is necessary to cover all possible orientations of the fracture plane. Hence θ can vary from $0 \leq \theta \leq \pi$ (FIGURE 2). The stresses in the fracture plane can then be calculated by tensor transformation:

(4) $\sigma_N = \sigma_y \sin^2\theta + \sigma_z \cos^2\theta + \tau_{yz} \sin 2\theta$

(5) $\tau_{NT} = \frac{1}{2}(\sigma_y - \sigma_z)\sin 2\theta + \tau_{yz}\cos 2\theta$

(6) $\tau_{NL} = -\tau_{xy}\sin\theta - \tau_{xz}\cos\theta$

Failure will occur at the angle θ_{max} that makes the left side of equation (2) or (3) a global maximum. For a general 3D stress state no analytical solutions for extrema of these equations are known, the maximum has to be calculated in a numerical-iterative way. It can be observed that the SPC is homogenous of order one in σ. Therefore, presumed that only external loads are applied, the left side of equation (2) and (3) is equal to the material effort ε. Cuntze et al. [3] transformed the strengths of a layer into the strengths of the fracture plane as follows:

(7) $R_N^{(+)} = R_y^{(+)} = R_z^{(+)}$

(8) $R_N^{(-)} = R_y^{(-)} = R_z^{(-)}$

(9) $R_{NL} = R_{xy} = R_{xz}$

(10) $R_{NT} = \dfrac{R_N^{(-)}}{1+\sqrt{1+2p^{(-)}}}$

The suffixes (+,-) refer to the sign of σ_N. $R_y^{(+)}$, $R_y^{(-)}$, R_{xy} can be determined by means of simple experiments. R_{yz} is not directly measurable [10]. Therefore R_{NT} has to be calculated from $R_N^{(-)}$ using equation (10).

2.2. Extension to 3D reinforced plastics

Conventional composite structures consist of unidirectional layers with in-plane fibre reinforcements in layerwise constant orientations. 3D fibre reinforced composites use additional fibre reinforcements in any out-of-plane direction. While such structures offer improved strength in these directions, the in-plane strength of the layer may be decreased significantly due to increased fibre waviness [8]. Therefore it is not reasonable to use the same fibre density in out-of-plane directions as in the main fibre direction of the layer. Such a design would just be ineffective and would not take full advantage of the high specific fibre strength. In addition, the manufacturing process of out-of-plane fibre reinforcements is technologically expensive. The simplest and easiest way to manufacture a 3D fibre reinforcement is one exactly vertical to the composite layers. Thus a very common configuration of 3D composites will consist of unidirectional layers with high in-plane fibre density and fibre reinforcements exactly vertical to the layers with a significantly lower fibre density. Only this configuration is considered subsequently.

The prediction of fracture limits as well as fracture angles using the PCP and SPC has been experimentally verified for unidirectional composites for some selected states of combined stresses [10]/[3]. The PCP and SPC assume unidirectional layers being transversally isotropic, an idealization that is no longer valid for 3D composite layers. Nevertheless, it can be assumed that the fracture hypothesis of Mohr and the additional hypotheses H2 to H6 remain valid with certain restrictions even for IFF of 3D reinforced plastics of the type described above. Then the general mathematical concept and formulation of the SPC, which are based on these hypotheses, can be retained. But a new strength model for additionally orthogonal reinforced unidirectional layers must be developed. The following strength model is proposed to extend the application range of the SPC to additionally orthogonal reinforced unidirectional layers:

$$R_N^{(+,-)} = \tilde{R}_y^{(+,-)} \sin^2\theta + \tilde{R}_z^{(+,-)} \cos^2\theta + \tilde{R}_{yz}^{(+,-)} \left|\sin 2\theta\right| \tag{11}$$

$$R_{NT}^{(+,-)} = \left(\tilde{R}_y^{(+,-)} + \tilde{R}_z^{(+,-)}\right)\left|\sin 2\theta\right| + \tilde{R}_{yz}^{(+,-)} \tag{12}$$

$$R_{NL}^{(+,-)} = \tilde{R}_{xy}^{(+,-)} \left|\sin\theta\right| + \tilde{R}_{xz}^{(+,-)} \left|\cos\theta\right| \tag{13}$$

FIGURE 3, FIGURE 4 and FIGURE 5 show that the failure prediction of the modified SPC with the new strength model coincides well with experimental results obtained by the authors in the (x, y)-, (x, z)- and (y, z)-plane.

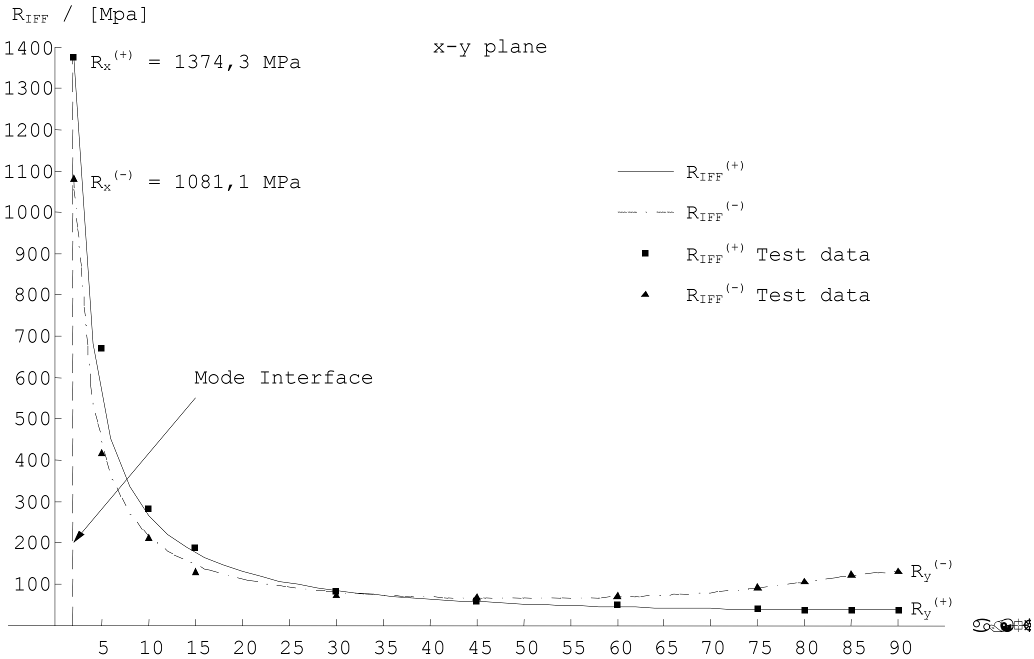

FIGURE 3. Experimental results and failure prediction in the (x,y)-plane

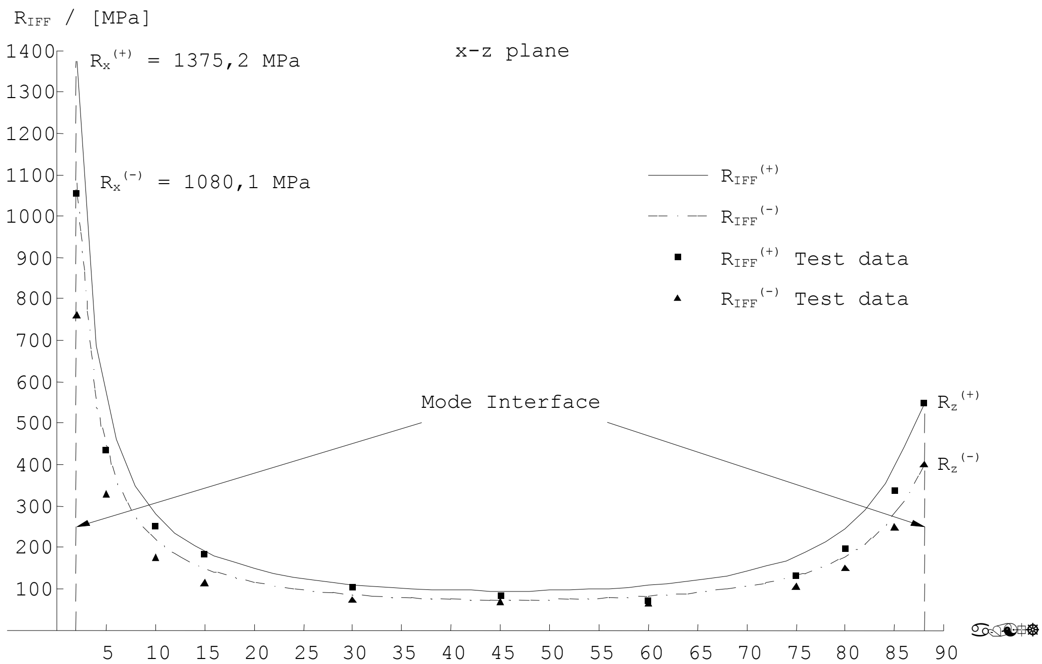

FIGURE 4. Experimental results and failure prediction in the (x,z)-plane

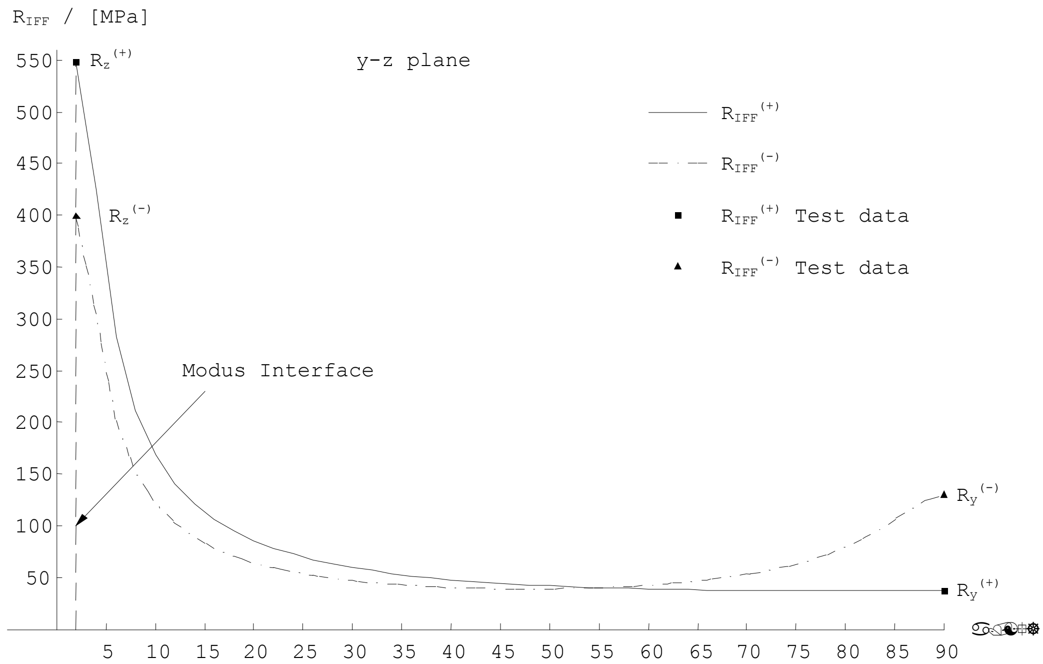

FIGURE 5. Experimental results and failure prediction in the (y,z)-plane

The experimental results were obtained from off-axis tests with an angle α between the load axis and the fibre axis. Due to different fibre volume ratios the measured fibre dominated strengths $R_x^{(+,-)}$ are not equal in the (x, y)- and (x, z)-plane, therefore the strengths $R_x^{(+,-)}$ measured in the (x, y)-plane were assumed also for the (x, z)-plane. But it can be seen, that by fitting the strength parameters in equations (11) - (13) to the test results the complete strength spectrum of an additionally orthogonal reinforced unidirectional layer can be simulated with the modified SPC. For further verification of the criterion additional test results are necessary.

3. DYNAMIC IMPACT BEHAVIOUR OF FUSELAGE SUBSTRUCTURES

Stringer stiffened panels are widely used in aerospace structures such as fuselage and wing panels. Stringer and skin can be either bonded adhesively or by co-curing them. FIGURE 6 shows the geometry of the panels used for damage tolerance testing.

In CODAC such structures are modelled by 8-node shell elements for the skin laminate and 3-node beam elements for the stringers. The impactor is modelled as a point mass with a given initial velocity. Using the Hertzian contact law, the contact force is related to the indentation of the skin laminate.

For the formulation of the transient impact response an implicit Newmark time stepping scheme is employed. Up to now degradation due to impact damage is not accounted for, so using a linear elastic material law and geometrical linearity as well, it is sufficient to decompose the systems of equations only once.

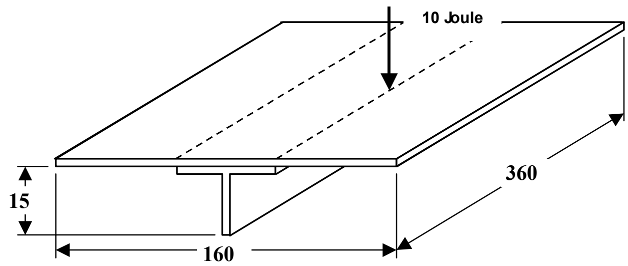

FIGURE 6. Stringer-stiffened panel

While the determination of the in plane stress components σ_{xx}, σ_{yy} and σ_{xy} makes no complications for 2-D shell elements, the determination of the transverse shear stresses σ_{xz} and σ_{yz} requires close attention. Because these stress components play an important role for matrix and delamination failure, it is essential to assess them as accurately as possible. In CODAC the extended 2-D method [12] is implemented, which improves accuracy by using equilibrium conditions instead of the material law for the determination of the transverse stress components. The transverse normal stress σ_{zz} also plays an important role in the area close to the impact location. However, the area of damage is usually significantly larger than the contact zone of impactor and structure. Because in the border area of the damage zone the transverse normal stress is very small, the determination of σ_{zz} is not necessary.

For the different failure modes, separate stress based failure criteria are used for the assessment of damage during the transient impact process. For fibre failure the maximum stress criterion is used., while for inter-fibre failure the physically based criteria from Hashin [5], Puck [10] or Cuntze [3] are recommended. For delamination failure due to low-velocity impact physically based criteria from Puck and Hashin have yielded unsatisfactory results. For this failure mode a special semi-empirical criterion from Choi/Chang [2] has lead to better results:

$$(14)\quad D_a \cdot \left[\left(\frac{{}^{n}\sigma_y}{{}^{n}Y} \right)^2 + \left(\frac{{}^{n}\tau_{zx}}{{}^{n}S_i} \right)^2 + \left(\frac{{}^{n+1}\tau_{yz}}{{}^{n+1}S_i} \right)^2 \right] = 1 .$$

Choi/Chang found in an experimental study, that the empirical parameter D_a is insensitive to ply orientations and laminate thickness. It is mainly dependent on the material system used. Choi/Chang suggest to use 1.8 for T300/976 and it was found, that this gives good results for the Prepreg material system HTA/6376 as well as for structures made of NCF laminates which were cured using the RTM technology.

In FIGURE 7 and FIGURE 8 the time dependent contact force due to a 10-Joule impact under the edge of the stringer foot (FIGURE 6) is graphed for a NCF panel and a conventional Prepreg panel. In FIGURE 8 severe damage occurred after about 4 ms at a contact force of 2.5 kN. This leads to oscillations of the contact force curve. Up to this moment, simulation and experiment are in very good agreement. The loss of stiffness due to the impact damage leads to a flatter slope in the descending branch of the experimental curve of FIGURE 8.

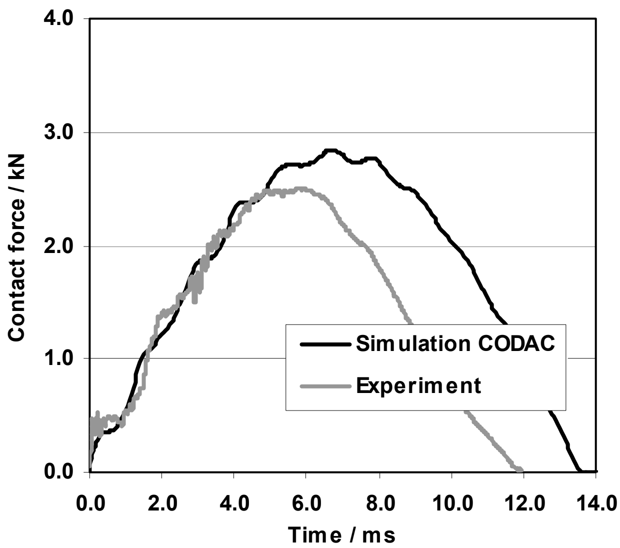

FIGURE 7. Contact force for a 10 Joule impact on the stringer foot edge of an NCF panel

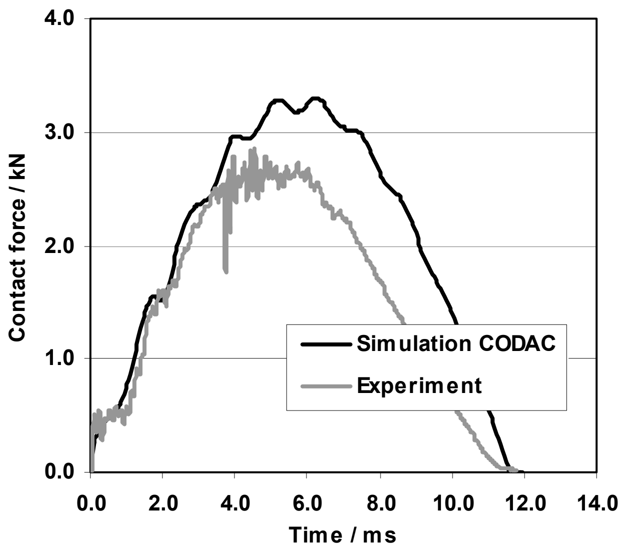

FIGURE 8. Contact force for a 10 Joule impact on the stringer foot edge of a Prepreg panel

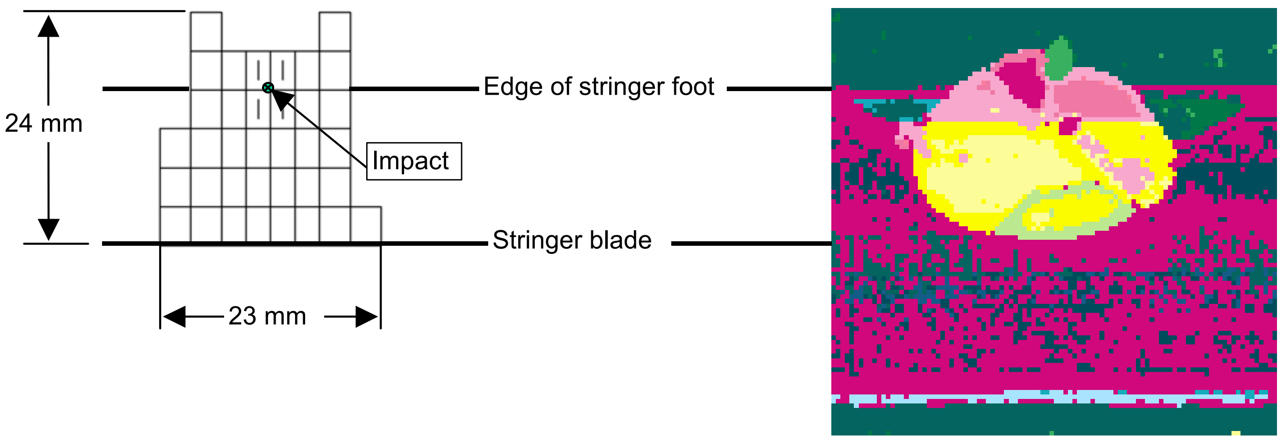

FIGURE 9. Delamination damage in the skin of a Prepreg panel after a 10-Joule impact under the edge of the stringer foot, comparison between simulation (left) and experiment (US scan, right).

In FIGURE 9 the delamination damage of experiment and simulation of the Prepreg panel are shown. The damage spreads out from the impact location at the edge of the stringer foot towards the stringer blade. This is observed in the experiment as well as in the CODAC simulation. The damage areas of experiment and simulation are comparable as well.

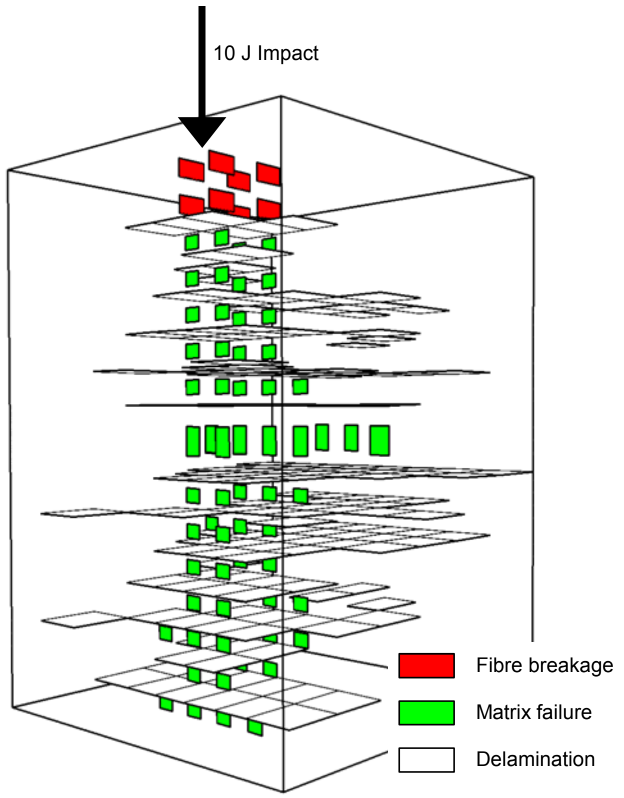

FIGURE 10. 3D simulated impact damage in the skin laminate of a Prepreg panel.

FIGURE 10 shows a 3D damage state of the CODAC simulation. While fibre failure is predicted directly below the impact point, matrix failure and delaminations are present in every layer / interface of the laminate.

4. CONCLUSION

Composites made of unidirectional layers will remain the most commonly used kind of composites. 3D composites will be used only where they are really necessary (e.g. in thick structures or areas where loads are applied). However, there will be a need for 3D reinforced structures with the ongoing extension of the application range of composites. A physically based failure analysis is a prerequisite for the use of such structures. The SPC for quasistatic loads has been adapted to orthogonal 3D fibre reinforced plastics consisting of unidirectional layers with high in-plane fibre density and fibre reinforcements orthogonal to the layers with a significant lower fibre density. Such a configuration of 3D fibre reinforced plastics is especially advantageous from a mechanical, technological and economical viewpoint.

For the fast calculation of the dynamic impact response a trade-off between precision and computational efficiency has to be made. In spite of the simple models used in CODAC the transient impact response and the damage area can be predicted quite accurately as the examples presented have shown. Further development is necessary to account for the degradation of material properties in the presence of damage. Additionally, a physically based delamination criterion would be desirable.

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