

Application of a Newer Inter-Fiber-Failure Criteria on CFRPs in Aero Engine Development

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Abstract: Carbon-fiber-reinforced plastics (CFRP) are being used for highly loaded lightweight structural components for many years. Up to now mostly insufficient two-dimensional classical failure criterions, which are embedded into FE-software like Tsai-Wu, Hill, etc. have been used for the dimensioning of composites. To achieve better predictions of the three-dimensional complex composite failure behavior newer, so-called action-plane based failure criterions have been developed, e.g.: PUCK, JELTSCH-FRICKER or LaRC04. In addition to this, the complex step-by-step component failure process including post-failure load redistribution can be accurately simulated using a combination of these newer criterions with a convincing material degradation model. Within this work this new method was implemented into Abaqus to investigate the complex failure behavior of a CFRP flange connection of a Rolls-Royce aero engine. For instance, it is shown, that small radii next to the bolt-connection result in three-dimensional stress states that initialize delamination and gradual component stiffness reduction. The comparison of additional experimental and numerical data confirmed the implementation and prediction quality of new action-plane based failure criteria into Abaqus. Due to this the knowledge about the complex component behavior has been significantly extended, such that finally a cost-reducing design improvement was available.

Keywords: Aircraft, Composites, Crack Propagation, Delamination

1. Introduction

Aiming at more environmentally-friendly, more efficient and more powerful aero engines, lightweight structures will become more important in the future. Compared to conventional, isotropic materials, fiber-reinforced plastics leads to in reduced masses, higher stiffness-to-weight ratios and higher structural damping. Apart from these advantages a number of disadvantages occur. Some of them are for example a much lower maximum allowable operation temperature, the need for two-dimensional stress states and high requirements for load application areas.

From this, the knowledge about the complex failure behavior and the applicability of realistic failure criteria are the basis of an optimum design of multi-ply composites.

1.1 Particularities of composite materials

Each single ply of fiber-reinforced plastics consists of two components:

- High-tensile fibers and
- Shaping and covering matrix.

While the fibers are the load-carrying component, the embedded liquid matrix material fixes the fiber position and orientation during the hardening. Apart from the supporting function the fiber-covering matrix material also protects against environmental influences. The excellent mechanical properties of each single ply along the fiber orientation are in a remarkable contrast to the mechanical properties transverse to the fiber direction (see Figure 1).

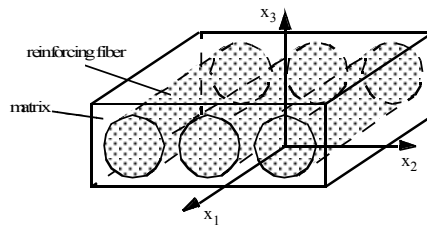


Figure 1. Unidirectional single ply and local coordinate system.

Contrary to isotropic materials like metal the resulting anisotropy establishes the basis for the high lightweight potential of such composite structures. In addition to the individual, loading-dependent design of composite structures, including the determination of the optimum lay-up sequence, the numerical verification of the mechanical strength of the structure is one of the most challenging tasks.

Statements about the structural strength of each single ply of such laminates can be calculated by so-called failure criteria. Up to now classical failure criteria like Tsai-Wu, Hill or Hashin are usually implemented into commercial FE software. These two-dimensional failure criteria can only reach a limited accuracy, due to their common approach, which bases on the von-Mises yield condition for isotropic materials (Azzi/Tsai [1965]). In addition to that, classical failure criteria only show the existence of failure, but they cannot distinguish between the different kinds of failure like fiber-failure or inter-fiber-failure. Without the knowledge about this major, fact no precise improvement of the composite design, fiber orientation or laminate lay-up is possible. Also the simulation of the post-failure behavior including allocation of degraded material properties to failed sections requires information about the mode of fraction.

Based on the work of Cuntze et.al. [1997] and Puck [1996, 1998] new failure criteria were implemented into the commercial Finite-Element software Abaqus to achieve a more realistic view and a better prediction of the complex failure behavior of composite structures.

2. Failure modes and criteria

2.1 Fiber-failure

Fiber-failure is the only “desired” kind of failure of composites, because the fiber material is the reinforcing support element of the structure that can be stressed up to a limiting strength. The reason for fiber-failure is normal stress σ_{11} along the fiber direction regarding the local single ply coordinate system (see Figure 2).

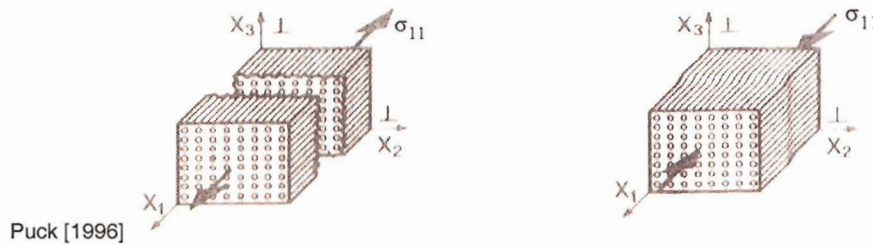


Figure 2. Fiber-failure caused by parallel tension and compression.

2.1.1 Definition of fiber-failure

The effort value of fiber-failure is defined by:

$$E_{FF} = \frac{|\sigma_{11}|}{R_{\parallel \text{Tension / Compression}}} \quad (1).$$

2.1.2 Model degradation

Caused by the high-energy release rate, fiber-failure results in a massive destruction of the affected single ply area. Thus, the fiber-matrix composite completely disintegrates, which can also induce a fiber- or inter-fiber-failure of neighbored plies. No load carrying capacity remains, so parallel and transverse stiffness are set down to zero.

2.2 Inter-fiber-failure

In contrast to the fiber-failure, transverse loading of single plies can result in micro fractures of the matrix material. If the loading increases furtheron, the existing single micro fractures merge into larger cracks up to the complete separation of the single ply. At this, the resulting fracture-plane orientation is perpendicular to the fiber orientation. Nevertheless the affected single ply is still tied with its adjacent plies – the fracture is localized to the failed single ply. An inter-fiber-failure with a fracture-plane angle θ_f of 90° – the so-called delamination – is an exception. In that particular

case the inter-fiber-fracture-plane occurs in the interface boundary-layer between the adjacent single plies.

The interrupted flux through the affected single ply induces load redistributions across the whole laminate. Due to the existing lower stress level in adjacent plies, compared to the fiber-failure stress level, the fibers of the adjacent plies commonly will not fail.

2.2.1 Transformation of single ply stresses into action plane stresses

Applying a new three-dimensional failure criteria, the stresses $\sigma_{22}, \sigma_{33}, \tau_{12}, \tau_{13}, \tau_{23}$ of the local x_1, x_2, x_3 -coordinate system of each single ply have to be transformed into the stresses $\sigma_n, \tau_{n1}, \tau_{nt}$ of the action plane, which is rotated around the x_1 -axis with a value of θ (see Figure 3 below):

Puck
[1996]

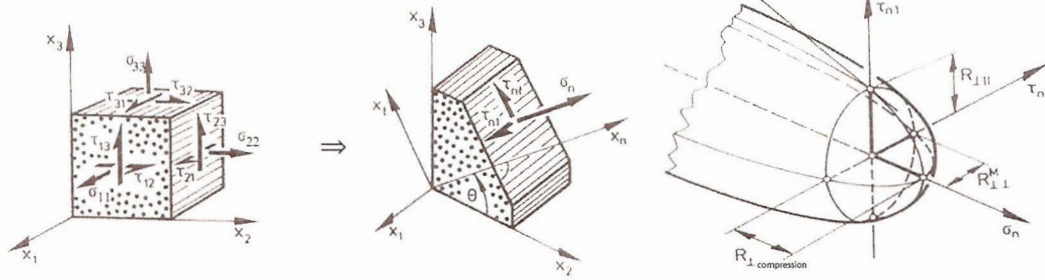


Figure 3. Transformation into action plane stresses (left); failure envelope according to action plane stresses (right).

$$\sigma_n(\theta) = \sigma_{22} \cos^2 \theta + \sigma_{33} \sin^2 \theta + 2\tau_{23} \sin \theta \cos \theta \quad (2),$$

$$\tau_{n1}(\theta) = \tau_{31} \sin \theta + \tau_{21} \cos \theta \quad (3),$$

$$\tau_{nt}(\theta) = -\sigma_{22} \sin \theta \cos \theta + \sigma_{33} \sin \theta \cos \theta + \tau_{23}(\cos^2 \theta - \sin^2 \theta) \quad (4).$$

Following the recommendation in Cuntze et.al. [1997] the robust, easy-to-handle single-parabolic failure envelope of Jeltsch-Fricker including four parameters was chosen for all further investigations (see Figure 3, right).

That application of an action-plane based failure criterion enables the distinction between the different failure modes that typically occur during the load increase, depending on the appearing action-plane stresses σ_n, τ_{n1} and τ_{nt} (see Figure 4).

2.2.2 Tension (mode A)

Transverse tension stress and/or transverse-parallel shear stress result in a fracture-plane, which is perpendicular to the fiber direction (see Figure 4, left).

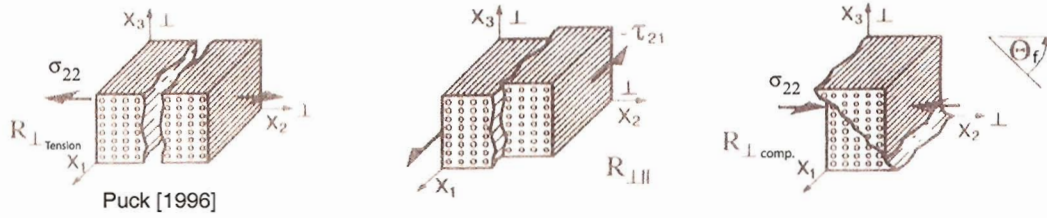


Figure 4. Inter-fiber-failure mode A (left), mode B (middle) and mode C (right).

According to Cuntze et.al. [1997] the inter-fiber-failure function in case of transverse tension $\sigma_n > 0$ is:

$$E_{IFF} = \sqrt{(1-p)^2 \left(\frac{\sigma_n}{R_{\perp Tension}} \right)^2 + \left(\frac{\tau_{n1}}{R_{\perp II}^M} \right)^2 + \left(\frac{\tau_{n1}}{R_{\perp II}} \right)^2} + p \frac{\sigma_n}{R_{\perp Z}} \quad (5).$$

Here $R_{\perp Tension}$, $R_{\perp II}$, $R_{\perp I}$ are the experimentally determined basic strengths, whereas

$$p = -m_{\perp I} \cdot \frac{R_{\perp Tension}}{R_{\perp I}} \quad (6)$$

defines the gradient of the failure envelope in the $\sigma_n - \tau_{n1}$ -plane for $\sigma_n = 0$.

2.2.3 Model degradation at mode A

Due to the ply splitting, inter-fiber-failure mode A causes an interruption of the flux through the single ply. Nevertheless with increasing distance to the fracture-plane, the affected single ply is able to take more load again. That means: The transverse stiffness of this affected area is decreased from a macroscopic point of view. On the contrary to this, the fibers are not affected, so the parallel stiffness remains unchanged (see Puck [1996] for further information).

2.2.4 Compression (mode B)

In contrast to relative innocuous inter-fiber-failure mode A, which is induced by transverse tension, inter-fiber-failure mode B is a result of the combination of transverse compression and transverse-parallel shear stresses (see Figure 4, middle).

At this, no crack opening appears due to the transverse compression $\sigma_n < 0$, the friction between the two clenched fracture surfaces still allows the shear stress transfer through the fracture-plane.

It is valid: The higher the transverse compression, the higher the transferable shear stress. Transverse compression restrains the inter-fiber-failure caused by the resulting inner friction, so the transverse-parallel shear stress is the only reason for the inter-fiber-failure mode B with $\sigma_n < 0$ and $|\sigma_n| < |\tau_{n1}|$. The effort of inter-fiber-failure can be calculated using:

$$E_{IFF} = \sqrt{(p)^2 \left(\frac{\sigma_n}{R_{\perp Tension}} \right)^2 + \left(\frac{\tau_{nt}}{R_{\perp\perp}^M} \right)^2 + \left(\frac{\tau_{n1}}{R_{\perp\parallel}} \right)^2} + p \frac{\sigma_n}{R_{\perp Z}} \quad (7).$$

2.2.5 Model degradation at mode B

Inter-fiber-failure mode B only leads to a decreased shear modulus (see Puck [1997]).

2.2.6 Compression (mode C)

If the transverse compression stress is higher than the transverse-parallel shear stress of the action plane ($\sigma_n < 0$, $|\sigma_n| > |\tau_{n1}|$), the resulting fracture-plane appears at $\theta_f \neq 0$ (see Figure 4, right).

At this σ_n and τ_{n1} do not exclusively induce the inter-fiber-failure in this fracture-plane, also the tangential shear stress τ_{nt} participates. The angle of the fracture-plane depends on the σ_n - τ_{n1} ratio. The sloped fracture-plane of the affected ply has a wedge-like acting, and as a result dangerous compression loads in direction of the laminate thickness can cause delamination as well as local buckling.

The effort of inter-fiber-failure mode C can be determined using Equation (7) again.

2.2.7 Model degradation at mode C

The rules for reduced shear modulus are the same as for inter-fiber-failure mode B.

2.2.8 Delamination

The boundary surfaces between two neighbored single plies do not include any fiber reinforcement, so they represent the weakest point in the laminate. Additionally to this, intersections and waviness of crossing fibers induce small, air-entrapping skip areas, which decrease the inter-laminar strength. At that boundary surface only a small amount of energy is necessary for crack initiation, because no fibers have to be severed, so the failure resistances have to be decreased with the weakening factor $f_{w, Delamination} = 0,8 \div 0,9$ according to Puck [1997]. In the worst case the outer single ply can be completely stripped off of the laminate during delamination, so the bending- and torsion stiffness of the structure are reduced, which can result in an anticipated structural breakdown by buckling.

The stresses σ_{33} , τ_{31} and τ_{32} of the interlaminar action plane with $\theta_f = 90^\circ$ are the reasons for this particular inter-fiber-failure mode, whose effort can be calculated using:

$$E_{Delamination} = \frac{1}{f_{w, Delamination}} E_{IFF}(\theta = 90^\circ) \quad (8).$$

2.3 Gradually failure behavior and laminate breakdown

As always mentioned before the first failure of a composite structure does not necessarily induce the sudden, complete structural disintegration in any case. Due to the appearing multitude of inter-fiber-failures at different load levels in different single plies a wide damage range occurs starting with an initial crack and ending with the total structural breakdown.

Mostly in high-performance composites, inter-fiber-failures markedly appear prior to the first fiber-failure. If the load keeps increasing under this condition, more and more local inter-fiber-failures will occur. As a result the transverse stiffness of the affected plies steadily decreases, so the flux through these degraded plies will be transferred to neighbored plies with higher load capacities. If their ply strengths are exceeded too, other plies have to bridge them, so the load redistribution between the adjacent single plies will continue. The composite structure does not completely fail if a load increasing is still possible.

Even though fiber-failures result in massive local laminate damaging, the appearance of the first fiber-failure does not induce the complete laminate failure in any case. Nonetheless, in general large fiber-failures areas are equal to laminate break down.

3. Implementation into Abaqus using a FORTRAN user subroutine

The structural analysis with FE-software can be divided into three phases:

- Preprocessing (model creation, load and boundary condition application),
- Solving the boundary value problem,
- Post processing (data visualization).

As usual, an incremental procedure was chosen to solve the geometric and material non-linear problem of the gradually failure behavior. After the initial increment size was defined, the Newton-iteration starts to create the current equilibrium solution, basing on the base-state stiffness. The resulting stress values of each increment are the input for the action-plane-based failure criteria, which is implemented in the FORTRAN-user subroutine and which starts at the end of each increment calculation. If the calculated failure efforts exceed the limiting failure resistances, the stiffness values of the affected elements will be degraded as described in chapter 2 (see Figure 5).

Next, the equation will be solved again using the updated stiffness matrix to determine the stress values of the following increment. Thus the material degradation always appears with a small offset using this procedure; at every increment the stress level is a little bit higher than required by the failure criteria to degrade the affected element. This unfavorable effect can be significantly reduced choosing an appropriate increment size. The degradation procedure will be proceeded until the load-displacement curve is sufficiently calculated.

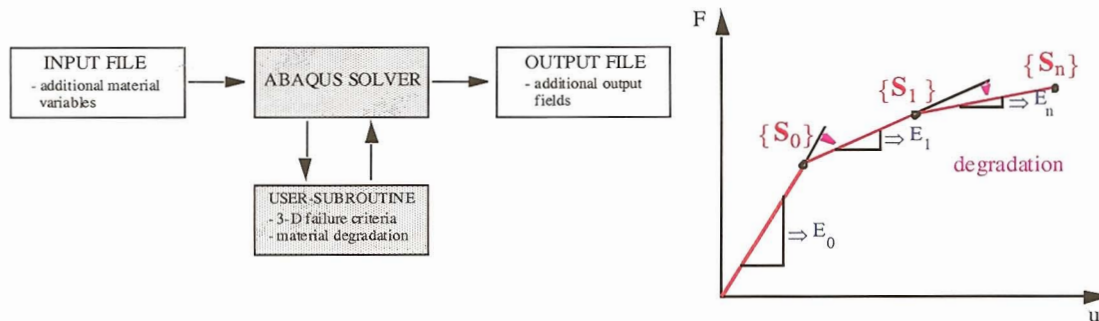


Figure 5. Implementation of an user-subroutine into Abaqus (left), determination of load-displacement curve using degraded Young's modulus (right)

4. Example of application

4.1 Introduction

A composite flange connection of the bypass-duct of a Rolls-Royce aero engine was chosen to verify the implemented action-plane based failure criterion (see Figure 6).

Courtesy of Rolls-Royce Deutschland

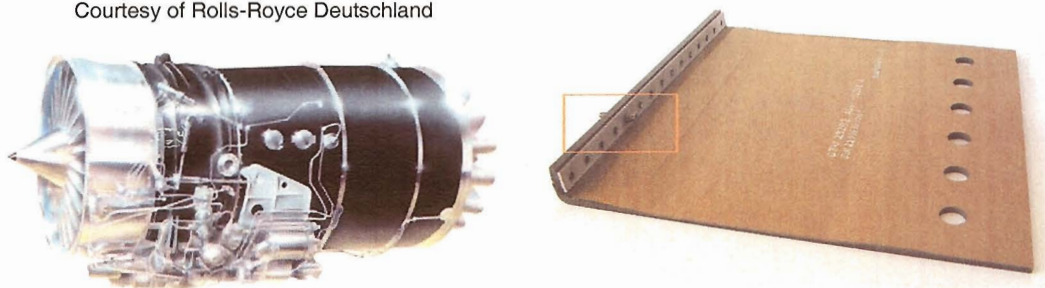


Figure 6. Rolls-Royce Tay aero engine and 20-ply flange test component.

Prior to the simulation, the flange connection was experimentally loaded by tension force until the complete structural breakdown occurs. The results gained from this test should be numerically reproduced for a better understanding of the complex failure behavior of the flange. The newer 3-D failure criteria in combination with a convincing material degradation model were chosen for this purpose. At this point, it has to be mentioned that the appearance of the first relevant ply failure, which is characterized by a steep descent in the load-displacement curve, should be the

major criteria for composite dimensioning. Nevertheless in terms of safety requirements it is also important to know how does the laminate react after the first ply failure. Can the load still be increased or does the complete component fail immediately? For that purpose the numerical investigations have to be proceeded, if the first relevant ply failure was always detected.

4.2 20-ply flange

The datum-base flange consists of 20 woven fabric plies of carbon fibers with orientations of $\pm 45^\circ$ and $0^\circ/90^\circ$. Four elements along the thickness were used to model each single ply of the laminate lay-up. At this the upper and the lower elements belong to one direction, whereas the two middle elements have an orientation rotated about 90° with respect to the outer elements. Due to this modeling, unrealistic warping of a single ply under uniaxial load can be avoided. The total number of hexahedron-elements of the flange part is 62,000; the overall DOF number is around 277,000.

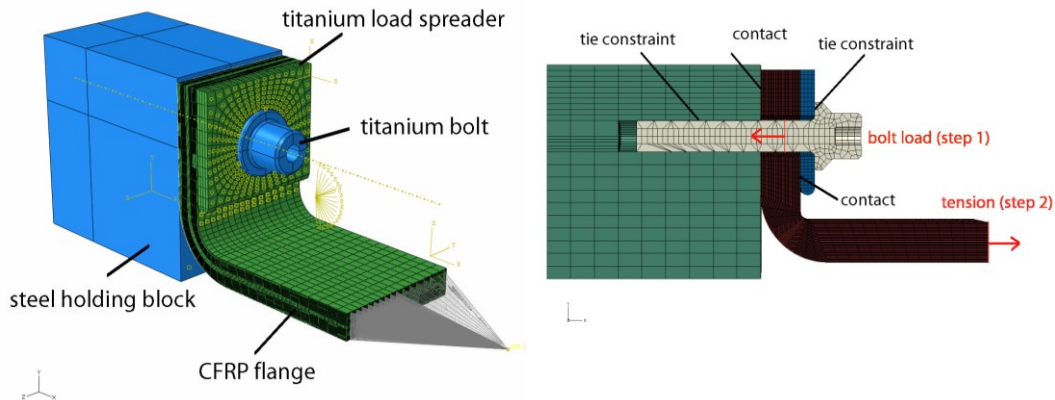


Figure 7. Finite element assembly including a 20-ply flange part.

Contact definitions were used between the flange and the metallic holding block as well as between the titanium load spreader and the flange component. On the contrary to this a tie constraint was chosen for the bolted connection. Finally the nodal displacements in z-direction were set to zero aiming at a simulation of the boundary conditions between the 20 single sections of the symmetric real component (see Figure 6, right).

The flange displacement steadily increases with rising number of increments in the FE analysis, similar to the former experimental investigations. The resulting load-displacement curve is shown in Figure 8.

As presented, the numerically and the experimentally determined load-displacement curves are very similar when compared to each other. The differences between the two curves are caused by the combination of uncertainties concerning material properties and failure resistances as well as concerning boundary conditions of the experimental investigations. Geometrical tolerances and tolerances due to real fiber-orientation also have to be mentioned in this context.

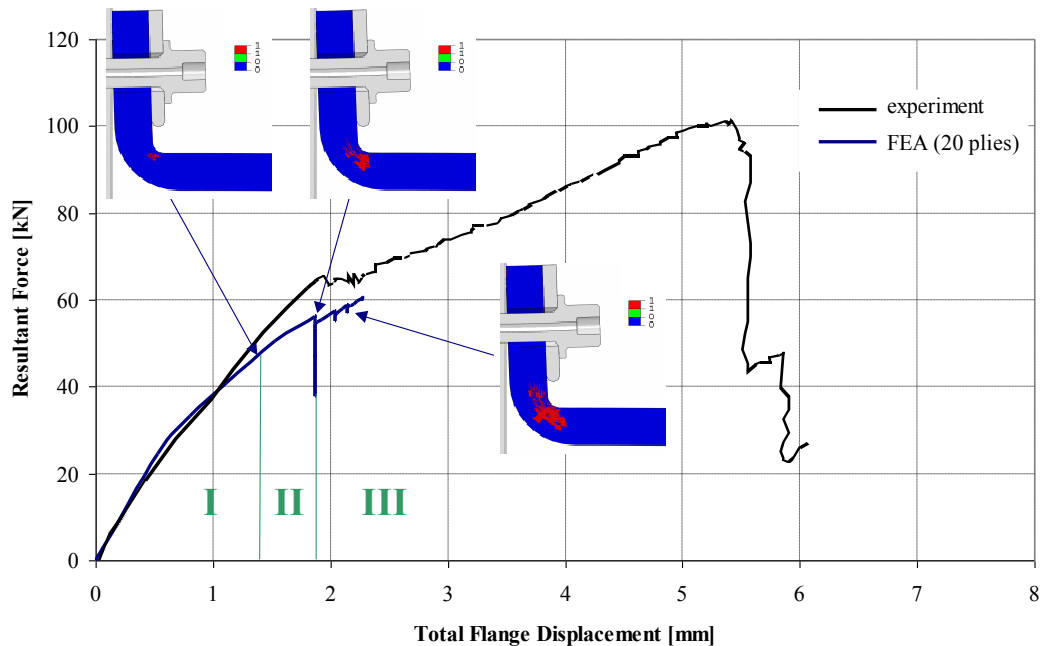


Figure 8. Load-displacement curve of experiment and FE calculation and failed flange areas at different load levels; datum 20-ply model.

Aiming at a better interpretation both curves have to be divided into three section:

Section I:

When the flange displacement starts to increase, a steady rise of the resulting force can be found in the first section of the diagram. The resulting slope of the load-displacement curve indicates the total assembly stiffness at each load level. The appearing stress values of every element are still below the limiting values of the fiber-failure as well as the inter-fiber-failure criteria.

Section II:

Although no remarkable decline can be found in the curves until a displacement of ~1.9 mm, the first inter-fiber-failure occurs at a displacement of ~1.4 mm (see Figure 8). Generally small areas of inter-fiber-failures of composite structures cannot be detected only using experimental investigation methods. The resulting small gradients of the load-displacement curves which are typically induced by initial inter-fiber-failures mostly cannot be detected using low sampling rates. Thus the first ply failure that can be obviously detected on the test sheet occurs at higher load level at a displacement of ~1.9 mm.

Under the appearing bending/tension condition the occurring maximum principal stress vector of the inner and outer plies in the arc is in perpendicular direction, so the 0° -fibers are able to take over this flux in that area. Contrary to this the maximum principal stress vector in the middle section of the arc is radially directed. Thus high inter-fiber-failure efforts are induced by the high stresses in direction of the flange thickness. As a result delamination is initialized in that curved area, so the gradually failure sequence starts, while the effort of fiber-failure is only around 30% (see Figure 9). At this condition neighbored plies start to bridge the failed ply section. If the load increase continues, more and more elements fail due to high transverse tension stress in the middle of the laminate. Thus the total flange stiffness is lowered, characterized by a decreased slope of the load-displacement curve.

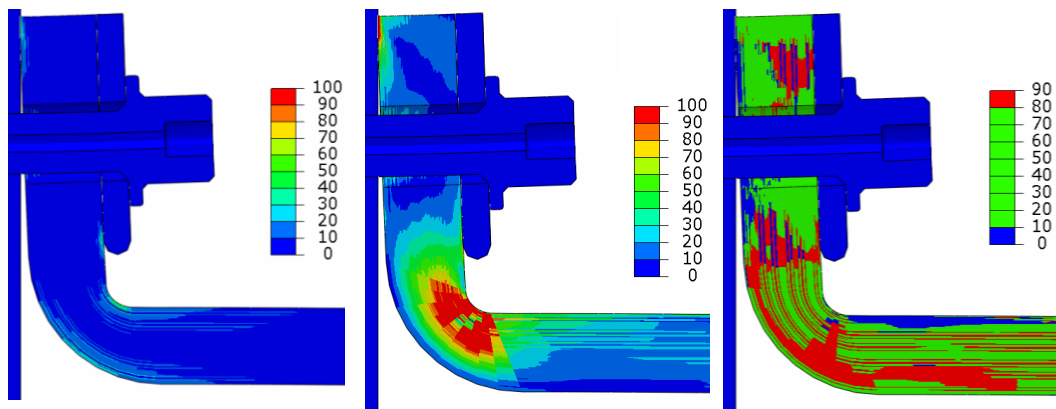


Figure 9. Effort of fiber-failure [%] (left), effort of inter-fiber-failure [%] (middle) and action-plane angle [°] (right); FE calculation at $u_1 = .9mm$ (20-ply model).

Section III:

Caused by the proceeding flux redistribution, adjacent plies are more loaded due to the still-increasing tension until their failure effort also exceeds the inter-fiber-failure resistance amount. At a displacement of $u_1 = .9mm$ first fiber-failures occur in and next to the pre-damaged areas.

That particular loading condition is marked by a distinctive decrease in the calculated load-displacement curve (see Figure 8). From this load level the gradient of the curve strongly decreases due to the reduced total component stiffness. However the bridging effect of the neighbored plies with higher load capacities still avoids an immediate composite breakdown. If the increase of the flange displacement continues, more and more ply failures occur, which is characterized by the non-steady curve shape in the diagram.

Finally the whole flange component fails at a resulting force of ~ 105 kN, because all of the remaining non-failed plies also exceeded their load capacity.

4.3 Improvement of flange lay-up

Aiming at a reduced aero engine mass, also the flange component has to be optimized concerning its mass. With this in mind a modified FE-flange model was created to determine whether a reduced number of plies could be realized without a decreasing mechanical strength. Based on the knowledge about the datum-base 20-ply flange the lay-up sequence was modified as follows: Six of the plies with the smallest radii were completely removed. In the outer region all of the outer $\pm 45^\circ$ -plies were replaced by $0^\circ/90^\circ$ -plies, whereas some of the inner $0^\circ/90^\circ$ -plies were replaced by $\pm 45^\circ$ -plies. To sum up, the total number of $\pm 45^\circ$ -plies decreased from 10 down to 4, while the number of $0^\circ/90^\circ$ -plies remains unchanged. Thus the specific radii of the outer $0^\circ/90^\circ$ -plies was increased which has a positive effect on the total component stiffness and counters the reduced flange thickness. The remaining $\pm 45^\circ$ plies are located in or next to the neutral layer with regard to the bending stress state component in the arc of the flange.

4.4 14-ply flange

The new, recalculated load-displacement curve of the 14-ply flange is shown below in Figure 10.

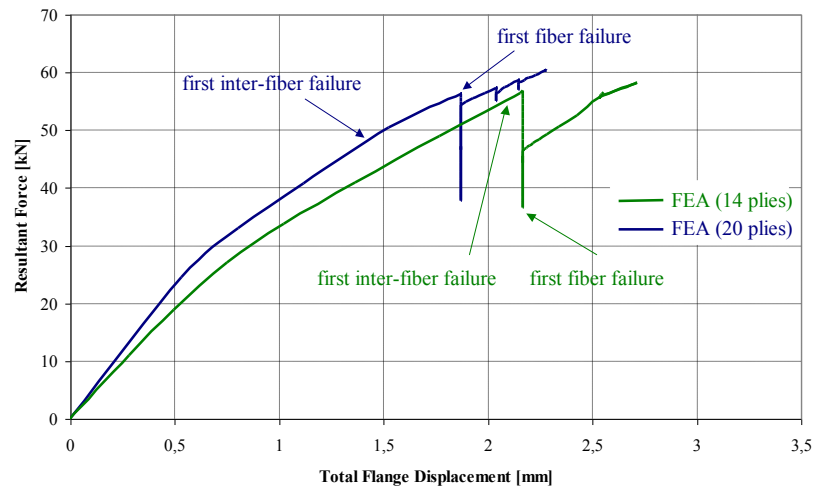


Figure 10. Calculated load-displacement curve of 20-ply and 14-ply model.

Although the gradient of the load-displacement curve is lower compared to the initial 20-ply flange, which is caused by the reduced flange thickness and the resulting total flange stiffness, the amount of tension force which results in the first significant composite failure (main design criteria) is nearly the same as for the datum-base design.

The reason for that is the replacement of the outer $\pm 45^\circ$ -plies by $0^\circ/90^\circ$ -plies, that makes the composite lay-up more load-case-attuned. Thus the reinforcing 0° -fibers are able to take over major load parts during the flange bending-tension process, which reduces the matrix loading as well as the effort of inter-fiber-failure at the same forcing level (see Figure 11).

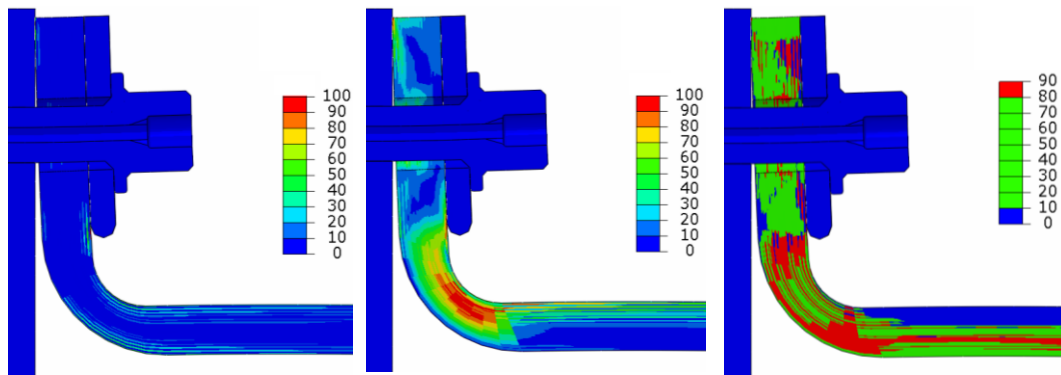


Figure 11. Effort of fiber-failure [%] (left), effort of inter-fiber-failure [%] (middle) and action-plane angle [°] (right); FE calculation at $u_1 = 0.1mm$ (14-ply model).

5. Summary and outlook

Classical two-dimensional failure criteria (e.g. Tsai-Wu, Hill, etc.) that are usually implemented in commercial Finite-Element software are not able to distinguish between the different failure modes of fiber-reinforced composite structures. However this missing detailed knowledge is essential to achieve a better understanding of their complex failure behavior and for the prediction of gradually structural disintegration.

Newer three-dimensional, so-called action-plane based failure criteria (e.g. Puck, Jeltsch-Fricker, LaRC04) offer the possibility to close this loophole. With this in mind a FORTRAN subroutine in combination with a material degradation procedure basing on the usage of user-specified material definitions was used to implement this newer failure criterion into Abaqus.

As a first application a flange connection of the bypass duct of a Rolls-Royce aero engine was exemplarily chosen to verify the prediction quality of the newer failure criteria. At this the experimental determined load-displacement curve could be numerically reproduced within only small deviations.

Detailed information created by new field outputs were subsequently used to improve the datum-base flange design as well as the lay-up sequence. As an example the outer plies of the modified flange only consist of $0^\circ/90^\circ$ -plies now, which is much more advantageous in terms of the appearing flange bending/tension condition. At this the significant decrease of inter-fiber-failure effort in the highly loaded curve-section also indicates the resulting matrix load reduction. Altogether, a mass reduction of the flange of 30% could be realized keeping the identical component strength.

Thus the capabilities of the new three-dimensional failure criteria for a realistic prediction of the complex failure behavior of composite structures were demonstrated in detail. The application of newer 3-D-failure criteria in the dimensioning- and redesign process that is exemplarily shown in this paper offers the possibility for an improvement of composite structures of future- as well as in-service aero engines.

6. Appendix

$CFRP$	=	carbon fiber reinforced plastics
$E_{\text{Delamination}}$	=	effort of delamination
E_{FF}	=	effort of fiber-failure
E_{IFF}	=	effort of inter-fiber-failure
FF	=	fiber-failure
IFF	=	inter-fiber-failure
p	=	slope of the $\sigma_n - \tau_{n1}$ failure envelope at $\sigma_n = 0$
$R_{\parallel}$	=	Fracture resistance of an action plane against parallel stressing
$R_{\perp}$	=	Fracture resistance of an action plane against transverse stressing
$R_{\perp\parallel}$	=	Fracture resistance of an action plane against transv./parallel stressing
$R_{\perp\perp}$	=	Fracture resistance of an action plane against transv./transv. stressing
u	=	displacement vector
x_i	=	coordinate system of a unidirectional ply
$\theta_{(f)}$	=	angle of action/fracture-plane [°]
σ_{ij}	=	stress in a unidirectional ply with respect to global coordinate system
σ_n	=	normal stress acting on the action plane
τ_{ij}	=	shear stress of a unidirectional ply in the i - j -plane
τ_{n1}	=	normal/longitudinal shear stress acting on the action plane
τ_{nt}	=	normal/transverse shear stress acting on the action plane

7. References

1. Altenbach, H./Altenbach, J., „Einführung in die Mechanik der Laminat- und Sandwich-Tragwerke, Modellierung und Berechnung von Balken und Platten aus Verbundwerkstoffen“, Deutscher Verlag für Grundstoffindustrie, Stuttgart, Germany, 1996.
2. Azzi, V.D., Tsai, S.W., „Anisotropic Strength of Composites“, Experimental Mechanics, SESA Spring Meeting, pp. 283-288, September 1965, Denver, Colorado, U.S.A.
3. Cuntze et. al., „Neue Bruchkriterien und Festigkeitsnachweise für unidirektionalen Faserkunststoffverbund unter mehrachsiger Beanspruchung, Modellbildung und Experimente“, Fortschrittsbericht VDI-Reihe 5, Nr. 506, VDI-Verlag, Düsseldorf, Germany, 1997.
4. Hinton, M.J., Soden, P.G., „A comparison of the predictive capabilities of current failure theories for composite laminates, judged against experimental evidence“, Composite Science and Technology 62 pp. 1725-1797, 2002
5. Jones, „Mechanics of composite materials“, Scripta Book Company, Washington D.C., U.S.A., 1975
6. Klauke, T., „Neuere Bruchkriterien für den Zwischenfaserbruch und FE-Anwendung auf reale CFK-Bauteile“, Diploma-thesis, Chair of Structural Mechanics and Vehicle Vibration Technology, BTU Cottbus, Germany, 2003
7. Michaeli/Huybrechts/Wegener, „Dimensionieren von Faserverbundkunststoffen, Einführung und Hinweise“, Carl-Hanser-Verlag, Munich, Germany, 1994
8. Pinho, S. T. et.al., „Failure models and criteria of FRP under in-plane or three-dimensional stress states including non-linearity“, NASA/TM-2005-213530, Hampton, VA, U.S.A., 2005
9. Puck, A., „Festigkeitsanalyse von Faser-Matrix-Laminaten, Modelle für die Praxis“ (Strength Analysis of Fiber-Matrix/Laminates, Models for Design Practice), Carl-Hanser-Verlag, Munich, Germany, 1996.
10. Puck, A., Schürmann, H. „Failure Analysis of FRP Laminates By Means Of Physically Based Phenomenological Models“, Composites Science and Technology, No. 58, pp. 1045-1067, 1998.

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