Implications of free-edge effect at thin plain-woven carbon fiber reinforced plastic laminates with out-of-plane waviness under cyclic loading

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Abstract
The onset of damage caused by the free-edge effect in plain-woven carbon fiber reinforced plastic (CFRP) specimens with an out-of-plane waviness under tension-tension fatigue loading is investigated. Numerical calculations show that interlaminar and intralaminar stresses close to the out-of-plane waviness are higher than the equivalent stresses at the surrounding edge regions. Using submodels, the influence of the chosen out-of-plane waviness can be better assessed. The free-edge effect of the considered specimens, which originates from stress gradients between plies of different orientation, is altered by the change in the stress field caused by the out-of-plane waviness. Large interlaminar stresses between plies of the same orientation are obtained, which contrasts with existing literature. In experimental fatigue testing it is found that cracks at the free edge appeared at the predicted locations, and after reaching crack saturation, in regions close to the out-of-plane waviness, interlaminar and intralaminar stresses lead to additional cracks along the whole free edges. The experimental tests are supported by a three dimensional image correlation system (3D-DIC), a thermal-imager and a digital photographic camera, which allows detailed examination of selected areas. Visual observation during fatigue testing and post-mortem inspection show good agreement between experimental data and numerical calculations in relation to the location of the damage initiation. As a result, out-of-plane waviness at free edges must be considered as an additional significant fatigue damage initiation location in laminate analysis.

Keywords
Composite fatigue, out-of-plane waviness, free-edge effect, thin plain-woven CFRP

Introduction
Plain-woven carbon fiber reinforced plastics (CFRP) are widely used due to their good handling usability and tolerance against impact damages. Nowadays, use of these materials are very common in the aerospace industry, mostly for thin-walled structural components.\textsuperscript{1,2} Consequently, analytical and numerical approaches have been developed in the last 50 years to analyze and design such structural components. As already emphasized by Mittelstedt,\textsuperscript{3} the calculations were initially based on the classical laminate plate theory (CLPT), but in this theory, stress components in thickness direction are not taken into account. In order to design damage-tolerant components, it is necessary to consider all potentially detrimental influences on the fatigue strength. Therefore, it is essential to take the finite dimensions and possible defects into account. They may emerge during the manufacturing process, or in operational service and can result in premature failure. Such manufacturing defects are e.g. dry spots, in-plane fiber waviness, ply folds\textsuperscript{4–6} and out-of-plane waviness. Out-of-plane waviness, which are also termed

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wrinkles have already been studied in detail, whereby the attention was placed on the different geometries as well as on different materials and locations. However, it was found that wrinkles are often supported by resin pockets. For their experimental investigations wrinkles were artificially created by laminating over a steel rod, or by adding extra lamina strips.\cite{7,8,9} In order to determine the effect of wrinkles experimentally, digital image correlation was used. Elhajjar et al. demonstrated that for static tensile loading, interlaminar cracks along the sides could be detected, while observing the front surface.\cite{10} However, only static measurements on unidirectional laminates were made.

An often overlooked phenomenon which causes reduction in fatigue strength even in defect-free material is the free-edge effect. At free edges, a three-dimensional stress state is present, which is caused by adjacent plies of different orientation or different material properties. More precisely, the associated interlaminar stress components are involved in causing the free-edge effect.\cite{11,12}

According to Saeger,\cite{13} who investigated interlaminar stresses in plates with holes, the two phenomena that lead to these stresses should be considered separately from each other. These are on the one hand stress gradients in the load field, caused by the presence of a hole, and on the other hand the free-edge effect. However, woven fabrics are not discussed in this context.

The free-edge effect on plain-woven CFRP was described by Whitcomb et al.\cite{14} They carried out linear-elastic numerical investigations on unit cells and thus obtained conclusions regarding the free-edge effect on the stresses within the plain-woven laminate. In their findings, they underlined the influence of the weave architecture on the interlaminar stresses. This was confirmed by Espadas-Escalante,\cite{15} where again the problem was analyzed on a numerical basis while considering woven plies. In addition, intralaminar stresses at the free edge were discussed.

The present article expands the analysis of plain-woven CFRP to include an imperfection and considers damage initiation at the free edge under tensile fatigue. In detail, the influence of a geometrical half-wave imperfection, which is applied to thin plain-woven CFRP with symmetrical layup, on interlaminar and intralaminar stresses under cyclic tension-tension loading is examined. The focus of the investigation is placed on the accelerated onset of damage by the free-edge effect. Besides the complex stress state, this out-of-plane waviness leads to a distortion of the load field. Furthermore, this work includes the comparison of experimental and numerical results. Therefore, a global model is created for the numerical calculation, with which areas with high interlaminar stresses are identified. In the following, submodels are defined, to which boundary conditions of the global model are imposed. These submodels provide a more detailed description of the free-edge effect. The results gained from experimental tests are used to validate the global numerical model and to evaluate the onset of damage due to the free-edge effect in the submodels.

The article is structured as follows. First the geometry of the specimen and the material are specified. In the next section the test set-up is described. This includes the specimen preparation, a description of the measuring equipment and the explanation of the test program. Numerical modelling and result presentation are followed by the experimental result section. Prior to the conclusions, a discussion of the results will be given.

### Geometry and used material

Flat specimens with a half-wave imperfection are used to investigate the influence of an out-of-plane waviness on the damage initiation caused by the free-edge effect. In order to ensure a high repeatability in the production of specimens, an artificial form of the half-wave imperfection is assumed. A rather small waviness is introduced as this may be less likely picked up by quality control, yielding a more practically relevant imperfection case. The specimen geometry and the associated dimensions are shown in Figure 1. Also, the clamping tabs are depicted, which are glued on for the experimental tests. In addition, a detailed front side view with the unsupported waviness is presented. In general, resin pockets would emerge at the waviness, which would support the waviness zone. In this investigation, the resin pockets are not taken into account as a negligible influence on the free-edge effect during tensile cyclic loading is assumed. Moreover, resin pockets would prohibit the DIC measurements.

Four plain-woven CFRP prepreg plies with a ply thickness of 0.204 mm are used to assembly the layup. The prepreg is composed of 3k-T650 tows and

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**Figure 1.** Geometry and associated dimensions of the specimen with half-wave imperfection.
impregnated with a modified Solvay CYCOM® 970 epoxy resin. The fiber volume fraction is 0.6 inside the tow and the tow volume fraction is 0.756 in the weave. For this study a stacking sequence of [0f/45f]s, is chosen, where the small f denotes a fabric. The stiffness properties of the global FE models are manually calculated from the material properties, listed in Table 1, by means of classical laminate theory.

It is assumed that the tows have transverse isotropic material behavior and the matrix is isotropic. The parameters which are assigned to the tow are designated with $T$, those of the matrix with $m$. To differentiate between the waviness of the woven material and the waviness caused by the half-wave imperfection, the waviness ratio $WR = h/\lambda$ is compared with the aspect ratio $AR = D/L$. Where $h$ is the thickness of the ply, $\lambda$ the wavelength of the tow, $D$ is the depth of the imperfection and $L$ the length of it. This gives a waviness ratio $WR = 0.05$ and an aspect ratio of $AR = 0.025$.

**Test set-up**

To produce the desired geometry of the specimens with half-wave imperfection, first the contour of the half-wave imperfection was milled into an aluminum plate. In a subsequent step, a CFRP mould was obtained out from this aluminum plate. On this moulded form, the four plies with a stacking sequence of [0f/45f]s, were hand-layed. In addition ply 3 and ply 4 were flipped to reduce the internal stresses that arise during the subsequent autoclave-curing. With the selected manufacturing process the formation of a resin pocket could be prevented. A consecutive application of glass-fiber-reinforced-plastic (GFRP) clamping tabs, which were cured at room temperature, and water jet cutting to size completed the specimen production. Based on ASTM D3039, the edges of the specimens were smoothed by grinding.

A µε thermo-imager cutting to size completed the specimen production. Based on ASTMinitiation of damage and the progressive growth are identified and assessed over the lifetime with the assistance of optical measuring systems. For this purpose a digital image correlation (3D-DIC) system from Correlated Solution® and a digital photographic camera are used. To prepare the specimens for the 3D-DIC system, the entire specimens are coated with a white primer and black dots are airbrushed on to generate the, so-called speckle pattern. This pattern is used to measure the surface strains.

All experimental tests are carried out with a BPS-LH0025 servo-hydraulic cylinder from Zwick-Roell® that allows to apply loads up to 25 kN. Hydraulic wedge grips MTS 647 are used to clamp the specimens in the testing facility. In order to ensure that only tensile loads are applied, the grips are secured against rotation using self-developed guide columns and slide bearings. In Figure 2, a schematic representation of the test set-up and location of the optical measuring devices is given. To compare the strains computed with the submodels with those from experimental testing, the DIC would need to be focused on the free edge. The thin laminate thickness of 0.816 mm restricts the DIC use, since the available standard-lens equipped system cannot evaluate and track such thin surfaces. Consequently, the 3D-DIC system is arranged in such a way that the cameras focus on the indentation due to the half-wave imperfection. The thermo-imager is positioned opposite to the 3D-DIC system. In addition, a digital photographic camera is mounted in front of the specimens positioned opposite to the 3d, the grips are secured against rotation using self-developed set of possible cracks.

From static tests, which are based on ASTM D4762-18, the stress levels for cyclic testing are identified. For the static test series, six specimens are used. Cyclic tests are then carried out at a stress ratio of $R = 0.1$ and at two different upper stress levels, which equal 75 and 80 percent of the average static tensile strength. All tests are performed until total fracture of the specimen has occurred or two million cycles are reached.

The controlling of the specified test program is performed with a CATS Control Cube system, whereas a sequence block is repeated multiple times throughout

### Table 1. Material properties of the prepreg components.

| Tow properties | Value and unit |
|----------------|----------------|
| $E_{T,1}$     | 183050 MPa     |
| $E_{T,2} = E_{T,3}$ | 9260 MPa   |
| $v_{T,12} = v_{T,13}$ | 0.237    |
| $v_{T,23}$    | 0.387          |
| $G_{T,12} = G_{T,13}$ | 4600 MPa |
| $G_{T,23}$    | 3380 MPa       |
| Matrix properties | Value and unit |
| $E_m$         | 3480 MPa       |
| $v_{m}$       | 0.35           |

![Figure 2. Test set-up with operated optical measuring devices.](image-url)
the specified test program. A schematic representation of a single sequence is shown in Figure 3. Thereby a rest phase for $t = 10$ s at $F_{\text{stat},0} = 0$ kN followed by a quasi-static ramp with a rate of 12 kN/min up to $F_{\text{stat}} = 5.7$ kN is specified. The selected force $F_{\text{stat}}$ corresponds to a strain of 3500 $\mu$e of a straight reference specimen and is chosen on the basis of linear elastic behavior. In addition, this force is below the mean load of the cyclic testing sequence. From the measurements during the ramp, the stiffness of the specimen is determined at the start of each sequence. By comparing this value with the stiffness during the following sequence blocks the stiffness degradation due to fatigue can be obtained. At $F_{\text{stat}} = 5.7$ kN another rest phase for 10 s is defined. A following ramp with rate 12 kN/min raises the load to the mean load $F_{\text{mean}}$, from where the cyclic loading starts. Five fade-in cycles are added. All cyclic loadings are carried out with a frequency of $f = 5$ Hz. A final ramp with a rate of 12 kN/min up to $F_{\text{stat},0} = 0$ kN concludes the sequence.

**Numerical models**

The influence of the half-wave imperfection regarding the free-edge effect is assessed by numerical calculations. For this purpose, numerical finite element (FE) models of a straight reference specimen and a specimen with a half-wave imperfection are created with Abaqus 2018. These global FE models are used to provide the boundary conditions for the submodels. The generated global model of the straight reference specimen differs from the specimen with a half-wave imperfection only by the missing imperfection and a coarser mesh. Boundary conditions remain the same. The straight reference specimen is used to emphasize the influence of the half-wave imperfection. Therefore three different submodels are generated. As locations for submodels the following regions are selected. The position of the submodel variants defined for the straight reference specimen are in the centre of the associated FE model, whereby one side of the submodels coincides with the free edge of the straight reference specimen. Two submodels are created for the specimen with a half-wave imperfection. The position of these submodels are depicted in Figure 4. A submodel is placed in the transition region and another submodel in the middle of the waviness. All submodels are placed at the free edge of the associated global model. Again one side of the submodels coincides with the free edge. For each submodel two variants are taken into account (cf. Figure 5). The difference between the two arrangements, which from now on are named standard variant and shifted variant, is that for the shifted variant, ply 2 and ply 3 are shifted by half of the weave pattern along the Y-axis (cf. Figure 5). The shift affects the position of ply 2 and ply 3 so that the 0f tows are visible at the free edge. Whereas the position of the
The two variants of the submodel, which are defined for the reference specimen, the center of the half-wave imperfection and for the transition radius, are used to describe the free-edge effect. For the created submodels, the stress components $S_{ZZ}$ and $S_{XZ}$ are evaluated at the free surface along predefined paths. The fact that the same base model is used for the reference submodel, the center of the half-wave imperfection and the transition radius, but with regard to the actual shape, makes it possible to compare the results of the stress components without any further computations.

The material properties for the plies of the global FE models are calculated from the component properties. They are assigned to the FE models as engineering constants. The coordinate systems are positioned at the half of the width, whereby the origin of the specimen with a half-wave imperfection is located within the half-wave. The coordinate system used to define the 0f plies are aligned as follows. The X-axis points in the direction of the clamping tab and the Z-axis indicates the thickness direction. Thereby the ply 1 is the lowest in Z-direction and ply 4 is the highest. The Y-axis points in transverse direction. To define the 45f plies an additional coordinate system is introduced, which is located at the same origin, but rotated around the Z-axis.

In Figure 4, a representation of the generated mesh for the specimen with half-wave imperfection, the coordinate system of a 0f ply, and the locations of the submodels is shown. For illustration symmetries are taken into account and so only a quarter of the geometry is displayed. The region next to the out-of-plane waviness and close to the free edge are meshed with more elements. Each ply is discretized in thickness direction by three elements. The positions where the submodels are placed are indicated.

According to the experimental test set-up, the boundary conditions for the global FE models are assigned. The global FE models are fixed at one end (X-, Y- and Z-directions of every node of the model face) and the nodes located at the other end are translated by displacement boundary conditions along the X-axis. Due to the long specimens length, the Poisson Poisson other end are translated by displacement boundary conditions along the X-axis. Due to the when a tensile force of $F_{stat} = 5.7\, \text{kN}$ is measured on the test rig.

Using TexGen, the submodels are created and exported for analysis in Abaqus. The dimensions of the tows and the thickness of the plies, as well as the spacing between tows, are considered. The corresponding material properties of tows and matrix are listed in Table 1. It is assumed that tows and matrix are ideally bonded. As already mentioned, the resulting displacements of the global FE models are transferred to the submodels as boundary conditions. Thereby, the displacements of the individual nodes of the global FE models are interpolated onto the nodes of the submodels. In more detail, the nodes of the matrix and the nodes of the tows, which are located at the lateral surfaces, are used. Only the nodes placed at the free edge and at the upper and lower surface are not included. In Figure 5 the two variants of the created submodels are depicted, whereby the matrix has been hidden for an improved visualization. In addition, the elements that are located on the free edge are displayed in a different color. However, as can be seen in Figure 4, the submodels in the transition radius and in the middle of the waviness are not planar in shape. But since TexGen can only handle planar models, an intermediate load step is required in which the submodels are forced into the required shape. In this intermediate load step the initially straight submodels are formed into the specimen specimen this intermediate load step submodels are stress-relieved from residual forming stresses and used for the analysis.

In order to evaluate the strain components relevant for the free-edge effect, paths are defined for the individual submodels and associated variants. These paths are shown in Figures 6 to 8. The two variants of the reference submodels are shown in Figure 6. The paths are arranged equidistant over the length. Those paths for the submodel of the standard reference model are denoted with Ro, those for the shifted reference model with Rs. Similar to the paths arranged for the reference submodels the paths for the center submodels are defined in Figure 7. The paths of the standard submodel are labeled Co, the shifted variant is identified by Cs. Finally the paths for the submodels of the transition radius are defined in Figure 8. Again the paths of the standard variant are denoted with To, the shifted variant with Ts. All paths are evaluated along the positive Z-direction. The generated FE models are computed using a static implicit solver supplied by Abaqus, under consideration of linear elastic material behavior.

![Figure 6. Analysis paths of the reference submodels.](image-url)
and geometric non-linearity. As damage initiation is expected to take place within few cycles, it is assumed that the matrix degradation and visco-plastic effects on the free-edge effect are negligible. In summary, the global model of the straight reference specimen consists of 253773 nodes and 230400 elements of type C3D8R. The same element type is also used for the specimen with a half-wave imperfection. In this model, 1068717 nodes and 970992 elements have been created. The submodels are composed of quadratic tetrahedral elements C3D10. The three submodels representing one variant, which differ only in their shape. However, the two variants of the submodels differ in the number of elements and nodes. The standard submodels consists of 214106 nodes and 149572 elements, whereas the shifted submodels consists of 258795 nodes and 182742 elements.

According to Espadas-Escalante, a similar procedure is used, so that the mesh is selected in the best achievable form, while taking the computation time into account. Also, a local mesh refinement is not performed. Nevertheless, it is possible to make a qualitative assessment of the stresses.

Numerical results

Initial numerical calculations on the global model supply the boundary conditions for the three submodels and the associated two variants. Consequently, the results of the global FE models are presented first. For the global model of the straight reference specimen a major principal strain of 3500 με is calculated. The results of the specimen with a half-wave imperfection reveal superimposed bending stresses when tensile loads are applied on the out-of-plane waviness. As the 3D-DIC system determine strains within the X-Y plane during experimental testing, the numerical results of the model of the specimen with a half-wave imperfection are transformed in the same global directions. The influence of the out-of-plane waviness can also be observed in the resulting strains, which are depicted in Figure 9. Thereby major principal strains E1 and the minor principal strains E2 of ply 1 are shown. The selection is made for the bottommost surface of ply 1, because the 3D-DIC system has recorded the strains of this ply during the experimental testing.

In regions far away from the half-wave imperfection, major principal strains of 3800 με are calculated. As a result of global bending caused by the half-wave imperfection, also different principal strains on the bottom and upper surfaces far away from the half-wave imperfection are present. The counter bending in the transition radii resulted in major principal strains reduced by a factor of 2.2 with respect to the major principal strains calculated far away from the half-wave imperfection. In the center of the imperfection a large rise in major principal strains to a factor of 1.63 took place. At the free edges the major principal strains in the transition radii are reduced additionally. By contrast, the major principal strains are increased at the free edges in the center of the imperfection. Due to Poisson’s effect the minor principal strains E2 provided high negative compressive strains at those regions.
where high positive major principal strains $E_1$ are present. The minor principal strains $E_2$ are lower in those regions where low major principal strains $E_1$ are acting. With those findings the submodels can now be evaluated.

Despite the fact that no convergence study is performed and consequently the absolute values are not proper, it is permissible to evaluate the stresses along the defined paths. The reference submodels, which obtained their boundary conditions from the straight reference specimen, are used to determine the relevant stress components caused by the free-edge effect. The relevant stress components are $S_{ZZ}$ and $S_{XZ}$, whereas $S_{YZ}$ is negligible. This is also consistent with the research of Whitcomb.\textsuperscript{14} In Figures 10 and 11 the stress results for the reference submodels are represented. On the horizontal axis the corresponding stresses are plotted. The vertical axis shows the normalized distance along the Z-direction. In addition the ply boundaries between adjacent plies are added as dashed horizontal lines and the individual plies are labeled. According to the free-edge effect, damage is initiated only by positive normal stresses and preferably between plies with different orientations.\textsuperscript{15} Consequently, compressive stresses in thickness direction do not cause damage initiation. The individual graphs shown in Figure 10 reveal that only ply 2 and ply 3 are exposed to positive normal stresses in thickness direction. Similar results are obtained for both variants. The discontinuities in the stresses are caused by stiffness changes. These stiffness changes result from the low stiffness of the matrix and the high stiffness of the adjacent tows. For the reference submodel, the stress paths along $R_01$ and $R_02$ are almost identical and they are symmetrical with respect to the stacking mid-plane. The stresses $S_{ZZ}$ along path $R_02$ follow a similar trend, but are generally lower. For the shifted variant, the stress curves of $S_{ZZ}$ have a similar trend, whereby two neighbouring tows are present in the middle of the laminate ($Z = h/2$). Thus, these stresses are higher at these locations. The only significant shear stresses $S_{XZ}$ for the standard variant of the reference submodel, which are depicted in Figure 11, are located within ply 1 and ply 4. Thus, non-zero intralaminar shear stresses also exist. This was already noted by Escalante.\textsuperscript{15} Moreover significant shear stresses $S_{XZ}$ occur in the shifted variant along $R_s2$. These arise at the interfaces of matrix to the adjacent 0 degree tows of ply 2 and ply 3. Otherwise, the shear stresses $S_{XZ}$ in ply 2 and ply 3 are negligibly small for both variants.

Similar to the results of the reference submodels, the results for the submodels at the center of the half-wave are evaluated. The extracted paths of these submodels are shown in Figures 12 and 13. For the standard submodel and the shifted submodel, it is determined that the stresses $S_{ZZ}$ are positive in ply 2 and ply 3. Due to superimposed bending stresses, the maximum stresses $S_{ZZ}$ are shifted into ply 2. This shift exists for both submodel variants. As Figure 9 shows, ply 1 is subjected to significantly higher major principal strains. Consequently, the stresses in this ply are also higher. The shear stresses $S_{XZ}$ shown in Figure 13 confirm this phenomenon. Within ply 1 the shear stresses $S_{XZ}$ are higher than the comparable shear stresses $S_{XZ}$ of ply 4. Along $C_s2$ additional shear stresses $S_{XZ}$ occur which are propagated between the matrix and the adjacent 0 degree tows. Once again the shear stresses $S_{XZ}$ are negligibly small between ply 2 and ply 3.

The results for the submodels, which are created for the transition radius are shown as graphs in Figures 14 and 15. There are again no positive stresses $S_{ZZ}$ in thickness direction with respect to ply 1 and ply 4 for

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**Figure 10.** Through the thickness stresses $S_{ZZ}$ for both variants of the reference submodels.

**Figure 11.** Shear stresses $S_{XZ}$ for both variants of the reference submodels.
both variants. However, high positive stresses $S_{ZZ}$ are present in ply 2 and ply 3. As a consequence of the counter bending, the highest stresses are located in ply 3. The stresses $S_{ZZ}$ in ply 3 are significantly higher for the shifted submodel compared to the standard submodel. The graphs of the shear stresses $S_{XZ}$ for the standard submodel and the shifted submodel prove that higher stresses exist in ply 4 caused by bending. Furthermore, high shear stresses $S_{XZ}$ are also involved in ply 2 and ply 3 along To3 and Ts3. Additional shear stresses $S_{XZ}$ along Ts2 has significant magnitudes as well. They are found in ply 2 and ply 3.

**Test results**

3D-DIC measurements and the images of the digital photographic camera taken during the experimental tests are used to validate the results obtained from the numerical calculations. For each specimen the quasi-static ramp to $F_{stat} = 5.7$ kN is recorded with the 3D-DIC system. It is observed that the strains recorded during this initial loading do not differ much for the individual specimens. Figure 16 shows the major principal strains $E_1$ and minor principal strains $E_2$ obtained by the 3D-DIC measurement of a selected specimen. The strain measurement confirms, that the specimens with a half-wave imperfection are additionally loaded in bending when tensile loads are applied, meaning the strain field is no longer homogeneous. The bending strains and tensile strains are superposed in the center of the waviness. In the transition radii, the resulting strains are reduced by bending in the reverse direction. The thermo-imager showed an average temperature rise of 1.2°C for each tested
specimen. Thus the influence of temperature on the results is considered negligible.

The digital photographic camera documents the damage initiation due to the free-edge effect. The camera is focussed on one of the lower transition regions. For each tested specimen, pictures are taken in constant time increments. However, it should be noted that there are four transition regions and that the digital photographic camera only observes one of them. Thus, initial damage might occur earlier in any of the other transition regions. Nevertheless, all tested specimens show a similar behavior regarding the free-edge effect, such that for each load level it is sufficient to display the response of a representing specimen. In Figures 17 and 18 these obtained results are shown. In the pictures, damage initiation resulting from the free-edge effect can be identified. As the side of the specimens is also coated with an airbrushed speckle pattern it is easily possible to relate the individual images to the initial state. The cross section views are heavily magnified and rotated.

For the specimen shown in Figure 17, where damage due to the free-edge effect is observed, damage initiation occurred after approximately 100 cycles. The high upper stress level of 80% of the tensile static strength causes initial damage in form of a crack. This crack is located inside ply 3 and between ply 2 and ply 3. As the number of cycles increases, further cracks occur. After 20000 cycles the majority of the plies at the free-edge of the transition radius are separated.

For the specimen, which was tested at 75% load level, damage initiation begins at roughly 200 cycles. Similar to the specimen tested at 80% load level, a first crack is located inside ply 3 and between ply 2 and ply 3. However, even after 81000 cycles no new cracks appear in the captured image area. This could be interpreted that crack saturation has been reached. Nevertheless, the final crack state was different for each specimen.

**Discussion of results**

The benign form of the half-wave imperfection and the selected manufacturing process prevent the formation of a resin pocket. Thus, the focus can be placed on the unsupported waviness, based on the assumption that the resin pocket has no effect on the free-edge effect under tension-tension fatigue loading. When comparing the numerical results of the specimen with half-wave imperfection with the according experimental data, the principal strain distributions as illustrated in Figures 9 and 16 show good agreement. The small differences could originate from local material inhomogeneities as well as from internal stresses introduced in the manufacturing process. However, the global FE model reflects the behavior of the real specimens and thus provides appropriate boundary conditions for the submodels, where the highest out-of-plane stresses $S_{ZZ}$ are predicted. In addition, the photos of the digital photographic camera clearly identify, for all tested specimen, the location of the initial damage within the transition region. For each experimental tested specimen cracks are found between ply 2 and ply 3, which are shown in Figures 17 and 18. This result is in conflict with existing findings, in which initial damage preferentially occurs between plies of different orientation. In contrast, the specimen with half-wave imperfection shows initial damage inside a single ply and between plies of the same orientation. An explanation for this behavior are the additional bending stresses resulting from geometry of the half-wave imperfection and intralaminar shear stresses which are present in a woven fabric. Bending stresses and
intralaminar shear stresses are also present in the center of the half-wave imperfection, but the curvature of the transition region is significantly larger. Thus, this region is more critical. The curves of the stresses $S_{ZZ}$ and $S_{XZ}$ in Figures 14 and 15 also show that the transition region is a critical region for initial damage.

Comparing the experimental results in Figures 17 and 18 with the results of the submodels of the transition region in Figures 14 and 15, it is obvious that the highest normal stresses $S_{ZZ}$ are not solely responsible for onset of damage caused by the free-edge effect. Consequently, the additionally acting shear stresses $S_{XZ}$ must have significant influence.

**Normal through-thickness stresses**

With regard to the normal stresses in thickness direction $S_{ZZ}$, it is revealed that negative or negligible small positive stresses are present in ply 1 and ply 4 for both variants of all submodels. Using the reference submodel as a basis for comparison, it is observed that as a result of bending, higher stresses for the submodels of the center and the transition radius are present. This is shown in Figures 10, 12, and 14. In the center of the imperfection the maximum stress is shifted to ply 2. As a result of bending, a reduction of $S_{ZZ}$ in ply 3 is found. This phenomenon appears in all defined paths of both variants. The results of the submodels of the transition radius reveals the effect of bending again. Here, bending in the other direction occurs in the transition radius, reversing that phenomenon. This means that the stresses in ply 2 are reduced, and increased in ply 3. For the shifted variant, the fraction of tows at the free surface is increased, so that there are slight differences to the standard variant. These differences are primarily driven by the stiffness discontinuities between the adjacent tows of the individual plies and the matrix. The reference submodel, as well as the submodel for the center of the half-wave imperfection and the transition radius indicate that the stresses for the shifted variants are higher than those of the associated standard submodels. By comparing the submodels of the transition radius and of the center of the half-wave imperfection, it is revealed that the damage initiation occurs preferentially in the transition radius. Therefore, from the paths of $S_{ZZ}$, the transition radius could be seen as the more critical region.

**Interlaminar and intralaminar shear stresses**

For a complete consideration regarding the location of the onset of the free-edge effect, the interlaminar and interlaminar shear stresses $S_{XZ}$ are taken into account. Both variants of all submodels are subjected to shear stresses $S_{XZ}$ within ply 1 and ply 4, as shown in Figures 11, 13, and 15. Again the reference submodel is used as a basis for comparison. The shifted variants must deal with additional shear stresses in the second and third plies. This is revealed by the paths of the shear stresses along the middle of each shifted submodel. The related graphs are shown in Figures 11, 13, and 15. Apart from this, the shear stresses of both variants are similar. Analogous to the stresses in thickness direction $S_{ZZ}$, the shear stresses are also affected by the bending behavior. Thus the shear stresses in the center of the half-wave imperfection within ply 1 have a higher magnitude compared to those in ply 4. For the submodel of the transition radius, the opposite phenomenon is present. In fact, the shear stresses are lower in ply 1 and higher in ply 4. An important difference is found in the submodels of the transition radius. The maximum shear stresses occur within ply 3. In addition, interlaminar shear stresses are also present between ply 2 and ply 3. The superposition of normal through-thickness stresses and out-of-plane shear stresses in the transition radius is expected to be the reason for damage initiation inside ply 3 and between the plies of same orientation.

**Conclusions**

In this study the influence of a geometrical half-wave imperfection on the free-edge effect was investigated for plain-woven carbon fiber reinforced plastics under cyclic tension-tension loading. For this purpose FE submodels were created, which were composed of matrix and fiber tows. In order to assign the correct boundary conditions to the submodels, global FE
models of a straight reference specimen and a specimen with half-wave imperfection were created. The global FE model of the specimen with half-wave imperfection was validated by experimental tests, so that permissible boundary conditions were ensured. Since the DIC was not able to be focused on the free edge as a result of the small thickness of the laminate, an indirect validation had to be performed by means of surface strains on the frontal side. The two defined submodels with the two layup variants were placed at selected locations, whereby an intermediate step was performed in advance to shape the submodels into the desired form. This intermediate step was crucial for the two variants of the submodels placed in the curved transition area and in the middle of the half-wave imperfection. The third submodel was generated for a straight reference specimen, where again both variants were taken into account.

Defined analysis paths in the submodels revealed on the one hand the differences between the two variants, and on the other hand the distributions of the relevant stress components that caused the free-edge effect. Due to its geometry the specimen with half-wave imperfection was additionally stressed in bending during tensile loading, which was also reflected in the out-of-plane normal stresses $S_{ZZ}$ and shear stresses $S_{XZ}$. For all submodels no positive normal stresses $S_{ZZ}$ in the outermost plies were present. Thus, this stress component was only effective in ply 2 and ply 3. The shear stresses $S_{XZ}$ primarily occurred within the outermost plies. However, there were also interlaminar shear stresses between ply 2 and ply 3. The transition region was identified as the critical region in which the free-edge effect took place. This was confirmed with the experimental findings. It was found that the locations of initial damage were inside ply 3 and between ply 2 and ply 3. The numerical calculations showed that initial damage occurred at the location where high normal stresses $S_{ZZ}$ and additional shear stresses $S_{XZ}$ were present, i.e. inside ply 3 and between ply 2 and ply 3. Thus initial damage occurred between plies of the same orientation and inside a single ply.

Since the location and occurrence of initial damage is now understood by experimental tests and numerical calculations, the next step is to develop a criterion, with which damage initiation and furthermore damage progression could be described. In addition, the influence on the lifetime, affected by the half-wave imperfection, will be investigated. Also the influence of different aspect ratios and ply orientations will be part of future investigations.

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