BENDING DEFORMATION LIMITS FOR CORRUGATED MORPHING SKINS

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Abstract

Corrugated composites are widely considered as candidates for morphing applications. This presentation extends the understanding of corrugated composites with respect to deformation limits for static bending deformations. For this reason, the curvature at rupture due to layer delamination and fiber fracture of corrugated samples is predicted and compared to appropriate experimental results. Four different sample types are manufactured from unidirectional carbon-fiber/epoxy laminate. Bending experiments are conducted using a specially designed device which subjects the samples to an almost pure state of bending.

1 Introduction

Morphing applications gain increasing interest in the last decades. A review of morphing activities on aircraft is recently given by Barbarino et al. [1]. Generally, morphing designs become significantly different depending on the prevailing morphing deformation, namely in-plane or bending. Additionally, such devices can be subdivided into their number of morphing degrees. Most applications aim to increase an airplane’s envelope and efficiency by adjusting one in-plane degree of freedom, e.g. the wing sweep or area. Finally, it must be distinguished between discrete and continuous morphing. The latter requires special attention to the skin structure as continuous morphing skins must meet two counter-acting requirements: Compliant and a large deformation limit in morphing direction while, at the same time, stiff enough to bear aerodynamic loads. An actual state of morphing skins is summarized by Thill et al. [2]. Most suggested skin solutions try to meet those challenges by imposing an extreme anisotropy, e.g. cellular honeycombs for in-plane morphing [3].

Corrugated skins also show this anisotropy and are regarded as particularly suitable for uniaxial in-plane and bending morphing as the corrugated structure increases the deformation until rupture for both deformation modes. Simultaneously, an extreme bending stiffness transverse to the morphing direction is provided. An open question in most suggested morphing skins is how to maintain a smooth aerodynamic surface. One-sided rubber filling is one attempt made by Yokozeki et al. [4] to overcome this deficiency for corrugated panels.

There are several investigations available in the literature dealing with the estimation of representative stiffness properties for laminated corrugated panels, e.g. [4-6]. In contrast, deformation limits and failure mechanisms have rarely been investigated, yet. However, Winkler and Kress [7] theoretically estimated deformation limits accounting for in-plane strains.

This paper focuses on bending deformation limits of circular corrugated panels. A possible application for such skin is e.g. a contour-variable, highly deformable droop nose as currently presented by Burnazzi and Radespiel [8]. The following goal is to validate an introduced analytical method to predict morphing deformation limits. For this reason, a bending fixture is constructed which imposes an almost constant curvature along a sample (up to 35 1/m). Four different geometries of unidirectional carbon/epoxy corrugated samples are manufactured and the curvatures at rupture are determined. These results are compared to analytically estimated values. For the first time, delamination failure is included in the prediction of morphing limits of circular corrugated panels.

1.1 State of uniaxial bending morphing skins

Before dealing with complicated structures the state of morphing using standard flat skins and mate-
rials is examined to provide a kind of reference solution. The performance criteria for pure bending deformations in one particular direction are the ultimate curvature and bending stiffness. Thus, a promising material characteristic is a high ratio of strength to modulus in morphing direction. Hence, candidates’ material data, namely two aluminum alloys (Al 2024 T3 and Al 7075 T6), the titanium alloy Ti-6Al-4V and Hexcel’s unidirectional glass-fiber/epoxy HexPly 913 (UD-GRP), are given in Tab. 1. Herein, subscripts \( c \) and \( s \) indicate chord (morphing direction) and span values, respectively, and superscripts \( + \) and \( - \) tension and compression values. The rolling-direction of the metal sheets is chosen to give a maximum ultimate curvature and the UD-GRP fibers are oriented in morphing (chord) direction. A Kirchhoff plate with plane-stress state is assumed and the curvature in chord direction to be the unique deformation. Then, the stresses read

\[
\sigma_c = \frac{E_c}{1-\nu_{cs}^2} \kappa_c z, \tag{1}
\]

\[
\sigma_s = \frac{E_s}{1-\nu_{cs}^2} \kappa_s z, \tag{2}
\]

\[
\tau_{cs} = 0, \tag{3}
\]

with the moduli \( E_c/s \), large and small poisson ratios \( \nu_{cs} \) and \( \nu_{cs} \) and the plate’s thickness coordinate \( z \). For the metals, the maximum von-Mises equivalent stress is calculated for the plate’s surfaces and compared to the yield stresses in chord-direction \( R_c^+ \) and \( R_c^- \). The glass-fiber composite is analyzed using the well-known criterion of Puck. The bending stiffness per width is calculated via

\[
D_{c/s} = \frac{t^3}{12} \frac{E_{c/s}}{1-\nu_{cs}^2} \kappa_c z \tag{4}
\]

with \( t \) being the plate-thickness.

Although tension moduli and tension/compression yield stresses (metals) and strength (UD-GRP) are used instead bending values, Fig. 1 gives a good overview on morphing possibilities and characteristics of flat skins. Obviously, the curvature limits are very sensitive within the range of low bending stiffness (Fig. 1(a)), and large ultimate curvatures can only be achieved for low skin thickness (Fig. 1(b)).

Summarizing, UD-GRP performs best especially regarding skin thickness against ultimate curvature which is a key criterion considering impact problems. Within the European project SADE [9] a contour variable droop-nose has been constructed from HexPly 913. There, the skin thickness decreases to \( 1 \) mm at the maximum morphing curvature of approximately \( 20 \) \( 1/m \) (safety-factor \( \approx1.8 \)). This can

<table>
<thead>
<tr>
<th>Material</th>
<th>( E_c^+ ) (MPa)</th>
<th>( E_c^- ) (MPa)</th>
<th>( \nu_{cs} )</th>
<th>( R_c^+ ) (MPa)</th>
<th>( R_c^- ) (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Al 2024 T3</td>
<td>73,700</td>
<td>-</td>
<td>0.330</td>
<td>300</td>
<td>320</td>
</tr>
<tr>
<td>Al 7075 T6</td>
<td>71,000</td>
<td>-</td>
<td>0.330</td>
<td>505</td>
<td>495</td>
</tr>
<tr>
<td>Ti-6Al-4V</td>
<td>110,300</td>
<td>-</td>
<td>0.310</td>
<td>1,000</td>
<td>1,115</td>
</tr>
<tr>
<td>UD-GRP</td>
<td>45,300</td>
<td>15,900</td>
<td>0.295</td>
<td>1,190</td>
<td>1,010</td>
</tr>
</tbody>
</table>

Tab. 1. Material data of state of the art aerospace materials. The first three are statistical B-values from Niu [10] and the unidirectional glass-fiber/epoxy data (UD-GRP) are measured mean values (number of samples is 5).

Fig. 1. Accessible ultimate morphing curvature against bending stiffness for a flat skin made of standard materials (a) and the resulting skin thickness (b).
be regarded as morphing limit for conventional materials and designs.

2 Materials and methods

The following describes the constructed large bending setup, the base-material, the manufacturing process and the geometry of the tested corrugated samples.

2.1 Large bending device

Continuous morphing implies deformation driven geometrical changes of a contour. Thus, a bending device is designed which fully prescribes the deformation. The state of bending is kept as pure as possible. As there does not exist any kinematic setup which directly imposes pure bending, various setups are realized by different authors [11]-[13]. These devices have in common that the bending angle at clamped ends is prescribed and the relative motion in between is left unconstrained. Constructions of such type are limited to static experiments. In contrast, the introduced bending device slightly departs from a pure state of bending but instead is close to morphing reality and also suits for cyclic investigations.

A sketch of the bending device is given in Fig. 2. The bending moment is measured directly at one clamp. The bending angle $\theta$ is prescribed and the error in the $x$-distance between the tangentially clamped ends compared to a pure bending state is minimized. In case of pure bending, the curvature reads

$$\kappa = \frac{1}{R} = \frac{2\theta}{L}$$

with radius $R$, sample length $L$ and the corresponding $x$-distance

$$\Delta x_{\text{pure}} = 2R \sin \theta.$$  \hfill (6)

The device $x$-distance at the sample’s elastic axis is

![Fig. 2. Sketch of the constructed large bending setup.](image)

![Fig. 3. Deformation of pure and device bending (a) and relative positioning error (b).](image)
\[ \Delta x_{\text{device}} = 2(a + b \cos \theta). \]  

(7)

In order to control the x-distance error

\[ \Delta x_{\text{error}} = (\Delta x_{\text{device}} - \Delta x_{\text{pure}}) / \Delta x_{\text{pure}}, \]

(8)

the length \( b = \bar{b} + \Delta b \) is adjustable via \( \Delta b \). With the fixed lengths \( a = 20 \text{ mm} \) and \( \bar{b} = 10 \text{ mm} \), the x-position is plotted for \( \Delta b = 0.6 \text{ mm} \) over the curvature, see Fig. 3(a). The relative error defined by Eq. (8) is studied for different \( \Delta b \) values and plotted in Fig. 3(b). This implies that the error can be minimized within the interesting curvature region. All experiments in this presentation are conducted with \( \Delta b = 0.6 \text{ mm} \) resulting in an x-position error < 0.2 % for all accessible curvatures. In addition to the mechanical data, sound emission is recorded directly at the sample within a silent environment during testing. All corrugated specimens are clamped with their geometric center regarded as elastic axis, cp. Fig. 4. Quasi-static experiments are conducted at \( \dot{\theta} = 13.4 \text{ deg/min} \) deformation speed.

### 2.2 Corrugated samples

Four different corrugated geometries are manufactured and visualized in Fig. 4. The geometry is assembled out of circular segments and is fully describable by the tangent-angle and half-period. Samples are fabricated from four layers of unidirectional carbon/epoxy prepreg cured at a temperature of 125 °C and 6 bar pressure to 1 mm nominal laminate thickness. Fibers are aligned with the corrugation direction. To obtain high quality samples, CNC-milled molds with counter-plates are used, cp. Fig. 5. This process gives considerable better results regarding thickness variations and symmetry compared to direct vacuum bagging on the sample surface instead of using counter-plates.

Flat plates of the base-material are similarly manufactured and characterized, see Tab. 2. Herein, subscripts \( \parallel \) and \( \perp \) indicate fiber parallel and transverse values, respectively. As the samples are loaded in bending, the fiber parallel initial bending modulus \( E_{\parallel}^{\text{bend}} \) is recorded. All other properties are tension/compression values. Additionally, as bending, tension and compression experiments require a different sample thickness, the mean corresponding layer thickness \( t_{\text{layer}} \) is given in Tab. 2.

![Fig. 5. Mold with un-cured sample and counter-plate.](image)

<table>
<thead>
<tr>
<th>Property</th>
<th>UD-CRP data</th>
<th>( t_{\text{layer}} ) (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>( E_{\parallel}^{\text{bend}} ) (MPa)</td>
<td>89,819</td>
<td>0.272</td>
</tr>
<tr>
<td>( E_{\perp}^{\text{bend}} ) (MPa)</td>
<td>9,309</td>
<td>0.266</td>
</tr>
<tr>
<td>( E_{\parallel} ) (MPa)</td>
<td>8,491</td>
<td>0.265</td>
</tr>
<tr>
<td>( \nu_{\parallel} ) (-)</td>
<td>0.304</td>
<td>0.266</td>
</tr>
<tr>
<td>( R_{\parallel}^{\text{bend}} ) (MPa)</td>
<td>2,176</td>
<td>0.266</td>
</tr>
<tr>
<td>( R_{\perp}^{\text{bend}} ) (MPa)</td>
<td>1,008</td>
<td>0.265</td>
</tr>
<tr>
<td>( R_{\parallel} ) (MPa)</td>
<td>42.7</td>
<td>0.266</td>
</tr>
<tr>
<td>( R_{\perp} ) (MPa)</td>
<td>180.9</td>
<td>0.265</td>
</tr>
</tbody>
</table>

Tab. 2. Material data of the carbon/epoxy base-laminate (number of tested samples is 5).

Finally, the precise laminate thickness of the cured samples is measured via micrographs. All samples show resin accumulations around the top and bottom of the circular segments. As accurate molds are used, this effect is probably due to thermal expansions in combination with the wavy geometry. The thickness is measured at 70 equidistant points along the samples. Figure 6 shows an exemplary micrograph indicating two thickness measures with and without resin zones (\( t_{\text{total}} \) and \( t_{\text{fiber}} \)). It turns
out, that the fraction \((t_{\text{total}} - t_{\text{fiber}})/t_{\text{fiber}}\) of the resin accumulations ranges between 2.2 and 5.8\%.

Both thickness values, the half-period \(c\) and the tangent-angle \(\phi\) are summarized in Tab. 3. The sample width varies around 23 mm.

![Micrograph of a CC 2 sample.](image6)

**Tab. 3. Geometry data of the manufactured samples.**

<table>
<thead>
<tr>
<th>Sample</th>
<th>(c) (mm)</th>
<th>(\phi) (deg)</th>
<th>(t_{\text{total}}) (mm)</th>
<th>(t_{\text{fiber}}) (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>CC 1</td>
<td>12.0</td>
<td>28.1</td>
<td>1.088</td>
<td>1.053</td>
</tr>
<tr>
<td>CC 2</td>
<td>10.0</td>
<td>77.3</td>
<td>1.065</td>
<td>1.042</td>
</tr>
<tr>
<td>CC 3</td>
<td>7.5</td>
<td>56.1</td>
<td>1.133</td>
<td>1.076</td>
</tr>
<tr>
<td>CC 4</td>
<td>15.0</td>
<td>56.1</td>
<td>1.126</td>
<td>1.064</td>
</tr>
</tbody>
</table>

Fig. 6. Micrograph of a CC 2 sample.

![Sketch of the corrugated unit-cell (a) and circular beam loaded by end-moments (b).](image7)

Obviously, the mean layer thicknesses slightly differ from each other (Tab. 3) and also from the base-laminate plates (Tab. 2). These deviations are assumed to only considerably affect the fiber parallel modulus and strength properties. As the carbon-fiber modulus is much higher than the resin modulus and the layer thickness correlates with the fiber volume content for prepreg laminates, the fiber parallel properties \(E_{\parallel}\) are adjusted separately for each corrugated geometry via

\[
E_{\parallel}^{\text{corrugated}} = E_{\parallel}^{\text{base-material}} \frac{t_{\text{layer}}}{t_{\text{fiber}}/n}, \tag{9}
\]

where \(n = 4\) is due to the four prepreg layers all samples consist of.

## 3 Bending deformation limits

The scope of this section is to predict deformation limits for corrugated panels under bending, thus, the ultimate curvature for pure bending loads. When approaching a morphing task, it is worthwhile to take multiple configurations into account. Hence, a linear analytical method is presented to rapidly estimate ultimate curvatures. For the first time, delamination failure is accounted for beside fiber fracture. Circular corrugated panels behave geometrically quiet linear [7] which promises satisfying results at least during preliminary design calculations.

When loading an originally curved beam, through thickness stresses occur. As these stresses may lead to premature delamination failure, corrugated morphing skins should avoid approaching this region. Delamination damage has already been reported by Dayyani *et al.* [14] at edges of trapezoidal corrugated panels. Martin [15] and Roos *et al.* [16] investigate delamination problems in singly curved composite laminates.

In order to deal simultaneously with through thickness and tangential bending stresses, the complete stress field of a corrugated cross-section is analyzed. Lekhnitskii *et al.* [17] give an analytical solution based on the existence of a stress function for circular segments loaded by equal end-moments which represents pure bending. The analysis is conducted for a representative unit-cell (Fig. 7(a)), while the stress field is analyzed separately for both circular segments using opposed end-moments.

A cylindrical coordinate system \((r, \phi, z)\) is introduced (Fig. 7(b)) and cylindrical anisotropy is as-
sumed. Under these conditions and in the absence of body forces, the stresses within the modeled cross-section are calculated using a differentiable stress function $F(r, \varphi)$ via

$$\sigma_r = \frac{1}{r} \frac{\partial F}{\partial r} + \frac{1}{r^2} \frac{\partial^2 F}{\partial \varphi^2},$$

(10)

$$\sigma_\varphi = \frac{\partial^2 F}{\partial \varphi^2},$$

(11)

$$\tau_{r\varphi} = 0.$$  

(12)

Lekhnitskii’s solution of the above problem is based on a stress function of type

$$F(r) = c_1 + c_2 r^2 + c_3 r^{1+k} + c_4 r^{1-k}$$

(13)

with constants $c_i$. The parameter

$$k = \sqrt{E_\varphi / E_r}$$

(14)

accounts for a transverse anisotropic material characteristic. Herein, $E_\varphi$ and $E_r$ denote the accordant elastic moduli. Defining $c$ as the ratio between the inner and outer radius $c = r_1 / r_0$ and $m = M / b$ as the bending moment $M$ per width $b$, the stresses in a circular segment denote

$$\sigma_r = \frac{m}{r_0 \alpha} \left[ 1 - \frac{1-c^{k+1}}{1-c^{2k}} \left( \frac{r}{r_0} \right)^{k-1} - \frac{1-c^{k-1}}{1-c^{2k}} c^{k+1} \left( \frac{r}{r_0} \right)^{k+1} \right].$$

(15)

$$\sigma_\varphi = \frac{m}{r_0 \alpha} \left[ 1 - \frac{1-c^{k+1}}{1-c^{2k}} c^{k} \left( \frac{r}{r_0} \right)^{-1} + \frac{1-c^{k-1}}{1-c^{2k}} c^{k+1} \left( \frac{r}{r_0} \right)^{-k+1} \right].$$

(16)

$$\tau_{r\varphi} = 0,$$

(17)

with $g$ defined as

$$g = \frac{1-c^2}{2} - \frac{k}{k+1} \left( \frac{1-c^{k+1}}{1-c^{2k}} \right)^2 + \frac{k c^2 (1-c^{k-1})^2}{k-1}.$$  

(18)

Note that both normal stresses do not depend on polar angle $\varphi$. The assumption regarding the stress/strain state in width direction is toggled by the modulus $E_\varphi$. In case of an unconstrained in-plane modulus, a plane stress state is prevailing, whereas a poisson-retarded modulus leads to a plane strain state in the corrugated cross-section. Hence, in the current investigation the bending modulus $E_\text{bend}$ is used. This is especially preferable for similar sample width of base-laminate bending and corrugated specimens. The circumferential stress $\sigma_\varphi$ neither follows a linear nor a hyperbolic law and reaches maximum values at $r = r_1$ and $r = r_0$. Furthermore, it declines close to zero around $r = r_m$. In contrast, the radial through thickness stress $\sigma_r$ behaves vice versa. The exact location of the maximum radial stress $r = r_{\text{max}}$ is estimated by solving the relation

$$\frac{\partial \sigma_r}{\partial r} = 0$$

(19)

for $r$. Figure 8 exemplarily shows these stress distributions for a CC 2 sample loaded by a 100 N moment flux.

![Stress distribution](image)

**Fig. 8.** Stress distribution in a corrugated CC 2 sample loaded by a moment flux of 100 N.

The radial stress is regarded to cause delamination failure which is thus most probable around the laminate center in thickness direction. Similarly, fiber breakage is initiated at the laminate surfaces by the circumferential stress. As both stresses are fairly decoupled, a maximum stress failure criterion which neglects any stress interactions is well applicable. Hence, the safety-factors against fiber breakage at inner and outer radius denote

$$S_{f_{\text{fb}}} = \begin{cases} R_{f_{\text{fb}}} / \sigma_{\varphi,0} & \text{for } \sigma_{\varphi,0} \geq 0, \\ R_{f_{\text{fb}}} / \sigma_{\varphi,0} & \text{for } \sigma_{\varphi,0} < 0. \end{cases}$$

(20)

Similarly, the safety factor against delamination at the maximum radial stress location is defined as

$$S_{d_{\text{del}}} = \begin{cases} R_{d_{\text{del}}} / \sigma_{r,0} & \text{for } \sigma_{r,0} \geq 0, \\ R_{d_{\text{del}}} / \sigma_{r,0} & \text{for } \sigma_{r,0} < 0. \end{cases}$$

(21)

As the stresses linearly depend on the moment flux, the three safety-factors are determined for the two unit moment fluxes $m_{\text{unit}} = \pm 1$ to account for the two opposed circular segments of the unit-cell. The moment fluxes at rupture are then calculated using the safety-factors $S_{f_{\text{fb}}}$ via

$$m_{\text{rupture}} = S_{f_{\text{fb}}} m_{\text{unit}}$$

(22)

and the corresponding curvatures at rupture via

$$\kappa_{\text{rupture}} = K_{44} m_{\text{rupture}}.$$  

(23)
Herein, $K_{44}$ denotes the compliance in corrugated direction and can be calculated from publications of e.g. Xia et al. [5] or Kress and Winkler [6]. Finally, the experimentally recorded ultimate curvature is compared to the first-failure curvature associated with fiber fracture $k_{\text{rupture}}^f$ and delamination $k_{\text{rupture}}^d$, respectively.

### 4 Results

At this point the 2D linear analytical prediction of the curvatures at rupture and the corresponding fracture mode (delamination or fiber breakage) is compared to experimental results.

Before presenting the outcome, the definition of curvature at rupture is discussed. All corrugated samples show a non-linear moment flux over curvature relation. Hence, two curvatures at rupture are introduced, namely, the non-linear, directly recorded $k_{\text{rupture}}$ and its linearized $k_{\text{rupture}}^\text{linear}$, cp. Fig. 9 (top). In both cases, final fracture occurs at the equivalent moment flux $m_{\text{rupture}}$, which is the maximum recorded load.

Fortunately, the identification of the failure mode has always been possible. All corrugated CC 1 and CC 4 samples primarily fail due to fiber fracture (Fig. 11(a)) while stepwise growing delamination appears at CC 2 and CC 3 type samples. Figure 11(b) shows a completely delaminated CC 2 sample at final fracture. Sound emission is only detectable during tests of delaminating samples. These emissions occur while leaving a linear moment flux/curvature behavior, cp. Fig. 9. Hence, local premature damage is only associated with CC 2 and CC 3 samples. This implies that any non-linearity is due to geometric non-linearity for CC 1 and CC 4.

The comparisons between the (linearized) experimental and the predicted delamination and fiber fracture curvatures are given in Fig. 10. Noticeably, all experimental deformation limits exhibit a low standard deviation. For all corrugation types the theoretical deformation limit due to delamination and fiber fracture is calculated. Types CC 1 and CC 4 indicate a higher ultimate curvature with respect to delamination than to fiber fracture which is vice versa for CC 2 and CC 3. Thus, the failure mode of all four sample types agrees with the experiment.

![Fig. 9. Experimentally recorded and linearized curvature at rupture (top) and corresponding sound amplitude (bottom) for a CC 3 specimen.](image-url)
It is well known that bending strength properties differ from in-plane values due to different stress distributions. In detail, the strength for bending loads show higher values as the maximum stress peak only covers a small fraction of the loaded cross-section. Similarly, the through thickness stresses within a corrugated cross-section reach a specific maximum, see Fig. 8. Thus, in both cases the impact of local effects is reduced compared to in-plane loads. Consequently, all theoretical predictions are conducted using 15% elevated strength values

\[ R_{\text{bend}} = f \cdot R_{\text{in-plane}} \]  

meaning \( f = 1.15 \) and \( R_{\text{in-plane}} \) denoting all strength values given in Tab. 2.

Obviously, the theoretical predictions meet the linearized experimental deformation limits well for the fiber fracture corrugation types. Notably, the CC1 type behaves considerably more non-linear (which is due to the flat corrugation waves) compared to the CC4. Hence, the use of a linearized curvature at rupture with the corresponding moment flux being the crucial property is justified. The delaminating sample behavior (especially CC3) is predicted less accurate. Possible reasons denote an inherently higher deviation of the essential transverse strength and the growing local damages resulting in a non-linear material behavior. The largest discrepancy (CC3 type) may be explained by the smallest corrugation radius, cp. Fig. 5. Delamination failure firstly occurs at the segments subjected to through thickness tension stresses and is thus bounded by adjacent segments. Hence, the maximum local damage due to radial tension stresses is smaller at

![Fig. 10. Calculated curvature at rupture regarding delamination and fiber fracture compared with the experimentally observed deformation limits. Error bars indicate the standard deviation (number of tested samples is 4).](image)

![Fig. 11. Observed fracture modes: Fiber fracture of a CC1 (a) and complete delamination of a CC2 specimen (b).](image)
the CC 3 than at the CC 2 type. For this reason, the critical damage size may already be reached within the tension zone at the well predicted CC 2 type. In contrast, the CC 3 samples still bear load after first delamination. Consequently, the linearized (with respect to the maximum moment flux) ultimate curvature is underestimated by the linear theory. However, earlier delamination events are detected considering the sound emission, cp. Fig. 9 (bottom).

5 Conclusions

After presenting bending morphing possibilities using a standard skin design and materials, bending limits of corrugated unidirectional carbon/epoxy composites are investigated. Pure bending experiments with four different circular corrugation types are conducted and the curvatures at rupture are extracted. Accordingly, an analytical prediction method accounting for fiber fracture and layer delamination is presented. The capability of this method is demonstrated in terms of the prediction of the ultimate curvatures and, for the first time, the failure mode, too. Hence, circular corrugated morphing skins can easily be evaluated with respect to their bending deformation limits. This is especially useful during a preliminary design phase.

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References