Crush simulation of carbon/epoxy NCF composites - Development of a validation test for material models

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Abstract

The high specific stiffness and strength of composites makes it advantageous for load carrying structures in the automotive industry. By successfully be able to numerically simulate the crush behaviour of composites, structure with high specific energy absorption can be implemented in the automotive industry. The purpose of this thesis is to verify the predictive capabilities of a crush model developed at SICOMP.

Initially currently available material models are investigated. Puck’s criterion is deeper studied. An improvement of the criterion is suggested and the model is updated to be able to output fracture angles in Abaqus.

The material model developed by SICOMP is a three-dimensional physically based damage model where failure initiation is estimated with proven failure criteria and damage growth is combined with friction to account for the right energy absorption.

The crush damage model has been implemented in Abaqus/Explicit as a VUMAT subroutine. Numerical predictions are compared with experimental results. Specimens with different fibre layups and crash triggers are tested.

Keywords: Damage mechanics, NCF, Automotive

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Sammanfattning

Den höga styvheten och hållfastheten hos kompositmaterial gör den gynnsam för lastbärande strukturer i bilindustrin. Genom att behär ska numeriskt simulera krossbeteendet hos kompositer kan en effektiv kompositstruktur med hög energiabsorption implementeras i bilindustrin. Syftet med examensarbetet är att undersöka den prediktiva kapaciteten av en materialmodell utvecklad av SICOMP.

Inledningsvis undersöktes befintliga materialmodeller, en fördjupad studie av Pucks kriterium genomförs. En förbättring av kriteriet föreslås och modellen uppdaterades så att brottvinklar kan visualiseras i Abaqus.

Materialmodellen som utvecklats av SICOMP är en tredimensionell fysikalisk modell där brott beräknas med beprövade brottkriterier och skadetillväxt kombineras med friktion för att beräkna ett korrekt energiupptag.

Materialmodellen implementeras i Abaqus/Explicit som en VUMAT subrutin. Simuleringarna jämfördes med experimentella data. Testföremål med olika fiberupplägg och kraschutlösare prövades.
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Notation

**Lower case Roman letters**

\( d_1 \) ........................................................................................................ damage variable

\( f_i \) ........................................................................................................ failure index, \( i = mc, mt, fc, ft, kink \)

\( l_c \) ........................................................................................................ characteristic element length

\( m \) ........................................................................................................ denoting misaligned fibre frame for kinking

**Upper case Roman letters**

\( G_{ic} \) .................................................................................................. mode \( i \) component of the critical energy release rate, \( i = 1, 2, 6 \)

\( S_L \) ........................................................................................................ longitudinal shear strength

\( S_T \) ........................................................................................................ transverse shear strength

\( X \) .......................................................................................................... direct strength in the longitudinal direction

\( X_c \) ........................................................................................................ compressive strength in the longitudinal direction

\( X_t \) ........................................................................................................ tensile strength in the longitudinal direction

\( Y_c \) ........................................................................................................ compressive strength in the transverse direction

\( Y_t \) ........................................................................................................ tensile strength in the transverse direction

**Lower case Greek letters**

\( \alpha \) ...................................................................................................... angle of the fracture plane for the matrix failure mode

\( \alpha_o \) .................................................................................................. angle of the fracture plane for the matrix failure mode for pure transverse compression

\( \gamma \) ...................................................................................................... shear strain

\( \gamma_0 \) .................................................................................................. shear strain at initial failure

\( \gamma_f \) .................................................................................................. shear strain at final failure

\( \varepsilon \) .................................................................................................... strain

\( \varepsilon_f \) ................................................................................................... strain at final failure

\( \varepsilon_0 \) ................................................................................................... strain at initial failure

\( \theta \) ........................................................................................................ misalignment angle for fibre kinking
\( \psi \) .......................................................... angle from the 2-axis to the misalignment plane for kinking in 3D
\( \mu_T \) ............................................................. friction coefficient for the transverse direction
\( \mu_L \) ............................................................. friction coefficient for the longitudinal direction
\( \mu \) ............................................................. friction coefficient between specimen and plates
\( \nu \) ............................................................. Poisson’s ratio
\( \sigma \) ............................................................. stress
\( \sigma^a \) ............................................................. applied stress
\( \sigma^{ef} \) ............................................................. effective stress
\( \sigma_{ij} \) ........................................................ stress components, \( ij = 12, 2^m3^\nu, 1^m2^m \)
\( \sigma_n \) ........................................................ normal component of the traction acting on a surface or plane
\( \tau \) ............................................................. shear stress
\( \tau_{ij} \) ........................................................ shear stress components, \( ij = 12, 2^m3^\nu, 1^m2^m \)
\( \tau_L \) ........................................................ longitudinal shear component of the traction vector in a potential matrix fracture plane
\( \tau_L^{fric} \) .................................................. friction stress associated with longitudinal direction
\( \tau_T \) ........................................................ transverse shear component of the traction vector in a potential matrix fracture plane
\( \phi \) ........................................................ angle of the matrix fracture plane in the kinking model
1. Introduction

The regulation authorities require the automotive industry to reduce the pollutants from combustion engines. To maintain today’s comfort and performance as well as meeting the regulations, the weight of the structure has to be reduced. Electric cars can also benefit from lighter structure by increasing the distance travelled with one battery charge. By reducing the weight of the structure, fewer batteries or smaller engines can be used to achieve the same performance, which reduces the weight even more.

The parts made of composites in automobiles today are often limited to non- or semi-load carrying structures, Park et al. (2012). In order to reduce the weight of automobiles composites could be used in load carrying structure. Composites have a higher strength to weight ratio than steel, which are the most commonly used materials in car structures today. In the event of a crash the car structure should absorb as much energy as possible, and still have sufficient survival space for the passengers in the car. Moreover the structure has to be stiff in bending and torsion to provide good performance in handling and manoeuvre. Composite materials for load-carrying structure have been well used in the aerospace industry, sports equipment, and racing applications. The price requirements and the cycle time in the automotive industry entails that the same methods cannot be used. To use composite structures in future cars, the crash behaviour has to be successfully numerically modelled. For this purpose a new material model for crash of composites is developed at Swerea SICOMP.

1.1. Purpose

The purpose of the thesis is to design a robust setup in Abaqus to be able to validate the material model developed by SICOMP. A parametric study is conducted to investigate which parameters influence the response, and to decide the values on those parameters. Finally the crush model is validated.

1.2. Method

The project started with a literature survey, which was divided in two parts. One part on failure mechanisms of composites, the second part was introduction in Abaqus. A Python script was developed to simplify the parametric study. In order to get accustomed to Abaqus, a Puck’s failure criterion for matrix was validated and improved. Later on the material model developed at Swerea SICOMP was implemented in Abaqus/Explicit as a VUMAT subroutine.

Two different specimens were used to validate Puck’s matrix failure criterion and SICOMP’s material model, a single element cube Fig. 1(a) and a flat specimen, Fig. 1(b).
The specimens had all fibres in one direction, and both pure longitudinal, and transverse stresses were applied. The flat specimen was modelled with three different crash triggers.

2. Failure of composites

Unidirectional fabric (hereafter referred to as UD) is when all the fibres have the same direction. In this section the different failure modes for a UD are described.

The failure behaviour of composites is dependent of the load direction i.e. tensile or compressive, and how the load is applied with respect to the fibre direction i.e. transvers or longitudinal, Fig. 2.

Composite failure occurs as a several sequence of events. Therefore it is important to distinguish between failure initiation and final failure, which often takes place at different time and stress levels.
2.1. Transverse failure modes

Transverse compressive failure for a UD, Fig. 2(a) is dominated by matrix properties. During this failure composites absorb energy by friction and by formation of new cracks. For pure transverse compression on a UD material the highest shear stresses are obtained at 45° from the load. It would be logical to expect the fracture occurs at that angle, but due to friction Puck & Schürmann (2002) found that the fracture angle is higher. For carbon and glass reinforced polymers a generally accepted and used value for the fracture angle is 53°, Pinho et al. (2005)

For a tensile transverse stress on a UD material high stress concentrations are created around the fibres, which can lead to cracks in the interface between fibres and matrix. The fracture plane occurs normal to the applied load, Fig. 2(b).

Matrix failures are divided in three modes, A, B and C, Puck & Schürmann (2002). The modes depend on the value of the stress $\sigma_{22}$ and the fracture angle $\alpha$, Fig. 3.

Experimentally it has been shown that the fracture angle is zero for tensile transverse stresses as well as for small compressive transverse stresses, respectively they are called “Mode A” and “Mode B” Fracture plane with an angle from the load is called “Mode C”.

2.2. Longitudinal failure modes

For longitudinal compressive stress fibre kinking may occur. It is initiated by microstructure defects as local misalignments of the fibres. The defects redistribute the stresses, which misalign the fibres even more. This cycle can finally lead to failure, Fig. 2(c).

In longitudinal tensile failure the fibres are carrying most of the load, since fibres have lower ultimate strain than the matrix the fibres will fail first. When all the fibres have failed the load will redistribute to the matrix. This will lead to catastrophic failure, Fig. 2(d).
2.3. Delamination

In crash of composites delamination is a common failure mechanism. Delamination is most common in the interface between layers with different fibre directions, Perillo et al. (2012). Delamination can be categorised in three modes, the modes are depending on the load, Fig. 4.

![Modes of delamination](image)

Fig. 4. Modes of delamination, (a) mode I opening mode, mode II sliding mode (b) and mode III tearing mode (c).

During crash combinations of all the modes can occur. The fracture toughness for each of the modes are denoted $G_I, G_{IIc}$ and $G_{IIIc}$. The fracture toughness is the area under the stress-displacement curve and can be determined experimentally. Generally $G_{C,II}$ is assumed to be equal to $G_{C,III}$. The reason is that there are no good test methods for mode III and the fracture surfaces are similar.

To be able to simulate delamination Perillo et al. (2012) suggested that cohesive elements should be used between layers with different orientation, since delamination is most likely to occur there.

3. Current failure models

Several material models have been developed; some of them are implemented in FE-software. In this section an LS-DYNA model, as well as Puck’s matrix failure criterion and Pinho’s material model are analysed. Puck’s model for matrix failure has been used for predicting initial failure and fracture angles, Appendix A.

3.1. MAT54

MAT54 is an LS-DYNA model based on a modified Chang-Chang criterion. The failure criterion is based on the single lamina strength, in tensile, compression, and shear. When an element reaches maximum allowed strength it is eliminated. MAT54 has some non-physical or immeasurable parameters. In order to fit simulation to experimental data these parameters have to be tuned by trial and error. One of them is the SOFT parameter. SOFT reduces the strength of the element row behind the crash front, to get a smoother load transition from the active row to the next, and to improve the response a low-pass filter can be used, Wade et al. (2011).
MAT54 is highly dependent of mesh size, Wade et al. (2011). MAT54 is a very simplified model. When a failure is initiated the lamina is totally eliminated and can no longer take any stresses, which is not physically correct. The lack of physical parameters in the model is a major disadvantage.

3.2. Failure initiation modelling

Puck formulated how friction influences failure, and fracture angle of composites. In compression the friction in the newly formed cracks increase the load carrying capacity of the composite. For matrix failure Pinho et al. (2012) suggested the failure criterion given by

\[ f_{mc} = \left( \frac{\tau_T}{S_T - \mu_T \sigma_N} \right)^2 + \left( \frac{\tau_L}{S_L - \mu_L \sigma_N} \right)^2 + \left( \frac{\langle \sigma_N \rangle_+}{Y_T} \right)^2 \]  

where the McCauley brackets, \( \langle \sigma_N \rangle_+ \) should be interpreted as \( \max(\sigma_N,0) \). The friction should only affect the failure response in compression, when the newly formed cracks are compressed. By simulation with the single element cube it was found that Eq.(1) overestimating failure in tensile matrix failure. A more physically correct failure expression where the friction is only included for compression is suggested

\[ f_M = \left( \frac{\tau_T}{S_T - \mu_T \langle \sigma_N \rangle_-} \right)^2 + \left( \frac{\tau_L}{S_L - \mu_L \langle \sigma_N \rangle_-} \right)^2 + \left( \frac{\langle \sigma_N \rangle_+}{Y_T} \right)^2 \]  

the McCauley brackets with “−”index should be interpreted as \( \min(\sigma_N,0) \). The stresses are given by

\[ \sigma_N = \frac{\sigma_{22} + \sigma_{33} + \sigma_{22} - \sigma_{33} \cos(2\alpha) + \tau_{23} \sin(2\alpha)}{2} \]

\[ \tau_T = -\frac{\sigma_{22} - \sigma_{33} \sin(2\alpha) + \tau_{23} \cos(2\alpha)}{2} \]

\[ \tau_L = \tau_{12} \cos(\alpha) + \tau_{31} \sin(\alpha) \]  

where \( \alpha \) is the fracture angle. Stresses are calculated for all possible fracture angles, \( 0^\circ \leq \alpha < 180^\circ \), and the stresses at the angle maximizing Eq.(2) are chosen. The longitudinal and transverse shear stresses are denoted \( S_L \) and \( S_T \) respectively. The longitudinal shear strength has to be experimentally measured, while the transverse can be calculated if no experimental value is available by Eq(4), Pinho et al. (2005)

\[ S_T = \frac{Y_C}{2 \tan \alpha_0} \]  

where \( Y_C \) is the transverse compressive strength. When the material is exposed to a compressive normal stress \( (\sigma_N < 0) \) the shear strengths increases due to friction Pinho et al.
In Eq. (1) \( \mu_T \) and \( \mu_L \) are the friction coefficients, where the index “T” is in transverse fibre direction and “L” in longitudinal direction. The transverse friction coefficient is given by

\[
\mu_T = -\frac{1}{\tan(2\alpha_0)}.
\]

(5)

If no experimentally value for the longitudinal friction coefficient is available Pinho et al. (2005) suggested it can be approximated with

\[
\mu_L = \frac{S_L \cos(2\alpha_0)}{Y_c \cos^2(\alpha_0)}.
\]

(6)

3.2.1. Tensile fibre failure

Only \( \sigma_{11} \) has influence over the failure in the tensile fibre failure mode, Pinho et al. (2006). Therefore the maximum stress failure criterion is used

\[
f_p = \frac{\sigma_{11}}{X_t},
\]

(7)

where \( X_t \) is the tensile strength in the longitudinal direction.

3.2.2. Compressive fibre failure

As mentioned above, fibre kinking is promoted by misalignment of the fibres. The misalignment angle is denoted \( \theta \), Fig. 5(a). For the 3D case the kinking plane is assumed to be located with the angle \( \psi \) from the 2-axis, Fig. 5(b).

For the general 3D case the stresses are first transformed from the material frame to the \( \psi \) frame by
\[
\sigma_{22'} = \frac{\sigma_{22} + \sigma_{33}}{2} + \frac{\sigma_{22} - \sigma_{33}}{2} \cos(2\psi) + \tau_{23} \sin(2\psi)
\]
\[
\sigma_{33'} = \sigma_{22} + \sigma_{33} - \sigma_{22'}
\]
\[
\tau_{23'} = \tau_{12} \cos(\psi) + \tau_{31} \sin(\psi)
\]
\[
\tau_{23} = \frac{-\sigma_{22} - \sigma_{33}}{2} \sin(2\psi) + \tau_{23} \cos(2\psi)
\]
\[
\tau_{31'} = \tau_{31} \cos(\psi) - \tau_{12} \sin(\psi)
\]

The stresses are then rotated to the misaligned fibre frame by
\[
\sigma_{11'} = \frac{\sigma_{11} + \sigma_{22'}}{2} + \frac{\sigma_{11} - \sigma_{22'}}{2} \cos(2\theta) + \tau_{12'} \sin(2\theta)
\]
\[
\sigma_{22'} = \sigma_{11} + \sigma_{22'} - \sigma_{11'}
\]
\[
\tau_{12'} = \frac{-\sigma_{11} - \sigma_{22'}}{2} \sin(2\theta) + \tau_{12'} \cos(2\theta)
\]
\[
\tau_{31'} = \tau_{31} \cos(\theta) - \tau_{12'} \sin(\theta)
\]
\[
\tau_{31'} = \tau_{31'} \cos(\theta)
\]

where \(m\) index denotes the misaligned frame. The failure criterion depends on the direction of the transverse stress, in compressive the friction increase the strength.

\[
f_{\text{fracture}} = \begin{cases} 
\left(\frac{\tau_t}{S' - \mu_l \sigma_N}\right)^2 + \left(\frac{\tau_L}{S' - \mu_l \sigma_N}\right)^2 & \text{for } \sigma_{22'} \leq 0 \\
\left(\frac{\sigma_N}{Y_t}\right)^2 + \left(\frac{\tau_t}{S_l}\right)^2 + \left(\frac{\tau_L}{S_l}\right)^2 & \text{for } \sigma_{22'} > 0 
\end{cases}
\]

The traction in the fracture plane is given by
\[
\sigma_N = \frac{\sigma_{22'} + \sigma_{33}}{2} + \frac{\sigma_{22'} - \sigma_{33}}{2} \cos(2\phi) + \tau_{23'} \sin(2\phi)
\]
\[
\tau_t = \frac{-\sigma_{22'} - \sigma_{33}}{2} \sin(2\phi) + \tau_{23'} \cos(2\phi)
\]
\[
\tau_L = \tau_{12'2} \cos(\phi) + \tau_{31'} \sin(\phi)
\]

where \(\phi\) has to be considered from \(0^\circ\) to \(180^\circ\), the angle \(\psi\) can be calculated by
\[
\tan(2\psi) = \frac{2\tau_{23}}{\sigma_{22} - \sigma_{33}}.
\]

### 3.3. Bilinear damage law

A bilinear law of how the strength of composites is degraded by damage is proposed by Pinho et al. (2006). When failure is initiated the material can still carry loads. After the peak load is reached the stress decreases linearly proportionally to the damage variable, Fig. 6.
Fig. 6. Schematic stress-strain curve for a composite.

At maximum strength, $\sigma_0$, damage is initiated and the damage variable $d$ is activated. The degraded stress after initial failure is given by

$$\sigma = (1-d)\sigma^{ef}$$

(13)

where $\sigma^{ef}$ is the stress for the undamaged cross-section, can be calculated by Hooke’s law. The damage variable is defined between zero and one, where zero is initial damage and one fully or final damage. Between initial and final damage $d$ is calculated by the strain or the shear strain, depending if the failure is caused by shear or strain. The general equation for the damage variable is given by

$$d = 1 - \frac{\varepsilon^0 - \varepsilon - \varepsilon^f}{\varepsilon^f - \varepsilon^0}$$

(14)

where $\varepsilon$, $\varepsilon^0$ and $\varepsilon^f$ are strain, strain at initial failure and strain at final failure respectively. For a shear failure the strains in Eq.(14) replaced by shear strains.

4. Adjustment to None Crimp Fabric

The models and theory described above are developed for UD-material. The relatively short cycle time in automotive industry makes the UD material less attractive. Other commonly used fibre fabrics are copied from the textile industry, such as woven, braided and knitted fabrics. Woven fabrics are suitable for low cycle time production. The waviness of the fibres in woven fabrics reduces the mechanical properties. The waviness is described with the crimp ratio, defined in Fig. 7(a). A fabric that is more suitable for low cycle time production than UD, but have better mechanical properties than woven is None Crimp Fabric (NCF). NCF consists of a number of fibre plies that are stacked on top of each other to obtain the desired properties, and the plies are stitched together with a yarn forming a blanket, Fig. 7(b).
Although the name of NCF implies the absence of crimp, some amount of crimp is always present in the fabric due to the yarn. In general the mechanical properties are not as good in NCF as for a UD, due to the fibre crimp. Damaged fibres from the stitching can have some influence on the properties as well. In tension the failure mechanism is similar to a failure for a UD. In longitudinal compression the fibre waviness promotes kinking, Edgren (2006).

The internal friction is higher in NCF than in UD due to the binding yarn between the layers. The higher friction gives a higher fracture angle for pure transverse compression ($\alpha_0$) than for UD prepreg. By experiments the fracture angle for pure transverse compressive stress has been measured to $62^\circ$, $Y_c = 130$ MPa and $S_L = 79$ MPa for the NCF used in this thesis. With Eq.(5), Eq.(6) and the measured values for $\alpha_0$, $Y_c$ and $S_L$ the longitudinal friction coefficient is above one ($\mu_L = 1.5$ and $\mu_T = 0.7$), Fig. 8.

One reason for the high friction coefficient can be the stitched yarn in the NCF. Another reason can be that Eq.(6) displayed in Fig. 8 is not valid for NCF, due to the difference in $Y_c$ and $S_L$ between NCF and laminated composites. Further investigation of the friction coefficient should be performed. A higher value of the friction coefficient increases the contribution of the normal stress in the compressive matrix failure criterion.
5. SICOMP’s model

SICOMP’s material model is a physically based model. Material input data can be directly obtained from experiments without any calibration. In this thesis the first version of SICOMP’s model is analysed. The purpose of the material model is to predict the entire failure for all the modes with a single model. The model couples friction and damage, by taking the sliding friction into account a more physical correct energy consumption and stress levels can be predicted. The left graph in Fig. 9 the stress curve (the red solid line) and the energy consumed by the material (the blue dashed line) are plotted as functions of the transverse strain without the sliding criterion, only static friction until initial damage. The damage growth follows the bilinear law, described in chapter 3.3. In the right graph the stress and the energy consumed plotted as functions of the strain with the sliding criterion. By introducing the sliding friction the stress response does not degrade to zero, and the material can still consume energy after final damage.

![Graph 1](energy_consumed_stress_curve.png)

*Fig. 9. The energy consumed (the blue dashed line) and the stress (red solid line) as functions of the transverse strain for compressive transverse failure, (a) bilinear law no sliding friction included, (b) with sliding friction included.*

SICOMP’s model has a physical friction where more energy is consumed by the material during crush compared to the bilinear law. By using SICOMP’s model a more correct stress and consumed energy can be predicted. In this version of the model the sliding friction is only introduced in compressive matrix failure. The bilinear law is used for all the other failure modes.

The kinking mechanism is not included in the first version, instead compressive fibre failure is assumed to have the same failure criteria as tensile fibre, but using the compressive strength instead of the tensile in Eq.(7).

Only one failure mode can be activated for each element. When a failure is initiated in an element it is locked to fail in that mode. This can cause problems for complex loading when several failure modes can be activated. Or if the load is changed after initial damage is activated in the element. For example, a compressive transverse load after initiated damage is
changed to a tensile transverse load. The element is locked to fail in compression, but in reality it will fail in tensile transverse mode. The damage variable is defined as in Eq.(14).

5.1. Compressive matrix failure

The compressive matrix failure criterion is calculated for every $15^\circ$, and $\alpha_0$ is also included. Once the criterion is activated the fracture angle is fixed. The criterion for initial failure is given by

$$f_{mc} = \left(\frac{\tau_L}{S_L}\right)^2 + \left(\frac{\tau_T}{S_T}\right)^2 = 1. \quad (15)$$

Compressive matrix failure is a shear failure therefore the damage variable is calculated by the shear strains. When the failure criterion is activated the damage variable, $d$ is calculated by

$$d = 1 - \frac{\gamma_{mc}^{0}}{\gamma_{mc}} \left(\frac{\gamma_{mc}^{f} - \gamma_{mc}^{0}}{\gamma_{mc}^{f} - \gamma_{mc}^{0}}\right) \quad (16)$$

where $\gamma_{mc}^{0}$ and $\gamma_{mc}^{f}$ is the shear strain at damage initiation, and the final shear strain respectively, given by

$$\gamma_{mc}^{0} = \sqrt{\gamma_{L,0}^2 + \gamma_{T,0}^2} \quad \gamma_{mc}^{f} = \sqrt{\gamma_{L,0}^2 + \gamma_{T,0}^2} \quad \gamma_{mc} = 2 \left(\frac{G_c / L_c}{\tau_0}\right) \quad (17)$$

where $\gamma_L$ and $\gamma_T$ are the strains longitudinal and transverse respectively. The longitudinal and transverse strains and the shear stress are denoted $\gamma_{L,0}, \gamma_{T,0}$ and $\tau_0$ respectively, all of them at initial failure. The fracture toughness of the material is denoted $G_c$. The characteristic length of a finite element is denoted $L_c$. For a cubic element with a fracture plane at $\alpha$ the characteristic length is given by, Gutkin & Pinho (2015)

$$L_c = \frac{L^2}{\cos(\alpha)} \quad (18)$$

Gutkin & Pinho (2015) suggested that damage and friction was coupled in 1D by

$$\tau = (1 - d) G \gamma + d \tau^{fric} \quad (19)$$

where $\tau^{fric}$ is the friction term and is dependent if there occurs any sliding or not, and are given by

$$\tau^{fric} = \begin{cases} G(\gamma - \gamma_s) & \text{if sliding does not take place} \\ -\mu \sigma & \text{if sliding takes place} \end{cases} \quad (20)$$
where $\gamma_s$ is the sliding strain and $\mu$ is the friction coefficient and are given by

$$\mu = \sqrt{(\mu_f \cos \lambda) + (\mu_s \sin \lambda)^2} \quad \text{where} \quad \lambda = \tan^{-1}\left(\frac{\tau}{\tau_L}\right)$$

(21)

5.2. Crash simulations with SICOMP’s model

To validate the material model a simple test setup is used. Flat specimens with all the fibres in the same direction are designed. Three different crash triggers are studied; chamfered, tulip and steeple, Fig. 10. The purpose of the crash trigger is to promote crush failure. Different angle on the crash triggers are investigated. The simple geometry is providing a well-known loading condition. The unidirectional fibre orientations simplify the analysis of the damage mechanisms.

![Fig. 10. Schematic images of the crash triggers, chamfered, tulip, and steeple, (a) viewed from the xy-plane, (b) viewed from yz-plane. The crash trigger angle is denoted $\beta$. The fibres are orientated as in 1) for longitudinal loads, and as in 2) for transvers loads.](image)

In this thesis three specimens are analysed closer. In transverse fibre direction one $10^\circ$ chamfered trigger, and one $30^\circ$ steeple trigger, for longitudinal one $30^\circ$ tulip trigger, Table 1.

<table>
<thead>
<tr>
<th>Trigger type</th>
<th>High [mm]</th>
<th>Width [mm]</th>
<th>Thickness [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Transverse chamfered $10^\circ$</td>
<td>22,3</td>
<td>3,7</td>
<td>1,9</td>
</tr>
<tr>
<td>Transverse steeple $30^\circ$</td>
<td>22,7</td>
<td>10,9</td>
<td>1,8</td>
</tr>
<tr>
<td>Longitudinal tulip $30^\circ$</td>
<td>21,6</td>
<td>10,3</td>
<td>1,9</td>
</tr>
</tbody>
</table>

In the experimental crush test the specimens were clamped between two metal blocks and crushed between two loading plates at quasi-static rate, Fig. 11. The metal block had a height of 17 mm.
In addition to good simulation results short simulation time is also desirable. In this section methods for saving computational time are presented. Since all the damage and crushing were assumed to be located in the top of the specimens they were modelled in two parts, Fig. 12(a). The upper part called “crash trigger” a fine mesh was applied to get a good resolution of the damage. The bottom part called “body” a coarser mesh was applied there. Since no damage was assumed to occur at the “body” the material model was not applied, i.e. the “body” was modelled with an elastic behaviour with no failure. The two parts were assembled together with a node to surface tie constraint. The “adjust slave surface initial position” was disabled, to get a smoother mesh interface between the two parts. The “crash trigger” was chosen as slave because of the finer mesh size. The crushing behaviour was assumed to be the same through the whole thickness of the specimens, which is true except at the edges of the specimen. This assumption entails that only a slice of the thickness for the chamfered- and steeple specimens were enough to model. The chamfered- and steeple specimen was modelled with one element row in the thickness. To avoid the specimens to buckle and to represent the experimental conditions, one of the $xy$-faces was fixed in $z$-direction, Fig. 12(a). The tulip trigger could not be modelled with one element row through the thickness due to the geometry and the experimental configuration, to save time it was modelled as a quarter model, Fig. 12(b).

In this section the finite element configuration of the crush test is described. The chamfered specimen was enclosed by four analytical rigid plates, Fig. 12(c). The rigid plate under and the vertical at both side of the specimen were fixed in displacement and rotation in all directions. The top plate was fixed in all directions except in the $y$-direction, where the displacement was prescribed. The steeple specimen had a similar configuration as the chamfered, but one of the vertical plates in Fig. 12(c) was replaced with a symmetry boundary in $x$-direction. The tulip specimen was modelled with three rigid plates, Fig. 12(d).
General contact was applied for the interaction between the analytical rigid plates and the specimen.

5.3. Validating the simulations

To validate the numerical result three energies had to be considered, kinetic energy, artificial energy, and strain energy.

The simulations were quasi-static, which means that the kinematic influence should be small, for good simulation results the kinetic energy should be close to zero. Parameters affecting the kinetic energy are time step, time increment, density, and total displacement of the top plate.

The artificial energy has no physical significance, it is numerical energy to keep the elements in shape and prevent them from hourglass. The artificial energy should not be higher than 5\% of the strain energy. The artificial energy is affected by the mesh size and boundary conditions. The elements should be cubic and regular in order to gain low artificial energy.

Strain energy is the energy absorbed by the specimen’s on-going deformation.

6. Parametric study

The parametric study presented in this section is summarised in a table in Appendix B. The table contains both tested and selected values of all parameters.

Shorter time period and larger time increments speeds up the simulation, but can introduce kinetic effects on the response. These two parameters together with the prescribed
displacement of the top plate define the loading rate. By trying different values for the time step and the time increment a combination giving the shortest simulation time, but still a negligible kinetic influence was found.

To get a good resolution of the damage with a short simulation time, different numbers of elements in the width were investigated to find a sufficient number. Partitions have been studied as well. The trigger distorts the mesh, by creating a partition the distortion from the crash trigger does not spread to all the elements. Two different partitions had been tested, one horizontal, Fig. 13(a), and one parallel to the chamfered, Fig. 13(b).

![Fig. 13. Partitions tested, (a) straight line, and (b) the partition parallel to the trigger angle.](image)

Higher trigger angles add more difficulties to build a regular mesh, thus correct partitioning becomes more important. With the horizontal partition the “crash trigger” cannot be meshed with a single row of elements in the depth. The reason why Abaqus would not allow one element trough the thickness was not further investigated.

The elements in the model were continuum 3D with reduced integration (C3D8R). Since the elements have reduced integration, hourglass control had to be investigated. Stiffness based (default) and viscoelastic (enhanced) hourglass control was tested. The enhanced hourglass control can provide an increased hourglass resistance for non-linear problems at high strain levels with only small additional computational cost. Since hourglass was a problem, fully integrated elements were tested. The fully integrated elements cannot hourglass since they have several integration points. However, SICOMP’s model calculates fracture angles at each integration point. With fully integrated elements it is possible to have different fracture angels at different integration points in the same element. Different fracture angles in one element do not have a physical interpretation. Another drawback is the computational time that is about one order of magnitude higher. With fully integrated elements the damage variable had values greater than one, but the damage variable is only defined between zero and one. The damage variable could grow larger than one due to interaction between the gauss points. A greater problem was even fully damaged the fully integrated elements were too stiff, and no load drop could be observed.

Tested crash triggers were chamfered, tulip and steeple for both longitudinal and transverse loading.

Four specimens with a 10° chamfered trigger, exposed to a transverse load were modelled with different friction coefficients between the specimen and the plats: $\mu = 0.16$, $\mu = 0.2$, $\mu = 0.25$, and $\mu = 0.30$, Fig. 14. Higher friction coefficient gives a stiffer response. Due to
the friction force the top plate does not slide as much on the specimen, this reduces interlaminar damage to the specimen.

![Graph](image)

**Fig. 14.** Transverse stress as a function of the displacement for chamfered 10° trigger with four different friction coefficients between the specimen and the plats.

For the contact interaction surface–to-surface and general contact algorithms were tested. The general contact was easy to apply and the reaction force responses on the horizontal plates were smoother than surface-to-surface contact. According to Abaqus documentation, general contact formulation uses sophisticated tracking algorithms to ensure that proper contact conditions are enforced efficiently. The general contact algorithm is by default only defined for the surfaces nodes of the elements. For large deformations when the surface of the specimen breaks the plate penetrates the internal nodes, since these nodes are not defined as a surface. The plate and the specimen were only in contact with the initial surface nodes, Fig. 15. For higher trigger angles this was a problem since the deformation was larger on these specimens.
By creating a node-based surface that also contains the internal nodes of the specimen the top plate could not penetrate the initial nodes, and a more physical realistic reaction force was obtained for the top plate.

### 7. Validation and discussion

In this section the simulations are compared with experimental data. The validation includes three parts. First, experiments and simulations of stress-displacements curves are compared, secondly the artificial-strain energy ratio from the simulation is investigated, and finally the damage growth in the simulation is compared with experimental pictures. The artificial-strain energy is a non-physical energy, it is a numerical energy to keep the elements in shape and prevent them from hourglassing, and it should not exceed 5 % for good simulations. Stresses were calculated with the constant bottom area for both specimens in experiments and simulations. The reaction force from the simulations was measured in the top plate.

#### 7.1. Transverse loads

In this section two trigger types with different geometry and angle were validated.

**7.1.1. Transverse chamfered with 10° trigger**

The stiffness of the simulated- and experimental specimen correlated. The results can be questionable since the artificial-strain energy ratio (AE/SE) exceed the 5 % limit for a valid simulation. Different boundary condition and hourglass control have been tested but the artificial-strain energy ratio could not be reduced below 5 %, Fig. 16. To achieve a lower AE/SE ratio it is possible that the material model has to be improved. But still AE/SE is relatively small during the simulation and the results can be used as an indication of how well
the material model can capture the crashing behaviour of composites. The AE/SE-peaks in the beginning of the simulation was due to both the strain- and artificial energy were small.

![Graph](image)

Fig. 16. Transverse chamfered 10° trigger, (a) the stress as a function of the displacement, (b) the artificial-strain energy ratio (AE/SE) as a function of the displacement.

The experimental 10° chamfered with NCF did not behave as expected, a lot of interlaminar damage was observed, Fig. 17(a). Earlier conducted experiments with a UD prepreg, Fig. 17(b), failed with less delamination and had more similarities to the simulation, Fig. 17(c) and (d). As mentioned earlier the binding yarn in the NCF can affect the mechanical properties, but these defects were not included in the simulation. With a smaller specimen the stitches influence the mechanical properties more. The transverse 10° chamfered specimen had a bottom area of 7.03 mm² this could be one of the reasons why the NCF failed with little crush.

![Images](image)

Fig. 17. Transverse chamfered 10° trigger, (a) Experimental at \( U_2 = 0.63 \), (b) Experimental UD, (c) Simulation at \( U_2 = 0.9 \), displaying matrix compressive damage, (d) simulation at \( U_2 = 0.9 \), displaying tensile matrix damage.

Another reason why the NCF specimen fails with more interlaminar damage compared with the prepreg specimen could be the fibre configuration. In the prepreg specimen fibres were
lying individually in the layers, Fig. 18(a). The fibres in NCF specimen were bundled. When the NCF specimen was compressed the fibre bundles was wedged by each other and more delamination and interlaminar damage occurs, Fig. 18(b).

![Image](a) ![Image](b)

*Fig. 18. Schematics figures of two layers before and after transverse compression, (a) prepreg, (b) NCF.*

7.1.2. **Transverse steeple with 30° trigger**

The transverse 30° steeple trigger was modelled with and without cohesive elements. From the experimental pictures interlaminar fractures could be observed in the middle of the specimen, and therefore the cohesive elements were applied on the symmetry boundary row. With the cohesive elements the simulation results correlate better with the experiments, Fig. 19.

![Graph](a) ![Graph](b)

*Fig. 19. Transverse steeple 30° trigger, (a) The stress as a function of the displacement for experimental, simulation without cohesive elements and simulation with cohesive elements, (b) the artificial-strain energy ratio as a function of the displacement.***

In the simulation with cohesive elements some elements were falling off of the specimen due to the deformation. These elements were not deleted and affected the artificial energy, since Abaqus still used artificial energy to keep them in good shape. By further investigate the element deletion parameter and make sure that elements that falls of the specimen do not contribute to the artificial energy a more trustworthy simulation can be achieved. Deleting these off fallen elements would not be enough for reducing the artificial-strain energy ratio below 5 %. When elements falls off from the specimen the load is distributed on fewer elements this lead to bigger distortion on them, Fig. 20. To achieve AE/SE below 5 % it may
be possible that the model needs to be modified in order to increase its stabilization. In the simulation without the cohesive elements was the AE/SE low but the strength was overrated, delamination has to be included to predict the stresses in the specimen.

![Experimental](image1) ![Without cohesive elem.](image2) ![With cohesive elem.](image3)

\[ U_2 = 0.3 \]

\[ U_2 = 0.6 \]

*Fig. 20. Transverse steeple trigger at displacement 0.3 and 0.6, (a) experimental, (b) compressive matrix damage without cohesive elements, (c) compressive matrix damage with cohesive elements.*

The experimental specimen bent to one side, which was an effect of voids and other defects that were not included in the simulation. When the cohesive elements were eliminated the symmetry boundary disappears, which was the reason why the specimen with cohesive elements can bend over the symmetry. The specimen behaves in a non-physical way. Cohesive elements should be added further from the symmetry boundaries, or a full model simulation with cohesive elements in the middle of the specimen would eliminate this problem.

### 7.2. Longitudinal loads

For specimens with longitudinal fibres interlaminar failure was more common. The trigger with least delamination was the tulip trigger.
7.2.1. **Longitudinal tulip with 10° trigger**

Two different models were investigated for the longitudinal tulip with 10° trigger. The first was as described above with a sharp top at the trigger. The second specimen had a flat top, Fig. 21. The flat top on the specimen allows the plate to have an initial contact with whole top elements instead of only the top nodes at each top element as for the sharp top simulation.

![Flat top specimen](image)

*Fig. 21. The 10° tulip trigger with a flat tip, the top plate has initial contact with the whole top elements.*

Local crushing occurs at small displacements in the experimental specimen, which the model with a sharp top had trouble to capture. To better capture the local crushing a flat top specimen was modelled, Fig. 22.

![Stress vs Displacement](image)

*Fig. 22. Longitudinal tulip 10° trigger, (a) The stress as a function of the displacement for experimental, simulation, (b) the artificial-strain energy ratio as a function of the displacement.*

The simulation with a sharp top had an elastic behaviour for small displacements. After $U \approx 0.03$ mm a change of the stiffness could be observed, but the response was still too stiff. The stiffness of the sharp-top-simulation did not correspond to the experimental stiffness until after the peak stress, but then the simulation was aborted. The fibre failure mode was activated in some elements. The deletion of those elements seems to happen too fast for the simulation to catch up, all the surrounding elements become distorted and the simulation was aborted, Fig. 24(a). In the experiments local crushing occurs at small displacements. The sharp top simulation cannot capture these due to some numerical problems with only the top
nodes in initial contact. In the sharp top simulation crushing was predicted to happen after 0.2 mm displacement, but in the experiment it occurs right from the beginning. The peak stress was captured with the sharp tip simulation.

With the flat trigger top more nodes were in contact with the top plate and the stiffness had a good correlation from the beginning of the simulations, but the peak stress could not be predicted. Almost just compressive matrix failure was activated for the flat top, Fig. 24(b).

The sharp tip simulation could predict the peak value but failed to predict the stiffness it may be due to that the material model has no kinking model included. The flat top simulation predicted the stiffness but could not predict the peak stress which could be due to no cohesive elements was used in the simulation or no delamination was included in the material model, which means that interlaminar damage could not be captured.

It is hard to detect combination of failure modes and especially inside the specimen. From the simulation it seems that crushing of the matrix occurs right from the beginning, and the peak value is decided by the final fibre failure or/and when delamination failure occurs. Both kinking and delamination should be further investigated.

8. Future work

Composites have a complex behaviour in crash. To fully understand and predict crash behaviour with numerical simulations improvements can be done in both the model and the
simulation setup. To capture the stiffness with longitudinal fibre direction a physically based kinking model has to be included in the model. In the current model improvements on the element deletion can be done. When deletion of elements in compressive longitudinal failure is activated the surrounding elements becomes too distorted and the simulation is aborted. An element that falls off the specimen has to be deleted so it does not contribute to the artificial energy.

Delamination is an important failure mode in crush, especially for steeple specimens, and longitudinal loading, since more delamination occurs then. Another solution to capture interlaminar damage is to model every ply as a row of elements and apply cohesive elements between every layer.

The artificial energy has to be reduced; the artificial-strain energy ratio has to be below 5 % for the whole simulation to have a valid simulation. Different boundary condition and hourglass control has been test. To further increase the stabilization of simulation the model could be improved.

Full models have to be investigated to really capture the behaviour of the specimens in the simulation. To fully understand the behaviour more complex layups and specimen geometries need to be investigated.

9. Conclusions

The simulation results in this thesis can be questioned due to artificial-strain energy ratio above 5 %, but the results can still be used as a good indication of the predictability of the current model. SICOMP’s material model can capture the transverse crushing behaviour reasonably without any tuning of parameters neither using any filter. The validation of the longitudinal failure mode is more complex since more interlaminar damage occurs. When elements are deleted by compressive fibre failure the simulation aborts due to surrounding elements becoming excessively distorted. Even for the longitudinal load case the matrix properties seems to be of most importance for this version of the material model.

The chamfered crash trigger has no geometric symmetry in the x-direction, Fig. 12. This allows the chamfered specimens to bend more during crushing. In simulation the bending of the specimen distorts the elements that lead to higher artificial energy. The steeple is the best trigger from a simulation time point of view, since least elements are needed. For longitudinal loading the tulip gave best experimental result with least interlaminar damaging.

Lower angles make it easier to create a good mesh that leads to a lower artificial energy and a better simulation result.
10. References


Pinho ST et al., 2005. Failure Models and Criteria for FRP Under In-Plane or Three-Dimensional Stress States Including Shear Non-Linearity, NASA, TM-2005-213530,


Appendix A: Simulations of matrix failure using Puck’s criterion

A subroutine written in FORTRAN (run in Abaqus) with Puck’s criterion for matrix failure was modified to predict failure initiation. The required material properties to run the subroutine and its values are shown in Table A2. The material used was NCF.

Table A2. Necessary input parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$Y_t$</td>
<td>29 MPa</td>
</tr>
<tr>
<td>$Y_c$</td>
<td>130 MPa</td>
</tr>
<tr>
<td>$S_L$</td>
<td>79 MPa</td>
</tr>
<tr>
<td>$\alpha_0$</td>
<td>62°</td>
</tr>
</tbody>
</table>

The failure criterion was calculated for $0^\circ \leq \alpha < 180^\circ$ with a $15^\circ$ step and also consider the fracture angle, $\alpha_0$, and $\pi - \alpha_0$. The highest failure index and the angle where it occurs were saved and displayed for the user.

In order to validate if the right fracture angle could be captured, two load cases were performed on a single element cube, with boundary condition according to Fig. A1. The two load cases were: pure transverse compressive stress, and pure transverse tensile stress.

![Fig. A1. Pure transverse compressive stress on a single cubic element](image)

The simple geometry of the cube makes it easy to see if the model could capture the fracture angle. In Fig. A2 the failure index for both cases are displayed as a function of the fracture angle.
Fig. A2. Failure index as a function of the fracture angle, (a) for pure transverse compressive stress, (b) pure transverse tensile stress.

For both cases the failure was initiated at the expected angle and correlates with experimental data. For the compressive and tensile stress the fracture occurs at 62° and 0° from the plane perpendicular to the load respectively.
Appendix B: Parametric study

Table 1B. Studied parameters the values that have been tested and the respective results.

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Testes performed</th>
<th>Results</th>
</tr>
</thead>
<tbody>
<tr>
<td>Time step</td>
<td>1 / 0,1 / 0,05 / 0,01</td>
<td>Use 0,05</td>
</tr>
<tr>
<td>Time increment</td>
<td>Automatic 10^{-7} / 10^{-8}</td>
<td>Use 10^{-8}</td>
</tr>
<tr>
<td>Nº of elements through thickness</td>
<td>3 / 5 / 7 / 10</td>
<td>Use 10</td>
</tr>
<tr>
<td>Partitions</td>
<td>without, straight and parallel</td>
<td>Use parallel to the trigger angle</td>
</tr>
<tr>
<td></td>
<td>to the trigger angle</td>
<td></td>
</tr>
<tr>
<td>Hourglass control</td>
<td>Default and enhanced</td>
<td>Use enhanced</td>
</tr>
<tr>
<td>Element deletion</td>
<td>Default and on</td>
<td>Use on</td>
</tr>
<tr>
<td>Trigger type</td>
<td>Chamfered, tulip and steeple.</td>
<td>Steeple best for simulation</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Tulip best experimental results</td>
</tr>
<tr>
<td>Trigger angles</td>
<td>10°/ 30°/ 57°</td>
<td>Better FE behaviour for lower crash angles</td>
</tr>
<tr>
<td>Fibre layups</td>
<td>0° / 90°</td>
<td>The model works better for</td>
</tr>
<tr>
<td></td>
<td></td>
<td>fibre layup 90° with respect to the load</td>
</tr>
<tr>
<td>Friction coefficient between plate and specimen</td>
<td>0.16 / 0.32</td>
<td>High influence on the results</td>
</tr>
<tr>
<td></td>
<td></td>
<td>The artificial energy was lower with 0.32</td>
</tr>
<tr>
<td>Element type</td>
<td>3D reduced and fully integrated (C3D8R/C3D8)</td>
<td>The model does not work with fully integrated</td>
</tr>
<tr>
<td>Contact formulation</td>
<td>Surface to-surface, general contact, and contact erosion</td>
<td>Use general contact, for higher crash angles, it is suitable with contact erosion</td>
</tr>
<tr>
<td>---------------------</td>
<td>--------------------------------------------------------</td>
<td>--------------------------------------------------------------------------------</td>
</tr>
<tr>
<td>Type of B.C. in z-direction.</td>
<td>No B.C., Z-Symmetry, and U3 = 0</td>
<td>Use U3 = 0</td>
</tr>
</tbody>
</table>