MODELLING OF THE MECHANICAL RESPONSE OF THERMOPLASTIC MATRIX TEXTILE COMPOSITES UNDER DYNAMIC LOADING – A COMPARISON OF METHODS

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Abstract

The observed large inelastic deformation, high fracture strain and significant energy dissipation capability as well as high throughput manufacturing and recycling potential makes fibre reinforced thermoplastics a strong candidate in highly structurally loaded components and crash applications in the automotive industry. The large inelastic deformation observed in thermoplastics requires a rethinking of modelling strategies, especially in terms of predicting failure and energy dissipation. In particular, the significant rate sensitivity and non-linearity of the matrix material needs careful consideration when developing constitutive models. This paper compares a complex damage mechanics based constitutive model with a pragmatic plasticity based model to predict the behaviour of glass fibre reinforced thermoplastic materials under impact loading. The models are compared using simple single element simulations as well as impact experiments on novel multi-layered flat-bed weft-knitted fabrics. While the CDM model more accurate predicted the non-linear shear behaviour, the macroscopic response of both modelling approaches was very similar and allowed to correctly reflect the major damage mechanisms observed in impact bending experiments.

1. Introduction

Textile-reinforced thermoplastics exhibit high specific mechanical properties (stiffness, strength, fracture toughness) and – compared with thermosetting composites – an improved damage tolerance, enhanced impact resistance as well as the recyclability and feasibility for a low-cost rapid production [1]. The use of thermoplastic reinforced fibre composites in structural applications with impact loading, however, requires good modelling tools that also consider inelastic deformation and strain rate dependent material behaviour. In particular the modelling of the inelastic deformation results in differences to more established modelling strategies for thermoset matrix composites. This paper aims to compare two main approaches. Inelastic deformation is modelled using a plasticity based approach compared to a damage mechanics based approach where all non-linearity is expressed as damage.

Existing constitutive models already allow for an accurate representation of the inelastic behaviour of thermoplastics. Prominent examples are viscoelastic-plastic models such as Boyce [2] and Polanco-Loria [3]. While accurately modelling the physics underlying the large plastic deformations and strain rate dependency in thermoplastics, these models require large numbers of parameters and are also difficult to incorporate into composite damage models. Other models use simplified formulations that capture some of the effects without fully representing the physical mechanisms. Examples are Goldberg et al. [4] and an extension by Gerlach [5].

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Continuum Damage Mechanics (CDM) based models originated mainly from the desire to model the brittle response of thermoset matrix composites. There are many CDM based models available now and some have already been implemented into Finite Element software such as LS-DYNA and ABAQUS. Prominent examples are Matzenmiller [6] and Pinho [7]. CDM models were further extended to also incorporate strong non-linearity observed in thermoplastic materials. One example, which is also used in this study, is the phenomenological damage mechanics model developed by Boehm [8]. Both modelling approaches, plasticity and CDM, are being used to investigate a novel multi-layered flat bed weft-knitted glass fibre fabrics with a thermoplastic matrix (PP) [9].

2. Plasticity modelling

A novel constitutive model was developed for this study. The model separately considers the thermoplastic matrix and the reinforcing fibres with two reinforcement directions. A volume averaging approach to homogenise the response of the ingredients on the unit cell level is employed.

2.1. Matrix model

The thermoplastic matrix is modelled as an isotropic material using the Goldberg model [4]. The strain tensor is decomposed into an elastic and inelastic part

$$\varepsilon_{ij}^{T} = \varepsilon_{ij}^{E} + \varepsilon_{ij}^{I}. \tag{1}$$

The stress in the matrix is calculated only from the elastic deformation

$$\boldsymbol{\sigma} = \boldsymbol{C} : \boldsymbol{\varepsilon}^{E} . \tag{2}$$

The inelastic deformation is obtained from the inelastic strain rate by

$$\dot{\varepsilon}_{ij}^{I} = 2D_0\, exp\left[-\frac{1}{2}\left(\frac{Z}{\sigma_e}\right)^{2n}\right]\left(\frac{S_{ij}}{2\sqrt{J_2}} + \alpha\delta_{ij}\right). \tag{3}$$

A detailed description of the model can be found in [5]. Rate dependent stiffness is modelled using the same model used for the CDM model presented later in this paper (see Eqn 13) with a_1 being a parameter derived from simple uniaxial tension or compression experiments at varying strain rate.

Fracture of the matrix material is modelled using the von Mieses equivalent strain criterion. Strain based fracture criteria are more appropriate than stress based failure models as the material shows a very low hardening modulus [5]. The failure criterion reads

$$\varepsilon_{eq} \geq \varepsilon_f \quad \text{with} \quad \varepsilon_{eq} = \sqrt{\frac{2}{3}\left(\varepsilon_{11}^2 + \varepsilon_{22}^2 + \varepsilon_{33}^2 + 2\varepsilon_{33}^2\right)} \tag{4}$$

and ε_f being a material constant. Fracture is modelled by releasing a fixed amount of energy, equivalent to the energy needed to create a crack. The energy dissipation algorithm includes a characteristic element size parameter to remove mesh sensitivity and is similar to the one used for fibre rupture, which will be discussed as part of the fibre model below.

2.2. Fibre model

The fibres are modelled using a simple elastic model. The stress in fibre direction is simply given by

$$\sigma_{11}^{f} = C_{11}^{f}\varepsilon_{11}^{f} + C_{22}^{f}\varepsilon_{22}^{f} + C_{33}^{f}\varepsilon_{33}^{f}, \tag{5}$$

while all other stresses are considered to be zero. The fibre behaves linear elastic and no rate effects are considered in this study. Rupture of the fibre is modelled using a simple maximum stress criterion that reads

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$$\frac{\sigma_{11}^{f}}{X_t(1-r^t)} \geq 1 \tag{6}$$

with X_t being the tensile strength of the fibre. The strength is calculated from a failure strain by $X_t = E_f\,\varepsilon_{1t}$, where E_f denotes the Young's modulus of the fibre and ε_{1t} is the strain at onset of failure. Once rupture is predicted the fibre properties are degraded using damage mechanics. A damage variable is calculated such that the amount of energy released equals a specified fracture energy

$$r^t = r^{t-\Delta t} + m\Delta\varepsilon \quad \text{with} \quad m = \frac{1}{\varepsilon_f - \varepsilon_0} \quad \text{and} \quad \varepsilon_f = \frac{2G_f}{\sigma_0 l_c}\ . \tag{7}$$

In the equations above ε_0 is the strain at onset of damage and ε_f is the strain when all energy has been dissipated. G_f is the energy release rate associated with fibre rupture and σ_0 is the stress in fibre direction at the onset of fibre rupture. The parameter l_c is a characteristic element size parameter to remove mesh dependency from the energy dissipation mechanism. Finally, the damaged stress in fibre direction is given by

$$\sigma_{11}^{fd} = X_t(1-r^t)\ . \tag{8}$$

Similar to fibre rupture, also compressive fibre failure is considered. The equations are as above with the associated strength being X_c and the fracture toughness being $G_{kinking}$.

2.3. Homogenisation

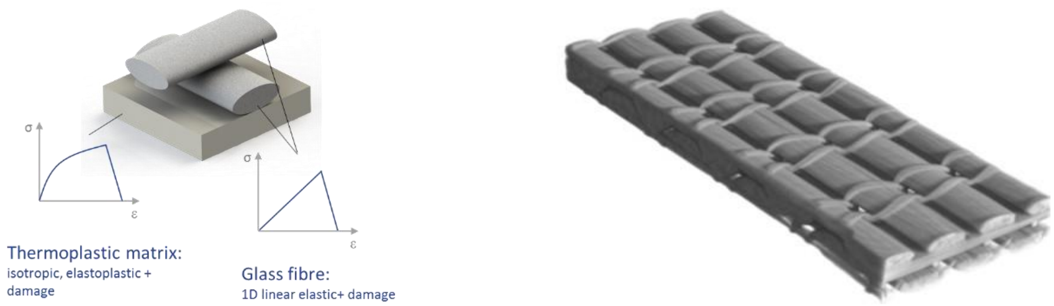
After evaluating the stress states and subsequent plasticity and/or damage state in the constituents, a homogenisation procedure determines the homogenised stress state at the unit cell level. The unit cell in this model consists of three parts (matrix, ply1, ply2, see Figure 1). Each part is characterised by a volume fraction that defines its portion of the unit cell. The fibre stresses of each individual ply is rotated into the coordinate system of the unit cell. The matrix stresses are evaluated already in the unit cell coordinate system and do not need to be transformed. The in-plane response is governed by the fibre plies and the matrix and the unit ply stress in x and y direction is given by

$$\sigma_i = \frac{1}{V}\int_V \sigma_i^k\, dV \quad \text{where } i = x, y \text{ and } k \text{ denotes the ply (ply1, ply2, matrix).} \tag{9}$$

The out-of-plane stress components are taken only from the matrix as the behaviour of the unit cell is assumed to be matrix controlled. This simplification does not, however, allow the representation of the binder glass fibre knitting yarn. Therefore, two empirical shear correction factors (SC_{ip}, SC_{oop}) were introduced as follows

$$\sigma_{12} = SC_{ip}\sigma_{12}^{m},\ \sigma_{23} = SC_{oop}\sigma_{23}^{m},\ \sigma_{13} = SC_{oop}\sigma_{13}^{m}. \tag{10}$$

While not explicitly modelling the textile, the correction factor enables the representation of the textile structure in the model. The constitutive model was implemented into LS-DYNA as a user material.



a) unit cell (schematic) *b) CT scan of MLG [10]*

Figure 1. Plasticity based composite model - overview

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3. Continuum damage mechanics based model

The used phenomenological material model is based on a continuum mechanical approach to predict direction dependent and mode related damage based on Cuntze [11]. An accumulative and mode related damage evolution assures a successive stiffness degradation for each orthotropic textile layer [8, 9, 12]. It is based on the assumption of linear-elastic material behaviour up to an initial damage threshold characterized by the damage strength $R_{di\,t/c}$ for each direction, with t denoting tension and c compression. The damage evolution effects induce stress-strain non-linearity's up to final failure. The damage initiation is described by the following criterion:

$$H^n = \left(\frac{\sigma_1^+}{R_{d1t}}\right)^n + \left(\frac{\sigma_1^-}{R_{d1c}}\right)^n + \left(\frac{\sigma_2^+}{R_{d2t}}\right)^n + \left(\frac{\sigma_2^-}{R_{d2c}}\right)^n + \left(\frac{\sigma_3^+}{R_{d3t}}\right)^n + \left(\frac{\sigma_3^-}{R_{d3c}}\right)^n + \left(\frac{\tau_{12}}{R_{d12}}\right)^n + \left(\frac{\tau_{31}}{R_{d31}}\right)^n + \left(\frac{\tau_{23}}{R_{d23}}\right)^n = 1 \qquad (11)$$

with n defining the failure envelope. In this study n was fixed to a value of 2. Although a successful application of the material model was shown in previous work [13, 14], some results indicate a non-physical response under compression and shear loading based on an automatic element deletion when a single failure mode reaches the failure threshold. Therefore, pragmatic non-interactive strain based failure criteria were used here

$$F_{t,c} = \left(\frac{\varepsilon_i}{\varepsilon_{i,fail}}\right) = 1 \quad \text{and} \quad F_s = \left(\frac{\gamma_{ij}}{\gamma_{ij,fail}}\right) = 1. \qquad (12)$$

After fracture is predicted, the failed element will be deleted for tensile failure modes only, while a residual strength is retained for compressive and shear modes as some load can still be transmitted. Strain rate dependency is considered by a scaling function for the stiffness parameters and the damage strengths:

$$E_i^0(\dot{\varepsilon}_i) = E_i^{0,\mathrm{ref}}\left[1 + a_i \ln\left(\frac{\dot{\varepsilon}_i}{\dot{\varepsilon}_i^{\mathrm{ref}}}\right)\right] \quad \text{and} \quad R_{di}^0(\dot{\varepsilon}_i) = R_i^{0,\mathrm{ref}}\left[1 + c_i \ln\left(\frac{\dot{\varepsilon}_i}{\dot{\varepsilon}_i^{\mathrm{ref}}}\right)\right] \qquad (13)$$

The model has been implemented in the FE software Abaqus/Explicit through a VUMAT.

4. Model calibration and comparison

The CDM model requires the 9 elastic constants (Young's moduli, shear moduli and Poisson's ratios) as well as 9 strength properties, which define the onset of diffuse damage (material non-linearity) and 9 failure strains. Damage evolution is controlled by an additional 18 parameters (2 for each damage mode) and rate dependency is defined by 9 constants. The calibration of the damage evolution requires a large testing program to provide the stress strain response in all material directions and all shear planes.

In contrast, the plasticity based model predicts the response from the ingredients, namingly fibres and matrix. Input properties are limited to the Young's moduli and Poisson's ratios of the thermoplastic matrix and the glass fibre. In addition, a failure strain for matrix and fibre as well as associated energy release rates are required. The matrix properties were obtained from simple tension tests of polypropylene at 6 different strain rates following the procedure described in [5] and data from [15]. A comparison of experimental data and numerical prediction for the matrix response is given in Figure 2. The properties of glass fibre are widely available in literature. Tensile and compressive failure strain as well as the correction factors that incorporate the textile behaviour were fitted to shear tests available here [1]. Volume fractions were known from the textile configuration.

Single element simulations were conducted to compare the model predictions and assess the quality of the prediction based on experimental data. The results are displayed in **Figure 3**. Both models predict the tensile response in fibre direction well (Figure 3a)). The non-linear stiffness response is also predicted. The plasticity based model slightly under predicts the stiffness, which might be caused by an assumed fibre stiffness that is too low. The post failure behaviour is notably different. The CDM model rapidly releases the energy, while the plasticity model allows for damage evolution that controls the amount of energy release.

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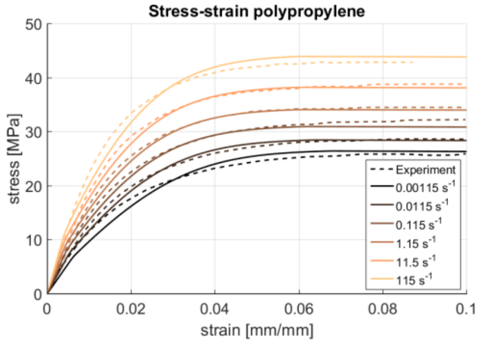
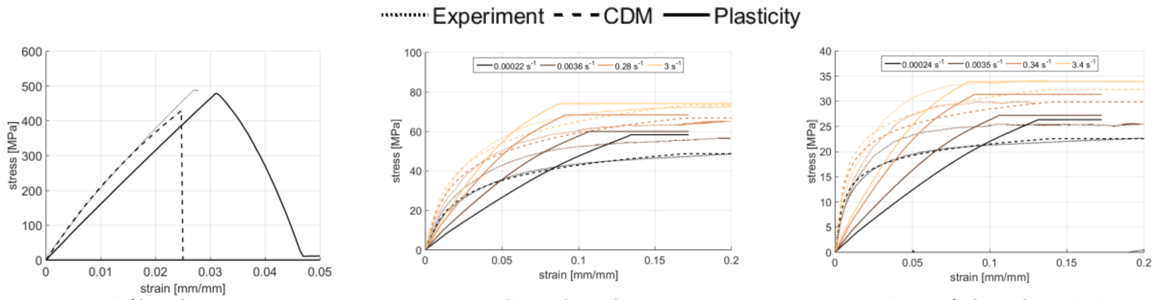


Figure 2. Comparison of experiment and prediction for polypropylene



a) fibre-direction *b) in-plane shear* *c) out-of-plane shear (23)*

Figure 3. Comparison of single element simulations with selected experimental results

Significant differences exist between the two constitutive models for the in-plane and out-of-plane shear cases (see Figure 3b) and c)). As expected, the CDM reproduces the stiffness and non-linearity very well as these load cases were used for calibration. The plasticity model, which predicts the response based on the unit cell, cannot correctly model the initial stiffness of both shear cases. This is most likely due to the simplifications of the chosen unit cell architecture. Out of plane binder glass fibres (see Figure 2b)), for example, are not considered in the model. Rate dependency and stress levels at damage saturation are predicted well by both models and in good agreement with the experiments, although a better fit is observed for the CDM model at lower strain rates.

5. Simulation of impact plate bending experiments

5.1. Experimental setup and finite element representation

Plate bending experiments were conducted at the Impact Engineering Laboratory of the University of Oxford in order to demonstrate and compare the predictive capability of both constitutive models. The experiments were performed and analysed using the methodology described in [16]. An instrumented titanium bar is fired against an unconstrained plate, which is simply supported by a base plate with a circular hole of 50mm diameter. The contact force and impactor tip displacement is derived from a calibrated strain gauge on the bar itself. Specimen plates of 100x100 with a thickness of 1mm and 3mm were subjected to impacts with velocities ranging from 6.5 to 9.5 m/s.

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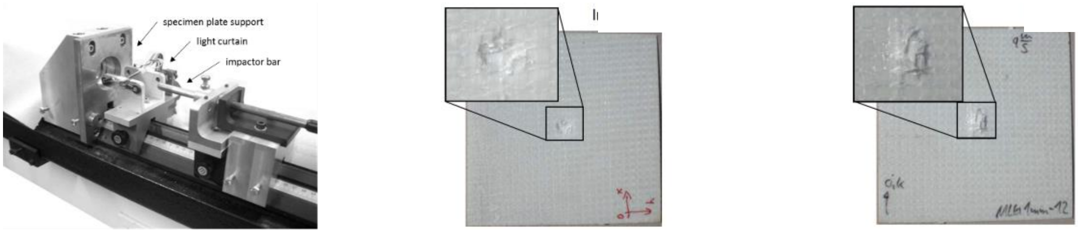

a) impact apparatus *b) impact damage 1mm plate with 9.5 m/s (impacted side)* *c) impact damage 1mm plate with 9.5 m/s (rear side)*

Figure 4. Experimental setup and observed visible specimen damage

The plates with one and three mm thickness showed similar behaviour. A significant inelastic deformation is observed after a very small linear elastic response. The inelastic deformation occurs at a constant force to about 2mm deflection (1mm plates) and 1mm deflection (3mm plates). This plateau is followed by linear deflection behaviour until the projectile is accelerated backwards. The force and deflection of this rebound point depends on material thickness and impact velocity. The projectile did not penetrate any specimen plate in this test series. Only the impact with 9.5 m/s on the 1mm plate resulted in some fibre rupture on the not impacted face of the specimen plate (see Figure 4c). An ultrasound scan revealed a small amount of localized damage around the impacted area. The boundary conditions resemble the experimental setup as closely as possible. The impacting titanium bar is modelled in full and impact is induced through an initial velocity applied to the unstressed bar. The target plate is entirely unconstrained. The back plate is fully constrained. Both impactor bar and target plate were modelled using underintegrated hexahedral solid elements, while the support plates is meshed with shell elements. The 3mm plate was modelled using 6 elements through the thickness with an in-plane edge length of 0.5 mm, which the 1mm plate only uses 2 elements through thickness with the same in-plane element size.

5.1. Results and comparison

The experimental setup described above was modelled in ABAQUS/Explicit (CDM model) and LS-DYNA (Plasticity model). Force deflection data is compared in Figure 5 and contour plots of the damage predicted are given in Figure 6.

The force-history and force-deflection curves show a good agreement with experimental data. The local inelastic damage observed shortly after the impactor hits the target specimen is modelled well by both constitutive models. The plasticity model slightly under predicts the subsequent constant force plateau in the force deflection-history. With the exception of the 3mm plate, both models correctly predict the subsequent stiffness of the plate. Fibre damage was only observed for the 1mm plate with 9.5 m/s impact. Both model correctly predict the onset, but only the plasticity model was able to represent the subsequent load drop.

The contour plots enable a comparison of observed and predicted damage zone (Figure 6). The CDM model predicts the relative shape of the zone, but under predicts the size. The plasticity model mainly indicates damage along the fibres that cross in the centre of the plate. Damage evolution as well as plastic deformation is predicted along these fibres, which subsequently does not result in a good prediction of the shape of the damage zone.

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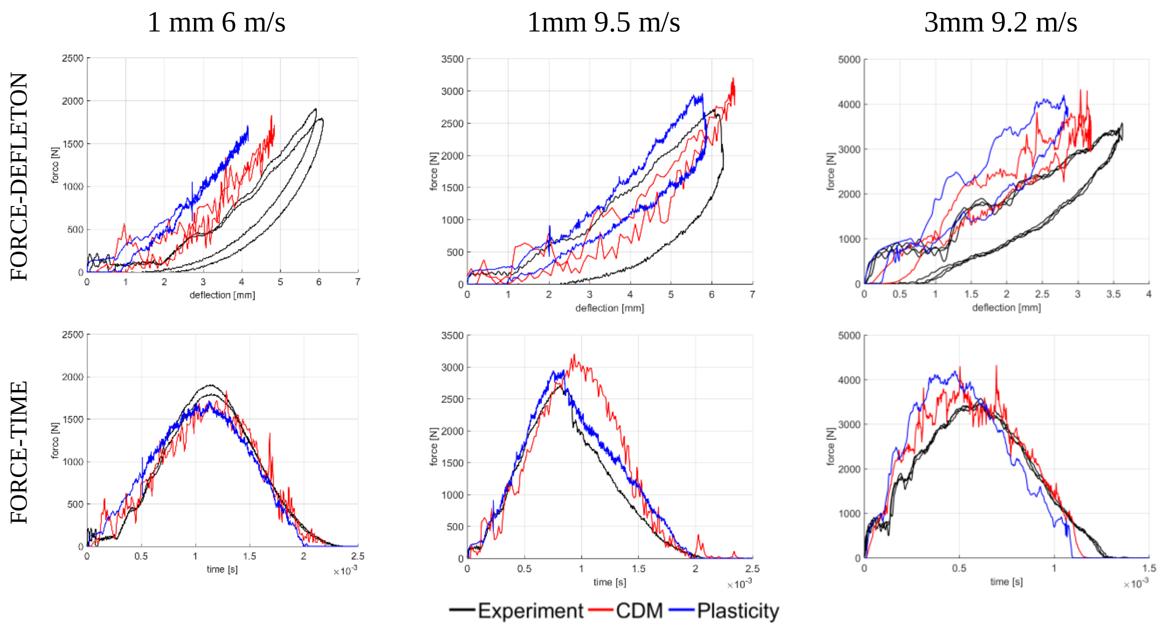
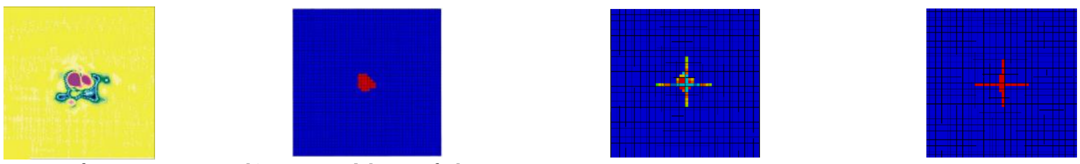


Figure 5. Comparison of force and deflection prediction with experiment



a) C-scan of experiment *b) CDM model: out-of-plane shear damage* *c) Plasticity model: plastic strain* *d) Plasticity model: fibre damage*

Figure 6. Damage observed in the numerical model (1mm, 9.5 m/s)

5. Conclusion

The CDM model prediction for the single element models was very accurate, while the plasticity model did not perform well on this scale. Both constitutive models were able to numerically represent the observed material behaviour observed in the impact bending experiments. In particular, the characteristic non-linear deformation during the initial stage of the interaction was captured well by both constitutive models. Subsequent fibre failure was also predicted by both models. The plasticity model has the advantage of a very straight forward calibration with significantly less parameters. The CDM model can be better fitted to experimental data and hence gives much more accurate results, but requires a large number of experimental data for calibration.

Future work will include an improvement of the unit cell of the plasticity model. Including through thickness binder yarns into the unit cell using the method of cells, for example, should allow for a much better prediction by the plasticity model.

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