Comparison of three different ductile damage models for deep drawing simulation of high-strength steels

B-A Behrens¹, D Rosenbusch¹, H Wester¹ and P Althaus¹*

¹ Institute of Forming Technology and Machines (IFUM), Leibniz Universität Hannover, An der Universität 2, 30823 Garbsen, Germany
* althaus@ifum.uni-hannover.de

Abstract. High-strength steels are increasingly used in deep drawing for automotive parts due to their improved strength properties. To increase the formability and thus extend the process limits, a deep drawing process with additional force transmission has been developed. For a numerical optimisation of the considered process, an exact modelling of the failure behaviour is essential. The forming limit curve (FLC) is widely used to predict the onset of necking in sheet metal forming. However, the validity of the FLC is limited to the case of linear strain paths. Therefore, the scope of past investigations has been on failure modelling depending on the stress state. This article presents the experimental-numerical characterisation of the failure behaviour of the high-strength steels HCT600X and HX340LAD. Tensile tests with butterfly specimens were carried out under varying stress states and simulated with ABAQUS to parametrise the stress-based models Johnson-Cook (JC), Modified Mohr-Coulomb (MMC) and DF2016. An additional experiment was carried out to evaluate the models in terms of predicted outcome accuracies by comparing the onset of fracture in the simulation with the experimental findings. In future investigations, the improved damage modelling will be applied in a deep drawing simulation with additional force transmission to optimise the process design.

1. Introduction
Due to the ongoing demand for reduction of CO₂ emissions, lightweight construction is still one of the main research topics in the automotive sector [1]. A reduced overall vehicle mass can be achieved by the application of high-strength steels. The improved strength properties qualifies them for structural components with low weights [2]. To exploit the full potential of these steels, existing production processes must be continuously improved to increase productivity, material utilisation, energy balance and dimensional accuracy of the final parts.

Deep drawing of sheet metals is mainly used for the forming of car chassis parts. To expand the forming limits, a deep drawing process with an additional force transmission has been developed [3]. This technology is based on the local introduction of an additional force in the bottom of the part during forming, which initiates a pressure superposition in critical areas for fracture. By delaying the initiation of necking, the process window is extended, which was proven in experimental tests [4]. A useful tool to determine the extended process window for different materials is the finite elements (FE) method, which enables a process investigation without cost- and time-consuming trial and error test. However, for a numerical investigation, a precise modelling of the failure behaviour is important to detect fractures during forming. The FLC, which describes the forming limit as a function of major and minor strains, is a standard tool for the prediction of necking in sheet metals. Nevertheless, the range of validity is limited to linear strain paths. Necking that occurs during deep drawing in the bottom radius of the part...
could not be reliably predicted with the FLC [5]. Therefore, a better approach is to describe the failure
behaviour by means of a stress-based damage model to improve the prediction accuracy.

In this work, three ductile damage criteria Johnson-Cook (JC), Modified Mohr-Coulomb (MMC) and DF2016 are applied to predict fracture for two high-strengths steels HCT600X and HX340LAD. Tensile test with butterfly specimen are carried out and simulated in ABAQUS to parametrise the coefficients of the criteria. The aim is to improve the damage modelling for a later application in the simulation of deep drawing with additional force transmission. The overall goal is to improve the process design and achieve a reduction of costs by avoiding time- and cost-consuming trial-and-error tests.

2. Stress based damage modelling

Modelling of the damage behaviour of ductile metals has been the focus of a wide range of researches in the last decades. A common approach is the use of an uncoupled fracture criterion, which is usually defined by an empirical formula. Since the multi-axial stress state has a great influence on the damage behaviour, current models take the stress triaxiality \( \eta \) into account, which is defined as the ratio of mean normal stress \( \sigma_m \) to the von Mises equivalent stress \( \bar{\sigma} \) and the three principal stresses \( \sigma_1, \sigma_2 \) and \( \sigma_3 \):

\[
\eta = \frac{\sigma_m}{\bar{\sigma}} = \frac{1}{3}(\sigma_1 + \sigma_2 + \sigma_3) \quad \frac{1}{\sqrt{1/2 \left[ (\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 \right]}}
\]

(1)

Johnson and Cook published a criterion that calculates the equivalent plastic strain at fracture \( \varepsilon^f_{pl} \) as a function of stress triaxiality \( \eta \), dimensionless plastic strain rate \( \dot{\varepsilon}^* \) and homologous temperature \( T^* \) [6]:

\[
\varepsilon^f_{pl} = \left[D_1 + D_2 \exp(D_3 \eta) \right] \left[1 + D_4 \ln \dot{\varepsilon}^* \right] \left[1 + D_5 T^* \right]
\]

(2)

\( T^* \) and \( \dot{\varepsilon}^* \) are defined by equation 3, where \( T_m \) is the melting temperature and \( \dot{\varepsilon}_{ref} \) and \( T_{ref} \) are the reference strain rate and the reference deformation temperature, respectively.

\[
\dot{\varepsilon}^* = \frac{\dot{\varepsilon}}{\dot{\varepsilon}_{ref}} \quad T^* = \frac{T - T_{ref}}{T_m - T_{ref}}
\]

(3)

Recent studies show that the JC criterion is still widely used today, e.g. for the simulation of blanking [7] or friction drilling [8]. However, it was found that the third stress invariant \( \xi \), defined by equation (4), also has a great influence of the materials fracture characteristic.

\[
\xi = \frac{27 (\sigma_1 - \sigma_m) (\sigma_2 - \sigma_m) (\sigma_3 - \sigma_m)}{2 \sqrt{(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2}}
\]

(4)

Therefore, current models consider the third stress invariant by including the Lode angle \( \theta \) or the normalised Lode angle \( \bar{\theta} \) defined by equation (5) in the calculation [9].

\[
\theta = 1/3 \arccos(\xi) \quad \bar{\theta} = 1 - \frac{6\theta}{\pi}
\]

(5)

Bai and Wierzbicki extended the Mohr-Coulomb criterion for ductile metals, which is widely known as MMC criterion [10]:

\[
\varepsilon^f_{pl} = \frac{A}{c_2} \left[ \frac{6}{c_1} + \sqrt{\frac{3}{2} (c_{16}^2 - c_{1b}^2)} (\sec (\frac{\bar{\theta} \pi}{6}) - 1) \right] \left[ 1 + \frac{c_1^2}{3} \cos \left( \frac{\bar{\theta} \pi}{6} \right) + c_1 \left( \eta + 1 \sin \left( \frac{\bar{\theta} \pi}{6} \right) \right) \right]^{-1/n}
\]

(6)

The MMC criterion has been successfully applied for various materials, e.g. aluminium alloys [11], advanced high strengths steels [12] and magnesium alloys [13]. Lou et al. published another ductile damage criterion, called DF2016, which is defined by the following equation [14]:

\[
\left[ \frac{2}{\sqrt{L^2 + \lambda^2}} \right]^c_1 \left( \frac{f(\eta, L, C)}{C_{16}^2 - C_{1b}^2} \right)^{c_2} \varepsilon^f_{pl} = C_3 \quad \text{with} \quad f(\eta, L, C) = \eta + C_4 \frac{3(3-L)}{3(3^L/2+3} + C
\]

(7)
$L$ is the Lode parameter, which is defined as follows:

$$L = \frac{2\sigma_2 - \sigma_1 - \sigma_3}{\sigma_1 - \sigma_3}$$  \hspace{1cm} (8)

The DF2016 criterion can be transformed into the space of $(\varepsilon, \eta, \overline{\theta})$ by the following relation:

$$\tan(\theta) = \frac{\sqrt{3}(L + 1)}{3 - L}$$  \hspace{1cm} (9)

Due to their simple formulation, uncoupled damage criteria offer a simple implementation in existing FE codes. The parametrisation of the criteria usually requires an extensive number of experiments to ensure the consideration of sufficient stress states. Therefore, a special tool system will be used, which enables the induction of different stress states in a butterfly specimen.

3. Test procedure

For the tensile tests, a butterfly specimen was used following the geometry of Dunand and Mohr [15], which was geometrically modified so that the fracture appears more clearly in the centre of the specimen due to a stronger strain localisation [16]. Figure 1 (a) shows the geometry of the butterfly specimen.

![Figure 1. Tool setup for the tensile tests (a), geometry of the butterfly specimen (b), centre of the specimen before (c) and after the tensile test (43.5°) with visible fracture (d)](image)

The specimens were cut by water jet cutting from sheet metals of the high-strength steels HCT600X and HX340LAD with a sheet thickness of 1.0 mm. On both sides the surfaces in the centre of the specimen were machined away with 25% of the initial sheet thickness. It was ensured that the surface roughness remained under the tolerances of $Ra < 0.8$ and $Rz < 6.3$ to avoid local effects on the fracture initiation due to roughness peaks. For the tensile tests, a special tool system in a single-axis tensile testing machine from Zwick Roell was used, which is shown in figure 1 (b). The tool consists of two
rotatable specimen holders as well as an upper and a lower fixture to fasten the holders. Due to the circular arrangement of the mounting holes, the specimen holders can be rotated in steps of 15.5°. This allows the testing under varying angles $\alpha$ of force application, which results in different stress states in the centre of the specimen ranging from a shear compression state to uniaxial tension. During the tests, the holders are fixed against rotation. The test were carried out quasi-statically at a constant speed of 0.02 mm/s until the onset of fracture appeared in the centre of the specimen. The optical measuring system Aramis from GOM was used to directly measure the displacements in x- and y-direction of the specimen by Digital Image Correlation (DIC). For this purpose, a stochastic pattern was applied on the specimen surface before testing. Furthermore, the optical measuring system was used to manually determine the onset of fracture, which was assumed when the first visible crack appeared on the surface of the specimen. The tests were performed at room temperature under the angles $\alpha$ of -3°, 12.5°, 28°, 43.5°, 59°, 74.5° and 90°. Each experiment was repeated five times. Subsequently, the experiments were evaluated in Aramis 2021 to obtain the displacements of the specimen in x- and y-direction. Therefore, two points were defined and the change of distance in x and y was measured, which are exemplary shown in figure 1 (c) and (d). Afterwards the average value of the five repetitions were calculated. These values were used as boundary conditions in the following numerical simulations.

4. FE modelling

Numerical simulations of the tensile test were carried out in ABAQUS to determine the equivalent plastic strain, stress triaxiality and normalised Lode angle. Therefore, elastic-plastic material models with the yield criteria according to Hill were defined [17]. The characterisation and material model validation of the considered materials were performed in previous study [3]. For this reason, tensile tests were carried out at room temperature according to DIN EN ISO 10002 with the specimens aligned at 0°, 45° and 90° to the rolling direction. Additionally, hydraulic bulge tests were carried out to extend the flow curves and thus enhance the extrapolation accuracy. A combined approach of Swift and Hocket-Sherby was used for HCT600X and the approach according to Gosh was chosen for HX340LAD, because they provided the best approximation to the experimental data. The resulting flow curves and the yield locus of the considered materials are shown in Figure 2.

![Figure 2](image_url)

**Figure 2.** Flow curves and yield surfaces for HCT600X (a, c) [3] and HX340LAD (b, d) and the simulation model with the applied boundary conditions for $\alpha = 43.5°$ (e)
Since only the displacements of the specimen centre were measured during the experiments, only this section was modelled for the simulation. The centre was arranged in the x-y-plane analogous to the position in figure 1 (c). One-half of the geometry was modelled and a symmetry plane in z-direction was defined. A mesh size of 0.1 mm was applied. The measured displacements from the tensile tests were specified as the boundary conditions in x- and y-direction. The simulation model as well as the applied boundary conditions are shown in figure 2 (e). After simulation, the equivalent plastic strain and the three principal stresses in the centre of the specimen were evaluated to calculate the stress triaxiality and the normalised Lode angle. Therefore, the element with the maximum equivalent plastic strain was selected for evaluation. This element was chosen because it was always located in the centre of the investigation area of the specimen, which represented a good agreement to the position of the fracture in the experiments. Since the stress triaxiality and the Lode angle vary during the deformation of the specimen, they are normalized by the equivalent plastic strain up to the onset of fracture based on the following equation.

\[ \eta_{\text{norm}} = \frac{1}{\delta_{\text{pl}}} \int_0^{\delta_{\text{pl}}} \eta \, d\varepsilon_{\text{pl}} \quad \text{and} \quad \bar{\gamma}_{\text{norm}} = \frac{1}{\delta_{\text{pl}}} \int_0^{\delta_{\text{pl}}} \bar{\gamma} \, d\varepsilon_{\text{pl}} \]  

After determination of the parameters, the model coefficients of the damage criteria JC, MMC and DF2016 were calibrated. A and n of the MMC criterion were determined by fitting the flow curve to the Swift hardening model. For simplicity, the parameters \( c_0^x \) and \( c_0^y \) were left at their default value of one analogous to [10]. The remaining parameters were fitted to the experimental data by reducing the sum of the least squares between experimental-numerical plastic strains and predicted plastic strains by the criteria. The results are given in table 1.

**Table 1. Coefficients of the ductile fracture criteria and the average error of the fit**

| Material  | JC  | MMC | DF2016 |
|-----------|-----|-----|--------|
| HCT600    |     |     |        |
| \( D_1 \) | 0.370 | 1018.6 | 1.949  |
| \( D_2 \) | 0.264 | 0.143 | 5.521  |
| \( D_3 \) | 8.992 | 0.166 | 5.482  |
| Sum of least squares | 0.03065 | 560.033 | 0.01937 |
| HX340     |     |     |        |
| \( A \)   | 713.0 | 0.165 | 3.115  |
| \( n \)   | 0.208 | 0.208 | 1.821  |
| \( c_1 \) | 401.934 | 6.918 | 0.522  |
| \( c_2 \) | 0.02452 | 6.918 | 0.479  |
| \( C \)   | 0.06465 | 6.918 | 0.522  |
| \( C_1 \) | 0.02580 | 6.918 | 0.479  |
| \( C_2 \) | 0.02580 | 6.918 | 0.479  |
| \( C_3 \) | 0.02580 | 6.918 | 0.479  |
| \( C_4 \) | 0.02580 | 6.918 | 0.479  |

The force application angle of 43.5° was not used for the parametrisation as this experiment will be used subsequently to validate the fracture criteria. While the criterion DF2016 achieved the best fit for HCT600X, the JC criterion showed the least sum of least squares for HX340LAD. In figure 3, the eq. plastic fracture strain calculated by the fracture criteria are shown as a function of stress triaxiality in the plane stress state.

**Figure 3.** Equivalent plastic fracture strain as a function of stress triaxiality in the plane stress state for HCT600X (a) and HX340LAD (b)
While MMC and DF2016 show the characteristic peaks of the plastic fracture strain at a stress triaxiality of 0.33 and 0.66, the JC criterion assumes a falling slope of the fracture strain with an increasing stress triaxiality. The highest plastic fracture strains were determined at the angle of -3°, where a pressure superimposed state is present. The criteria assume a strongly increasing plastic strain when the stress triaxiality approaches the section of uniaxial pressure, which is in accordance to the existence of a cut off value at $\eta = -0.33$, which has been revealed by Bao et al. [18].

5. Verification of the ductile damage criteria

As mentioned previously, the test with the force application angle of 43.5° was not used to parametrise the fracture criteria. This experiment was used to validate and compare the fracture criteria in terms of prediction accuracy. For this purpose, the damage models were implemented in ABAQUS in the form of $\epsilon(\eta, \bar{\Theta})$. The specimen was arranged at the angle of 43.5° and a multiple of the x- as well as y-displacement from the experiments were applied as boundary conditions. For the validation, the resulting displacement of the specimen at the onset of fracture was compared to the experimental findings. In the simulation, the fracture occurred when the output variable $DUCTCRT$ reached the value of one. Figure 4 shows the displacements in x- and y-direction of the experiments as well as the simulations. The error bars of the experiments show the 95 % confidence interval of the repetitions.

In all simulations, the onset of fracture occurred earlier than in the experiments. The best approximation for HCT600X was obtained with the MMC criterion with a deviation of 16.18 % from the mean value of the five experimental repetitions. A similar accuracy was achieved by the DF2016 criterion with a deviation of 19.23 %. Both models are within the 95 % confidence interval of the experiments. The largest deviation was reached by the JC criterion with 33.49 %. For HX340LAD the DF2016 criterion achieved the best approximation with a deviation of 4.9 %. MMC and JC showed deviations of 11.76 % and 16.52 %, respectively. It must be noted that the precise determination of the onset of fracture was difficult in the experiments, which can be one reason for the deviations.

In figure 5 the equivalent plastic strain at the onset of fracture measured in the experiments is compared to the simulation results. Since the best agreements are achieved by MMC for HCT600X and DF2016 for HX340LAD, only the plastic strains at fracture according to these criteria are shown. Since the onset of fracture occurs earlier in the simulation, the eq. plastic strain in the centre of the specimen is slightly lower than in the experiment, especially for HCT600X. However, the simulations show a good agreement regarding the localization of the maximal plastic strain as well as the overall deformation of the specimen geometry.
6. Conclusion and outlook

This research presents the application of three ductile fracture criteria for the prediction of failure in the two high-strength steels HCT600X and HX340LAD. Quasi-static tensile tests with butterfly specimen were carried out at room temperature under different angles of force application to induce varying stress states in the specimen centres. The tests were simulated with ABAQUS to identify the equivalent plastic strain as well as the normalised stress triaxiality and Lode angle. Subsequently, the parameters were used to calibrate the coefficients of the damage criteria JC, MMC and DF2016. Afterwards the criteria were implemented in ABAQUS and validated by simulating an additional test. In all simulations, the onset of fracture occurred earlier than in the experiment. The best agreements were achieved by the criteria MMC for HCT600X and DF2016 for HX340LAD, which shows that the models perform differently depending on the material considered. The JC criterion showed the greatest deviations to the experimental findings. This is attributed to the fact that the influence of the Lode angle is neglected and a falling course of the fracture strain is assumed with an increasing stress triaxiality.

In future investigations, the criterion MMC for HCT600X and DF2016 for HX340LAD will be applied in the simulation of a deep drawing process with additional force transmission. Due to the improved fracture characterisation, the process limits can be determined more precisely without cost- and time-consuming trial and error test. This will improve the process design and promotes the forming of high quality components.

7. Acknowledgement

The results presented were obtained in the project “Extension of the forming limits during deep drawing by additional force transmission” – 212270168. The authors thank the German Research Foundation (Deutsche Forschungsgemeinschaft, DFG) for their financial support.

References

[1] Tisza M and Czinege I 2018 Comparative study of the application of steels and aluminium in lightweight production of automotive parts Int. J. Lightweight Mater. Manuf. 1 (4) pp 229-238
[2] Schmitt J-H and Iung T 2018 New developments of advanced high-strength steels for automotive applications C. R. Acad. Sci. 19 (8) pp 641-656
[3] Behrens B-A, Bonk C, Grbic N and Vucetic M 2017 Numerical analysis of a deep drawing process with additional force transmission for an extension of the process limits IOP Conf. Ser.: Mater. Sci. Eng. 179 (1) 012006
[4] Behrens B-A, Bouguecha A, Bonk C, Grbic N and Vucetic M 2017 Validation of the FEA of a deep drawing process with additional force transmission AIP Conf Proc 1896 080024
[5] Behrens B-A, Bouguecha A, Bonk C, Rosenbusch D, Grbic N and Vucetic M 2017 Influence of the determination of FLC’s and FLSC’s and their application for deep drawing process with additional force transmission Proceedings of 5th International Conference on Advanced
Manuscript Engineering and Technologies pp 405-417

[6] Johnson G R and Cook W H 1985 Fracture characteristics of three metals subjected to various strains, strain rates, temperatures and pressures Eng. Fract. Mech. 21 (1) pp 31-48

[7] Behrens B-A, Bouguecha A, Vucetic M, Krimm R, Hasselbusch T and Bonk C 2014 Numerical and Experimental Determination of Cut-edge after Blanking of Thin Steel Sheet of DP1000 within Use of Stress based Damage Model Procedia Eng. 81 pp 1096-1101

[8] Behrens B-A, Dröder K, Hürkamp A, Droß M, Wester H and Stockburger E 2021 Finite Element and Finite Volume Modelling of Friction Drilling HSLA Steel under Experimental Comparison Materials 14 p 5997

[9] Cao T S 2017 Models for ductile damage and fracture prediction in cold bulk metal forming processes: a review. Int J Mater Form 10 pp 139–171

[10] Bai Y and Wierzbicki T 2010 Application of the extended Coulomb-Mohr model to ductile fracture Int. J. Fract 161 pp 1-20

[11] Beese A M, Luo M, Li Y, Bai Y and Wierzbicki T 2010 Partially coupled anisotropic fracture model for aluminum sheets Eng. Fract. Mech. 77 (7) pp 1128-1152

[12] Li S, He J, Gu B, Zeng D, Xia Z C, Zhao Y, Lin Z 2018 Anisotropic fracture of advanced high strength steel sheets: Experiment and theory Int. J. Plast. 103 pp 95-118

[13] Lee J-Y, Steglich D, Lee M-G 2018 Fracture prediction based on a two-surface plasticity law for the anisotropic magnesium alloys AZ31 and ZE10 Int. J. Plast. 105 pp 1-23

[14] Lou Y, Chen L, Clausmeyer T, Tekkaya A E and Yoon J W 2017 Modeling of ductile fracture from shear to balanced biaxial tension for sheet metals Int J Solids Struct 112 pp 169-184

[15] Dunand M and Mohr D 2011 Optimized butterfly specimen for the fracture testing of sheet materials under combined normal and shear loading Eng Fracture Mech 78 pp 2919-2934

[16] Peshekhodov I, Jiang S, Vucetic M, Bouguecha A and Behrens B-A 2016 Experimental numerical evaluation of a new butterfly specimen for fracture characterisation of AHSS in a wide range of stress states IOP Conf Ser: Mat Sci and Eng 159 p 12015

[17] Hill R 1948 A theory of the yielding and plastic flow of anisotropic metals Proc. R. Soc. Lond. 193 pp 281-297.

[18] Bao Y B and Wierzbicki T 2005 On the cut-off value of negative triaxiality for fracture Eng. Fract. Mech. 72 (7) pp 1049-1069