Influence Of The Plastic Material Behaviour On The Prediction Of Forming Limits

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Abstract. Prediction of the onset of necking is of large importance in reliability of forming simulation in present automotive industry. Advanced material models require accurate descriptions of the plastic material behaviour including the effect of strain rate [1, 3]. The usual approach for identifying the forming limits in industry is the comparison of a calculated strain map (major against minor strain) with a measured forming limit curve. This approach does not take into account the influence of strain path changes. Prediction of forming limit curves [4] with classical material models can already demonstrate that the forming limits are influenced by this strain path change effect. Including the effect of strain rate on the plastic material behaviour has a strong influence in prediction of onset of instability [2]. Neglecting this effect leads to underestimation of forming capacity of the material in stretch forming parts in particular. The shape of the yield locus [1, 2] will influence the predicted forming limit curves in the region from plane strain to bi-axial. Damage controlled failure will become more important using (advanced) high strength steels. This will affect the stress strain curve at high deformation grades. The work hardening is not only controlled by dislocation interaction, but also by void growth and possible presence of micro-cracks at the interface between the hard en soft phases.

Keywords: Plastic instability, Forming limits, Material modeling

INTRODUCTION

Prediction of the onset of necking in sheet materials is a powerful tool to identify the forming limits. Usually, a measured FLC (forming limit curve) is applied in combination with a major against minor strain plot calculated with an FEM-code. With this method critical places on the formed part can be identified prior to tooling and parts can be redesigned if this is necessary. The measured FLC is a too limited tool for this necking prediction, because it is only valid for the strain paths at which the tests were carried out. Improvement will be expected using a calculation method that takes into account this change of strain path, even when the material model is not adapted completely for this effect. The use of more advanced material models including the effect of strain rate, a more advanced yield criterion and the influence of damage is proposed here.

As a first step these features are implemented in an existing Marciniak Kuczinsky FLC model [4]. The plasticity tests for derivation of the plastic material behaviour are used also for determination of damage parameters. The advantage of applying these more advanced features in material modelling is demonstrated in this work. Two steel types were chosen for this analysis:
• good formable IF steel (DC06 and DX56DZ)
• dual-phase steel (DP600).

PLASTIC MATERIAL BEHAVIOUR

The Strain Hardening Model

In recent work [7, 8] models have been described for DP-materials, that includes the effect of a hard
martensite phase in a soft ferrite matrix. These models provide us insight on how hardening behaviour can be influenced via microstructural parameters. For the description of the hardening curves in instability analysis, the extended Bergström model of equation (1) is used:

\[
\sigma_f = \sigma_0 + \Delta \sigma_m \cdot \left[ \beta \cdot (\varepsilon + \varepsilon_0) + \left( 1 - e^{-\Delta (\varepsilon + \varepsilon_0)} \right)^m \right] + \sigma_0' \cdot \left[ 1 + \frac{k \cdot T}{\Delta G_0} \cdot \ln \left( \frac{\varepsilon}{\varepsilon_0} \right) \right]^{m'}
\]

(1)

\(\sigma_0 = \) back stress at zero dislocation density
\(\Delta \sigma_m = \) stress increase parameter for strain hardening
\(\beta = \) strain hardening parameter for large strain behaviour
\(\Omega = \) strain hardening parameter for small strain behaviour
\(\varepsilon_0 = \) pre-deformation parameter
\(n' = \) exponent for the strain hardening behaviour
\(\sigma_0^* = \) dynamic stress at zero thermal activation
\(\Delta G_0 = \) maximum activation enthalpy = 0.8 eV
\(m' = \) power for the strain rate behaviour
\(k = \) Boltzmann-constant = 8.617 \times 10^{-5} \text{ eV/K}
\(T = \) absolute temperature in (K)
\(\dot{\varepsilon}_0 = \) limit strain rate for thermally activated movement
\(= 10^8 \text{s}^{-1}\)

Yield Locus Description

For the yield criterion, the Corus-Vegter model [1] implemented in the simulation code PAM-STAMP 2G from the firm ESI is used. This yield criterion is based on four mechanical tests. A yield locus is constructed using the data of these tests (i.e. stresses and the deformation vector for each direction) by Bezier interpolation (Figure 1). Fourier series for each basic point from Figure 1 are used for the interpolation over the different angles with respect to the rolling direction. The data of the tests is given relatively to the flow stress in the rolling direction. This flow stress is obtained by the strain hardening law in the previous chapter.

DUCTILE DAMAGE DESCRIPTION

Previous work of the author on prediction of forming limits [1, 3, 8] was based purely on the plastic instability behaviour only controlled by constitutive equations for the plastic material behaviour. Concerning the amount of deformation in formable steels and the increasing use of advanced high strength steels, it is more likely ductile damage or even damage by shear occurs. Though damage is often originated in microstructural parameters as second phase particles, inclusions, grain boundaries, we will try as a first stage empirical damage criteria. A damage criterion is chosen dependent on the tri-axiality of the stress tensor and the equivalent plastic strain as proposed by Goijaerts (1999) [5]:

\[
w = B \cdot f \left( \frac{\sigma_h}{\sigma_f} \right) \cdot g(\varepsilon) \cdot \dot{\varepsilon}
\]

(2)

\(w = \) damage rate (\(\geq 0\)) controlling the weakening of the material by damage (0 \(\leq w \leq 1\))
\(\sigma_h = \) hydrostatic part of the stress tensor; \(\sigma_h/\sigma_f\) is the tri-axiality parameter
\(B = \) parameter controlling damage rate

One has to keep in mind that the damage rate \(w\) has to be larger than 0 and that the damage parameter has values ranging from 0 to 1.

In Table 1, four damage criteria are formulated based on equation (2) For the Oyane criterion, the damage rate \(w = 0\) if the tri-axiality \(\sigma_h/\sigma_f \leq -1/A\). The Rice and Tracey criterion, (3), always fulfils the condition of a positive value for the damage rate. Both the classical Oyane-criterion and the Rice and Tracey criterion do not incorporate an increasing damage rate with the plastic strain. Goijaerts [5] introduced a modification of the Oyane-criterion for the
introduction of this effect mainly for blanking processes. With a value for the exponent C = 0.63 for stainless steel, only one set of parameters is required for the description of damage in different test methods as blanking and tensile testing. For the purpose of the FLC prediction, the modified Rice and Tracey criterion in equation (6) is proposed for further analysis.

| TABLE 1: Examples of the functions \( f, g \) of equation (2) for ductile fracture criteria given in [5] |
|-----------------|-----------------|
| Criterion       | \( f\left( \frac{\sigma_h}{\sigma_f} \right) \) | \( g(\varepsilon) \) |
| Rice and Tracey (1969) (3) | \( \exp\left(A \cdot \frac{\sigma_h}{\sigma_f}\right) \) | 1 |
| Oyane (1980) (4) | \( \left(1 + A \cdot \frac{\sigma_h}{\sigma_f}\right) \) | 1 |
| Modified Oyane, (Goijaerts, 1999) (5) | \( \left(1 + A \cdot \frac{\sigma_h}{\sigma_f}\right) \varepsilon^C \) | |
| Modified Rice and Tracey (introduced here, 2007) (6) | \( \exp\left(A \cdot \frac{\sigma_h}{\sigma_f}\right) \varepsilon^C \) | |

**MATERIAL DATA**

![FIGURE 2. Basic strain hardening curves for the materials DC06 and DP600](image)

Material constants were obtained from data of the plasticity tests presented in the test from Figure 1 with the exception of the shear test. Two materials were used: an interstitial-free steel DC06 and an advanced high strength steel DP600. The hardening curves from both materials are given in Figure 2. Yield loci are given in Figure 3. In the hardening curves of Figure 2, the extrapolation was made with the help of the compression test on the sheet plane.

![FIGURE 3 Yield loci for DC06 and DP600 at 0° to RD including the optimised locus for DP600 on the measured FLC](image)

For identification of the damage behaviour, both the bulge-test and the compression test on the sheet plane are used as shown in Figure 4. Suppose that the stress for biaxial deformation is equal to \( \sigma_{bi} \) then a difference in hydrostatic stress is obtained of \( \sigma_{bi} \) as explained in Table 2.

| TABLE 2: Hydrostatic stress for the bulge-test and the compression test |
|-----------------|-----------------|-----------------|-----------------|
| Test            | \( \sigma_1 \) | \( \sigma_2 \) | \( \sigma_3 \) |
| bulgetest       | \( \sigma_{bi} \) | \( \sigma_{bi} \) | 0 | \( \frac{2}{3} \sigma_{bi} \) |
| Compression test | 0 | 0 | \(-\sigma_{bi}\) | \(-\frac{1}{3} \sigma_{bi}\) |

For DX56DZ (an interstitial free galvanized steel) and DP600 the damage evolution with strain was identified. Assuming the damage in the compression test is negligible, the damage parameter, \( w \), can be directly calculated from the ratio between the biaxial stress during bulging and the bi-axial stress during compression. From the modified Rice and Tracey criterion (6), the following relationship for the damage development is obtained under equi-biaxial conditions.

\[
w_{bi} = 1 - \frac{\sigma_{bi, bul}}{\sigma_{bi, com}} = \frac{B}{I + C} \cdot \exp\left(A \cdot \frac{\sigma_{h, bi}}{\sigma_f}\right) \cdot \varepsilon^{1+C} \tag{7}
\]
FIGURE 4. Equibiaxial stress strain curves from compression tests and bulgetests (RWTH) for derivation of damage behaviour of DX56DZ and DP600 with equivalent strain.

In Figure 5, the development of the damage parameter, \( w \), with the thickness strain is plotted. From this plot, one notices, that the damage rate increases with strain and that \( C \) has a positive value. This effect is stronger for DP600 than for DX56DZ, resulting into two different values for the exponent \( C \) for these materials: for DX56DZ, \( C = 0.5 \) and for DP600: \( C = 1 \). This result does not provide us the influence of the tri-axiality given by the constants \( B \) and \( A \). Goijaerts (1999) [5] proposes a value of \( A = 2.9 \). In the following section, an attempt will be made for the determination of these values from the measured FLC using the bi-axial results of Figure 5 as a fixed reference.

FIGURE 5. Damage evolution parameters of DX56DZ and DP600 with strain derived from compression tests and bulgetests (RWTH Aachen) [9].

FLC PREDICTIONS USING MK-THEORY INCLUDING DAMAGE

The plasticity model has been applied for FLC-calculations using the Marciniak Kuczinsky (MK)-theory [4]. This calculated FLC has been compared with measured FLC's determined at 90° with respect to the rolling direction. The plastic material model with the yield criterion at 90° and the damage behaviour of Figure 5 are used as a starting point for these analyses.

Original MK-criterion

Hill criterion (LHS)

FIGURE 6. Two types of basic damage defects in MK modelling.

The basic idea behind the MK-model is the presence of a certain defected zone over the whole width of the material (Figure 6). By imposing a strain path outside the defect and by maintaining the force equilibrium in and outside the defect, the development of instability is calculated. The calculation stops until the level strain rate inside the defect is 100 times faster than the one outside. In comparison with the classical MK-approach, this model has the following additional features:

- An initial damage defect of 0.001 (starting value of \( w \)) has been used. Usually a thickness defect is applied which can be considered as an initial amount of damage, \( w \), without the additional development with strain and tri-axiality described before.
- The possibility to impose a different instability criterion left (LHS) and right (RHS) to plane strain
  - RHS: uses a conventional MK criterion to impose an equal minor strain inside and outside the defected zone.
  - LHS: uses the Hill instability criterion, that the defected zone makes an angle where the normal strain rate has a value of zero and allowing a discontinuity in the shear in this direction. This leads to a proportional strain rates inside and outside the defected zone. It is possible to use the MK-criterion of the RHS too.
• By reformulation of the yield criterion in the strain rate space, a strain path can be imposed in an effective way including a change in strain path.
• Around the model a shell is programmed to adapt the calculated FLC to a measured FLC: by optimisation of the following parameters:
  - The initial amount of damage defect in the defected zone,
  - A biaxial pre-strain parameter for the Nakazima test (causing the minimum FLC at positive minor strains),
  - Yield loci parameters $\sigma_{ps}$, $\alpha_{ps}$ and $\sigma_{bi}$,
  - The damage parameters $A$, $B$ and $C$.

FLC’s were measured with the Nakazima method where strips of different widths are stretched over a hemispherical punch with a diameter of 75 mm. The determination of the limit strain values was made on the failed parts according to the new proposed ISO-standard 12004, (part B).

![Figure 7](image_url)

**FIGURE 7.** Comparison of the measured FLC of DC06 with three calculated FLC’s: 1. ini YL opt. dam: initial yield locus with optimised damage defect 2. ini YL no dam: initial yield locus without damage effect, using the same initial defect and bi-axial pre-strain as 1. 3. ini YL dam norate: initial yield locus with optimised damage defect without the effect of strain rate

In Figure 7, the results of calculations with the MK model are presented for DC06. The initial yield locus parameters give reliable predictions. Assuming that the damage behaviour for DC06 is the same as for DX56DZ the parameters $A$ and $B$ for damage were optimised while maintaining the biaxial result of Figure 5. For the constant $A$, a value of 2.3 is found. The optimisation of the initial damage defect and the biaxial pre-strain parameter is included. For this reason, the results of Figure 7 indicated as “ini YL opt. dam” are in very good agreement with the measured FLC. To demonstrate the effect of damage, an MK simulation is made without the influence of damage development. Most constitutive models do not take into account the influence of strain rate. Neglecting this influence has a much larger effect on FLC-prediction than leaving out the effect of damage as demonstrated in Figure 7. It can be understood easily that the major necking strain under plane strain conditions is equal to the current $n$-value of the material if we neglect the effect of strain rate and damage. From this viewpoint, it is required to use a plasticity model that includes the strain rate effect.

![Figure 8](image_url)

**FIGURE 8.** Comparison of measured FLC of DP600 with simulated FLC’s using initially measured yield loci with four calculated FLC’s: 1. opt YL dam: optimised yield locus with damage 2. ini YL dam: initial yield locus with damage, using the same initial defect and bi-axial pre-strain as 1. 3. opt YL nodam: optimised yield locus without damage, using the same initial defect and bi-axial pre-strain as 1. 4. opt YL dam norate: optimised yield locus with damage, without the influence of strain rate.

For DP600, the determination of damage parameters by adapting the predicted FLC on the measured FLC is possible, but this leads to unrealistic values for the parameters $A$ and $B$. Using these values for the constants $A$ and $B$ for prediction of the stress strain curve in hydraulic bulging leads to extreme softening by damage above strains of 0.2. This is not in agreement with the bulge test results in Figure 4. For this reason, the parameter $A$ is fixed on a value of 3 (Goijaerts, 1999) [5]. With the damage results under equi-bi-axial stress conditions in Figure 5, the parameter $B$ is determined. The adaptation of the predicted FLC on the measured one has been realised.
by varying the plane strain parameters $\sigma_{ps}$ and $\alpha_{ps}$ together with the initial damage defect and the equi-biaxial pre-strain parameter. This leads to the optimised MK calculation of Figure 8 indicated by “opt YL dam”. The optimised yield locus for this measured FLC is given in Figure 3 (indicated as “DP600 opt”) and compared to the initial yield locus (indicated as “DP600”). Using the initial yield locus leads to overestimation of the FLC under equi-biaxial conditions as shown in Figure 8. Leaving out the effect of damage results in higher values of the FLC for bi-axial (RHS) and uni-axial conditions (LHS) far from plane strain conditions. Neglecting the effect of strain rate leads to much lower values of the FLC for the LHS in particular.

**CONCLUSIONS**

The material model combining the plasticity and ductile damage behaviour is able to predict the onset of necking for DC06 in an accurate way. For the DP600, the FLC could be predicted accurately with this model, if the shape of the measured yield locus is adapted.

The effect of taking into account damage is noticeable for both materials. This was expected for the DP600 but not for the IF steels (DC06 and DX56DZ). One has to realise that the strain for these IF-steels in the FLC under equi-biaxial conditions is very high. It is difficult to separate the strain hardening effect from the damage effect. These two phenomena will overlap during occurrence of necking.

The effect for taking account of strain rate effects in the FLC model is very large. It is required for good onset of necking predictions in steels.

A weak point in the MK analysis is that it only works with an initial defect. The prediction of the FLC is sensitive for this initial defect for strain rate sensitive materials as steel.

The influence of strain path is not included in the current modelling of the strain hardening behaviour. This has to be studied in future research on this subject.

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**REFERENCES**

6. Y. An, H. Vegter, Experimental Study of the Friction Behaviour in the compression test, IIDDRG Working Groups meetings, 1999

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