Journal of Physics: Conference Series

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IOP Conf. Series: Journal of Physics: Conf. Series **1063** (2018) 012062 doi:10.1088/1742-6596/1063/1/012062

Phenomenological model for prediction of localised necking in multi-step sheet metal forming processes

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Abstract. Prediction of localised necking is crucial for cost, time and material efficient production of sheet metal components. Especially in multi-step forming operations, nonlinear strain paths occur due to complex part and tool geometry as well as changing loading conditions during the forming process. Today, prediction of localised necking for such components is often based on empirical knowledge of experienced forming specialists or specialised damage models particularly related to sheet metal materials. It is well known, that calibration of such damage models can be difficult, particularly for industrial applications. The model presented in this paper is a phenomenological approach for prediction of localised necking under varying loading conditions which is easy to calibrate. For the calibration procedure only 36 uniaxial tensile tests are needed. The model calibration and failure prediction was carried out for DP600 material with sheet thickness of 0.98 mm. An experimental forming component manufactured in three forming operations was used for validation of the model presented. Mathematical setup of the model, simulation approach as well as a comparison between numerical and experimental results will be given.

1. Introduction

Many sheet metal components are manufactured in multi-step forming processes. After deep drawing the major part geometry in the first operation of the process chain, trimming, secondary drawing and restriking operations are often necessary for production of the desired part geometry. For reducing time to market, engineering and tool manufacturing costs as well as revision cycles in tool try-out, most sheet metal forming tools are designed using finite element based forming simulation. In order to keep calculation time and computing costs at an acceptable level, the models used for feasibility estimation usually work with simplifying assumptions. Forming tools are considered rigid during the forming process, forming behaviour of the blank material is modelled using shell elements and failure prediction is done using the Forming Limit Curve (FLC). For correct feasibility assumption it is necessary to have exact knowledge about onset of localised necking and beginning of rupture for a given sheet metal material. Since the 1960s the forming limit curve as proposed by Keeler et al. [1] has become an industry standard for this procedure. Meanwhile experiments for measuring critical strain values have been standardised in ISO 12004-2 [2] and are usually done using Nakajima or Marciniak-test setups. Strain measurement using digital image correlation (DIC) systems provide very accurate results of material formability. Optical strain measurement systems allow usage of advanced time dependent methods for determination of onset of localised necking in Nakajima- or Marciniak-tests [3].

Since works of Müschenborn and Sonne [4] it is well known, that the forming history influences the onset of localised necking in sheet metal forming processes and that the conventional forming limit curve as standardised in ISO12004-2 is not capable of taking these effects into account. In most sheet metal forming processes, nonlinear strain paths do occur during manufacturing of a component. Usually the nonlinearity, which can be described by the change of the strain ratio $\beta=\varphi_2/\varphi_1$ is rather small, meaning the change of β during the forming process is around or below a value of 0.02. Furthermore most nonlinear strain paths and corresponding strain values of major and minor strain do not reach critical strain values of the forming material. In these cases, the conventional forming limit curve can be applied for giving a reasonably good prediction of part formability. In some cases, nevertheless nonlinear strain paths with stronger nonlinearities and critical strain values do occur during manufacturing of the sheet metal component. In these cases formability prediction using the conventional FLC is difficult. Currently for most industrial forming processes a safety margin of about 10 to 15% is applied to the FLC determined from Nakajima- or Marciniak experiments. This safety margin, based on expert knowledge and individual experiences of the method-planning department in each company. From a scientific point of view, it is clear that applying a safety margin to tool and part design is not a satisfying option, since the safety margin accounts for the lack in precision of the currently used model.

To overcome this problem and to give better feasibility prediction in sheet metal forming simulation a new model has been developed and will be presented in this paper. The basic setup of the model as well as the calibration procedure are described in the following chapter.

2. Model setup and calibration

The model presented in this paper is purely based on experimental data from uniaxial tensile tests. For full calibration of the model 36 tensile test specimen are needed. The calibration procedure includes tensile tests in 0°, 45° and 90° to the rolling direction. To account for the effect of nonlinear strain paths on material formability, 27 pre-stretched tensile tests specimen need to be measured. Pre stretching is done using rectangular sheet metal specimens with dimensions of 350 x 25 mm. These specimen are uniaxial pre stretched up to 15%. The FLC of a given material is determined using a modified version of the model presented by Abspoel et al. in 2013[5]. For better description of the change of the shape of the FLC after pre-stretching the material, a formula for calculation of a fifth FLC point has been added to Abspoel's model. In figure 1, a comparison of experimental data and calculated FLC using the new model is given for the DP600 material without pre-stretching. In figure 2, a comparison of calculated and measured FLCs after pre-stretching is shown. For better visibility, in figure 2 only the remaining formability of the sheet material after pre-stretching is displayed.

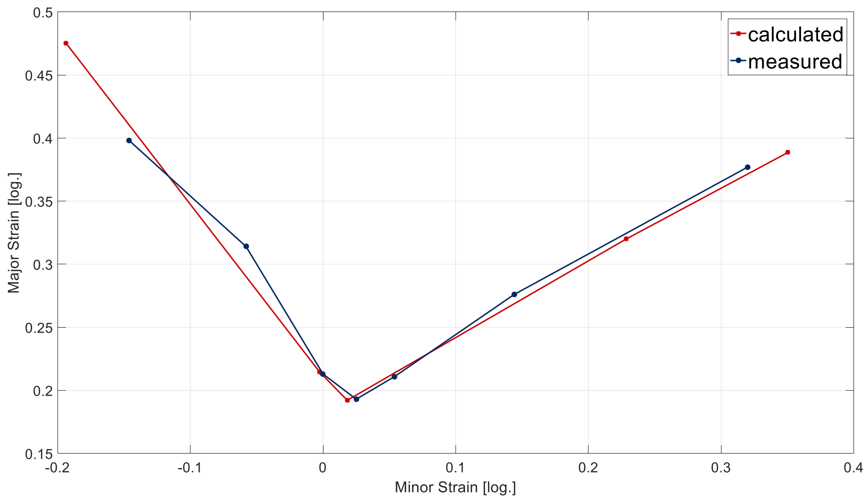

Figure 1. Calculated and experimental FLCs for DP600 sheet material

The mathematic formulation for calculating FLC data is given in table 1. Since the calculated FLCs are compared to experimental data from Nakajima-experiments, the plane-strain point has been shifted towards the biaxial direction. For comparison with test results form Marciniak-setup the biaxial shift can be compensated using the method described by Leppin et al [6].

Table 1. Mathematic model used for calculation of FLCs for arbitrary strain paths

FLC point	**Formulation**
Uniaxial point	$\varphi_{1_{UA}} = \frac{(1+0.787\mathrm{r}(\varphi)^{0.701})((0.0626\,A_{80}^{0.567}+(\mathrm{t}-1)(0.12-0.0024\,A_{80})}{\sqrt{1+(0.797r(\varphi)^{0.701})^2}}$ $\varphi_{2UA} = -\frac{(0.626A_{80}^{0.567}+(\mathrm{t}-1)(0.12-0.0024A_{80})0.797r(\varphi)^{0.701}}{\sqrt{1+(0.797r(\varphi)^{0.701})^2}}$
Intermediate uniaxial point	$\varphi_{1_{IMU}} = \frac{(-6\mathrm{n}(\varphi)+0.797\mathrm{r}(\varphi)^{0.701})((0.0626\,A_{80}^{0.567}+(\mathrm{t}-1)(0.12-0.0024\,A_{80})}{\sqrt{1+(0.797r(\varphi)^{0.701})^2}}$ $\varphi_{2IMU} = -\frac{(0.626A_{80}^{0.567}+(\mathrm{t}-1)(-0.0024A_{80})0.01r(\varphi)^{0.701}}{\sqrt{1+(0.797r(\varphi)^{0.701})^2}}$
Plane-strain point	$\varphi_{1PS} = 0.0084A_{80}+0.0017A_{80}(t-1)$ $\varphi_{2PS} = 0.018$
Intermediate biaxial point	$\varphi_{1IM} = 0.0062A_{80}+0.18+0.0027A_{80}(t-1)$ $\varphi_{2IM} = 0.75(0.0062A_{80}+0.18+0.0027A_{80}(t-1))$
Equibiaxial point	$\varphi_{1BA} = \varphi_{2BA} = 0.00215A_{80}+0.25+0.00285A_{80}t$ $\varphi_{2BA} = 0.00215A_{80}+0.25+0.00285A_{80}t$

For including the effect of nonlinear strain paths into the model, the formulas given in table 1 are modified using a so-called displacement function described in equation (1). This displacement function is used to calculate the remaining major strain value after a material has been pre stretched up to a certain amount of effective strain. The parameters A, B, C and D of equation (1) are determined using a best-fit algorithm and a polynomial interpolation between experimental data points. Parameter D is chosen to equal the major strain value of the standard material without pre-stretching. The procedure to determine this displacement function has been presented in [7]. Based on the assumption, that pre-stretching direction does not affect the major strain values of subsequent plane-strain points, the formulation of equations (1) and (2) have been determined. Parameter values for the displacement functions (equation (1)) of three extremal FLC points are given in table 2.

$$\varphi_{1\_remaining} = A\varphi_{eff}^3 + B\varphi_{eff}^2 + C\varphi_{eff} + D \tag{1}$$

$$\varphi_{1\_nl} = \varphi_1 - (A_{UA}\varphi_{eff}^3 + B_{UA}\varphi_{eff}^2 + C_{UA}\varphi_{eff} + D_{UA}) \tag{2}$$

Table 2. Parameters of calculated displacement functions

Parameter	**Uniaxial point**	**Plane-strain point**	**Biaxial point**
A	-0.0089	-28.61	-22.71
B	-1.401	-.815	4.908
C	-0.649	-1.418	-0.496
D	0.3889	0.1949	0.353

In figure 2, a comparison of the calculated displacement function and measured data is given.

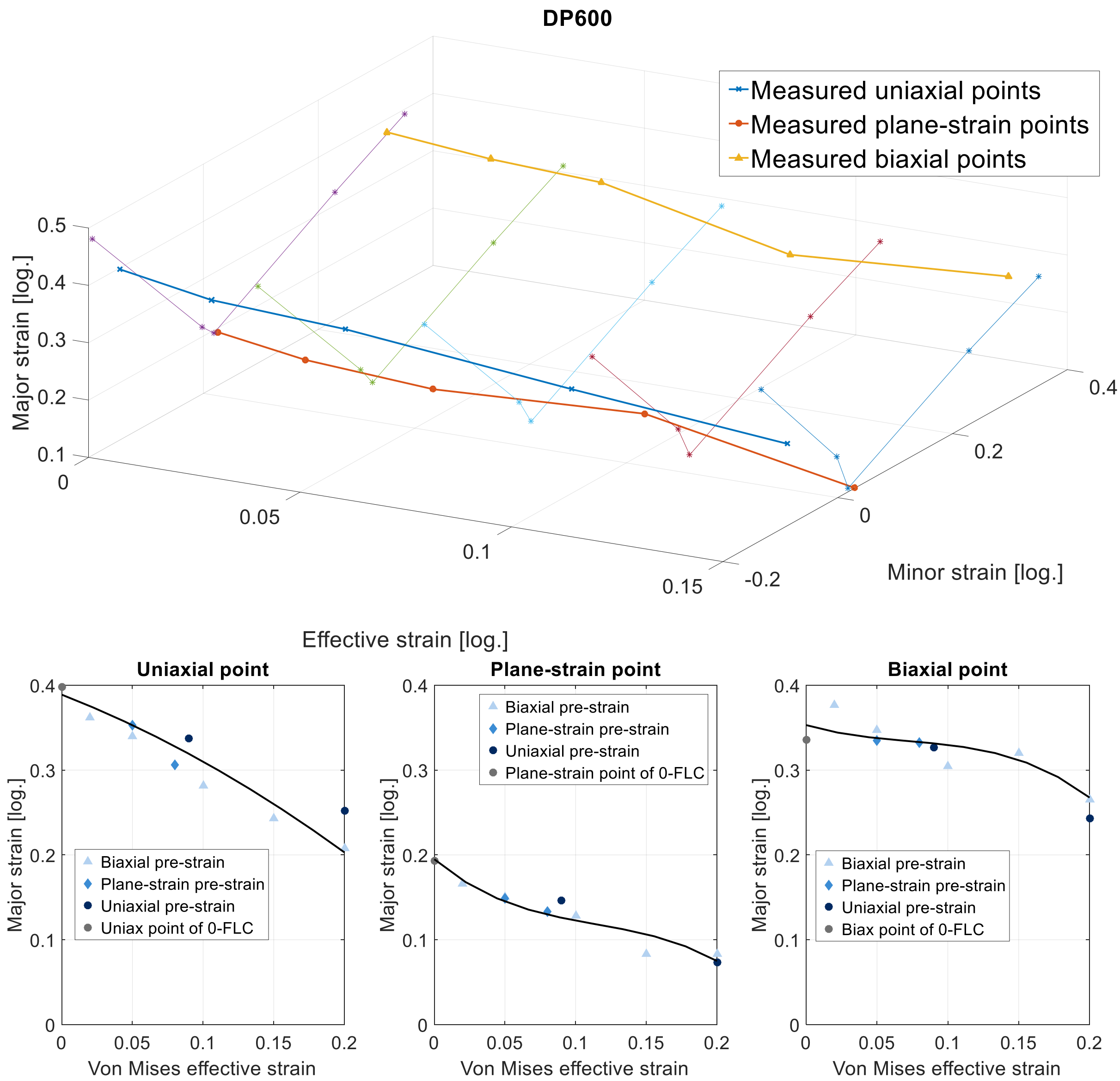

Figure 2. Displacement splines for uniaxial-, plane-strain and biaxial FLC point in comparison to experimental data

The finite element analyses of the part geometry investigated in this paper was carried out using Belytschko-Tsay element formulation with 7 integration points over sheet thickness. Barlat 89 yield criterion [8] and Hockett-Sherby yield curve description were used for simulation of material properties. The yield curve parameters were determined from tensile test data. Uniaxial tensile tests were carried out in 0°, 45° and 90° degrees to the rolling direction of the DP600 material for determination of Lankford coefficient r and hardening value n.

3. Application of presented model to a three step forming part

The model presented in section 2 is applied to an experimental part, formed in three operations. The sheet material is first pre stretched using a 340 mm circular punch. Depending on the blank geometry uniaxial, plane-strain or biaxial pre-stretching can be applied to the sheet material. By varying the drawing depth, different amounts of effective strain can be generated in the specimen. After pre-

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stretching the initial material, circular blanks with 160 mm diameter are cut and circular cups with a drawing depth of 35 mm are formed. During this secondary forming operation the strain path in the bottom area of the cup is tending towards the biaxial direction. In the third forming operation a specifically designed punch as shown in figure 3 is used to form a small dome in the middle of the specimen. Figure 4 shows an example of a critical nonlinear strain path in the specimen. Variation of drawing depths, blankholder forces and lubrication enables the possibility to create different nonlinear strain paths leading to onset of localised necking and fracture. Since the tool is designed in such a way, that DIC-Systems can be used for in situ strain measurement, strain paths as well as final strain distribution in the specimen can be measured at all times during the forming process.

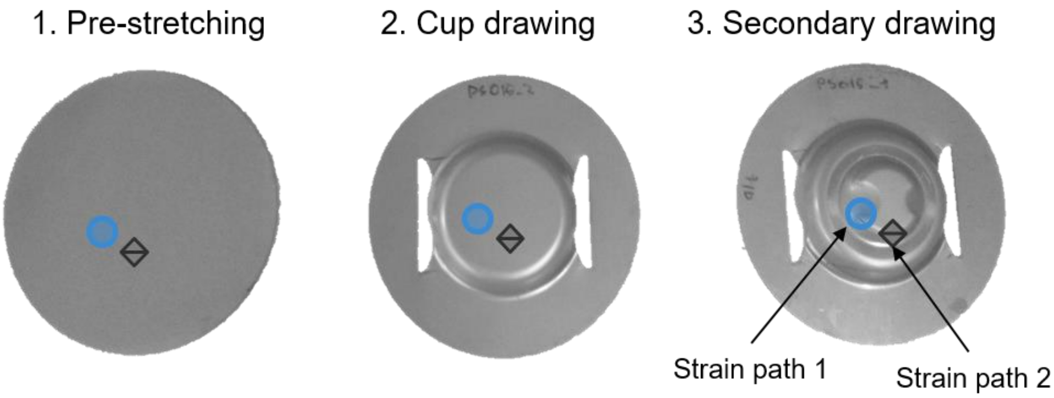

Figure 3. Test component formed in three drawing operations

The strain paths determined using gom Aramis DIC-System until the onset of localised necking are shown in figure 4. It is obvious, that in this case the prediction of localised necking using the conventional forming limit curve is not sufficient. Predicted critical drawing depth using ISO FLC is 29.0 mm. If the model presented in section 2 of this paper is used, onset of localised necking is predicted at a drawing depth of 31.0 mm. Experiments performed show a maximum drawing depth of 33.5 mm until fracture of the specimen. The onset of localised necking is approximately at a drawing depth of 32.0 mm, which is in good agreement with the prediction of the model presented in this paper.

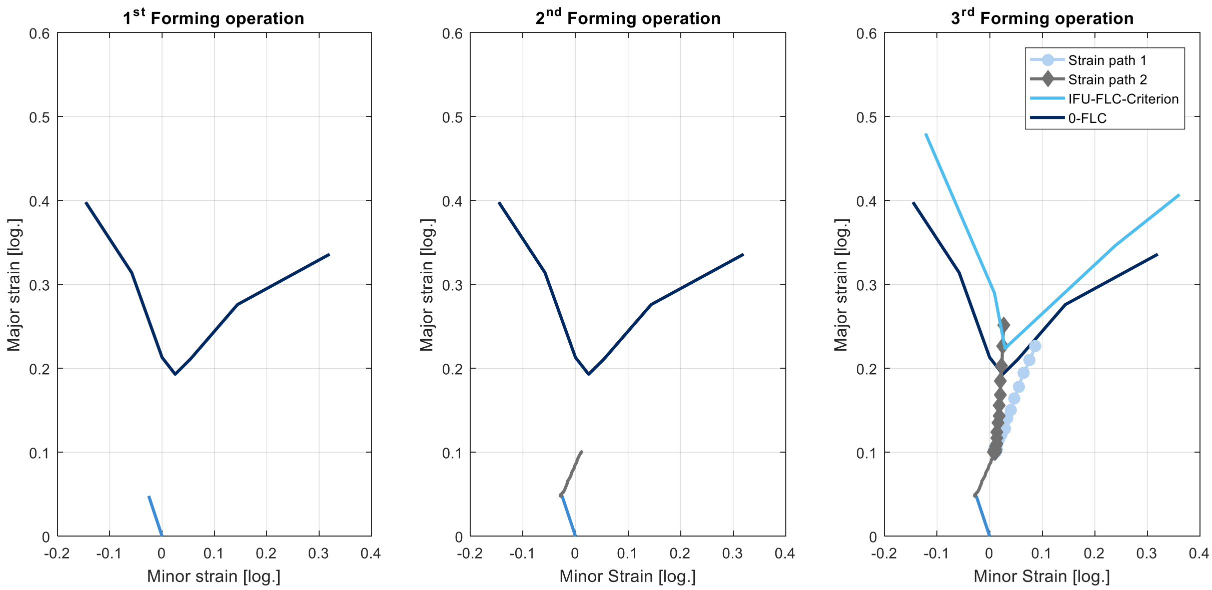

Figure 4. Measured evolution of strain path in forming specimen as shown in figure 3

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4. Conclusion and Outlook

A new criterion for the determination of onset of localised necking for nonlinear strain-paths is presented. Major advantage of the model is the calibration method, which is purely based on uniaxial tensile test data. Comparison with experimental data of pre-stretched FLC with bi-linear strain paths shows very good accuracy of the presented model.

For validation of the model presented in section 2 of this paper an experimental sheet metal component was formed in three deep- and stretch-drawing operations. Strain paths and strain distribution during the forming processes were measured using a DIC-system and compared to numerical results from forming simulation. Resulting strain paths in finite element simulation were in good agreement with experimental results. Then the new FLC criterion was used for prediction of critical drawing depth when localised necking would occur. Comparison to the results using conventional FLC the results using the new model presented in this paper are closer to the experimentally determined critical drawing depth.

The major advantage of the model presented in this paper, is the quick and easy calibration procedure for arbitrary sheet metal materials and the improved accuracy in prediction of localised necking in multi-step sheet metal forming operations. In order to further validate the criterion of this paper, it will be applied to other deep drawing components, exhibiting linear and nonlinear strain paths.

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