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The numerical investigation of the material behavior of high strength sheet materials in incremental forming

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Abstract. Springback is an inevitable phenomenon in sheet metal forming and has been found to reduce with an increasing number of forming steps. In this study the effect of incremental forming on springback is analyzed for DP780 steel. The cyclic hardening characteristics of the DP780 steel are determined by fitting the experimental moment curvature data of a cyclic pure bending test using Abaqus Standard. The change in elastic modulus with pre-strain is also considered in the material model. Using the developed material model a V- die forming process is numerically analyzed for single and multiple-step forming, and the effect on springback determined. The numerical results show that there is a reduction in springback with an increasing number of forming steps, and that this may be due to the plastic strain accumulated in the blank during the sequential loading steps in the bending region. A very good agreement has been achieved between the simulation and the experimental results. The present study seems to offer an effective approach to increase the accuracy of the springback prediction and provide a greater insight into the nature of the springback in the incremental forming process.

Keywords: roll forming, incremental forming, springback, hardening models, cyclic pure bending.

INTRODUCTION

Advanced High Strength Steel (AHSS) is indispensable for automotive applications to lightweight body structures and reduce fuel costs and emissions. Bending processes, such as V- die forming, air bending, and roll forming, are some of the forming methods to manufacture structural sections from AHSS. However these steel grades offer an inevitable challenge to control springback during the forming process due to their high strength to Young’s modulus ratio. Springback causes dimensional issues, particularly in the sub-assembly and assembly phases where welding guns cannot pull out the shape distortion. Therefore predicting the exact value of springback using numerical analysis is a necessity to allow its compensation during the tool and process design.

A survey of the literature shows that springback sometimes cannot be estimated sufficiently in bending processes using Finite Element Analysis (FEA) and two main issues have been stated [1]. The first is a change in the elastic modulus during deformation and unloading. In classic plasticity theory the elastic modulus after plastic deformation is assumed to be linear during unloading, and equal to the loading modulus. However recent investigations, such as Yoshida et al.[2], Cleveland [3] and Morestin [4], have pointed out that the stress strain relationship during unloading is non-linear. Also, the elastic modulus changes with accumulated plastic strain. Second, the hardening rule used in the FEA model also has an impact on the accuracy of the predicted springback. Isotropic [5] and linear kinematic hardening proposed by Prager [6] are not so effective, while combined isotropic-non-linear kinematic hardening model developed by Chaboche is considered suitable in predicting springback in a bending process [7, 8].

A wide variety of V die bending set-ups have been analyzed in an attempt to suppress or reduce springback [9-11]. Interestingly a recent study on the incremental V- die bending process has reported experimentally that springback decreases with increasing number of forming steps [12].

This paper investigates numerically the effect of incremental forming on springback in the V-die forming of DP780 using the finite element software package Abaqus Standard. An inverse approach of a cyclical pure bend test is used first to determine the hardening model. Next a V- die bend test with a radius of 15 mm was modeled using one, three and five forming steps to shape the final profile. The effect of the material model and of its capability of predicting the reduction in springback with the number of forming steps was investigated and compared with those from the experiments. This study will show that the reduction in springback appears to be dependent on the elastic modulus reduction due to plastic strain accumulation.
MATERIAL CHARACTERIZATION.

Tensile Test

DP780 specimens with a thickness of 2.0 mm and gauge length 50 mm were tested in a uniaxial tensile test to provide plastic hardening data for this study. The tensile test was conducted on a 100 KN Instron machine according to Australian standard AS 1391 -1991 and was carried out with three replicates using a cross - head speed of 2 mm/min and a non-contact extensometer. The yield transition was defined as the point at which the stress and strain relationship deviates from linearity and was determined by considering the reduction in the linear correlation coefficient (Figure 1). The Young’s Modulus, \( E \), was calculated from the gradient of the elastic part of the stress – strain curve measured with strain gauges that were glued on the surface of the specimen. The plastic region of the true stress – strain curve for the transverse direction is shown in Fig.2 the mechanical properties are given in table 1.

![FIGURE 1. DP780 Stress - strain curve with close up at transient region](image1.png)

![FIGURE 2. DP780 plastic stress strain curve (for both approaches)](image2.png)

### TABLE (1). Mechanical properties of DP780

<table>
<thead>
<tr>
<th>Yield strength ( \sigma_y )</th>
<th>Offset Yield strength ( \sigma_{0.2} )</th>
<th>Ultimate Tensile strength</th>
<th>Young’s Modulus</th>
</tr>
</thead>
<tbody>
<tr>
<td>352 MPa</td>
<td>580 MPa</td>
<td>916 MPa</td>
<td>191 Gpa</td>
</tr>
</tbody>
</table>

In order to take the real unloading behaviour into consideration in the present work, two major approaches were investigated based on the plastic stress – strain relationship: B1- constant Young’s modulus; and B2 – variable elastic modulus with accumulated plastic strain.

For approach B2, the change in the unloading modulus was fitted according to Yoshida’s model with six levels of plastic strain, see Fig.3 during unloading \( E_u \).

\[
E_u = E_o - (E_o - E_{sat} \cdot (1 - \exp^{-\xi P}))
\]

where \( E_o \) is initial Young’s modulus, \( E_{sat} \) is a value at the unloading modulus saturates, \( P \) is the effective plastic strain, and \( \xi \) is a material parameter equal to 85 in this case.
The numerical simulation was conducted using Abaqus Standard. There are four points to be highlighted in this work: First, the plastic stress strain relationship shown in Fig. 2 was used to define the material behavior for both approaches B1 and B2 and a Poisson’s ratio of 0.29 was chosen. Second, for approach B2, a USDFLD routine was created to consider the change in the elastic modulus with plastic strain. Third, the element type used was a 4-node reduced integration shell element (S4R). Fourth, based on the convergence test the optimum shell element size for both bending set ups was chosen to be a shell size of 5mm length and 2 mm width with 15 integration points through the thickness.

Cyclic Bending Model to Determine Material Hardening

An inverse approach of a cyclical pure bend test is used to determine the material hardening model. The experimental set up is shown in Fig. 4a, and details on the bend test can be found in Weiss[13]. The arms are fixed in bearings. The bottom arm is fixed while the top arm moves up and down generating pure bending on the blank. The reaction force and the displacement are measured. The FEA setup according to the experiments is shown in Fig.4b. The Bending arms were modeled as 3D analytical rigid surfaces while the blank was defined as a deformable body. The blank size was modeled with thickness 2 mm, width 20 mm and gauge length 50 mm as shown in Fig.4.b.

The resulting force – displacement data from the cyclic bending tests was used to calculate the moment curvature diagram[13]. With regard to hardening models, several hardening models were used with a von Mises yielding function; those are pure Isotropic, linear kinematic, and mixed hardening [7]. In the case of the combined hardening model, The Chaboche combined isotropic / non kinematic hardening model (Lemaitre and Chaboche) [14] is employed for the uniaxial loading test. The single isotropic hardening component $R$ is given by Equation 2:

$$ R = Q(1 - e^{-be^p}) $$  

(2)

Where $Q$ is the maximum value of $R$ and $b$ is the rate at which the saturation value of $Q$ is achieved. The nonlinear kinematic hardening component $\alpha$ is given by equation 3:

$$ \alpha = \frac{c}{\gamma} \left(1 - e^{-\gamma e^p}\right) $$

(3)

Where $\alpha$ is often called the back stress, $c$ is the non-kinematic hardening parameter and $\gamma$ is the rate at which $c$ decreases with plastic strain $e^p$. For the Chaboche model (mixed hardening model) the uniaxial plastic tensile data of DP780 (Figure 2), was fitted using [15]:

$$ \sigma = \sigma_p + R + \alpha $$

(4)
The hardening parameters were optimized at $Q = 50$ and $b = 3$ with using plastic stress strain data as an alternative option in Abaqus for kinematic hardening variables.

The boundary conditions for this model were a step dependent with a displacement – orientation type. Prescribed conditions were applied to the top and bottom arms with one degree of rotational freedom in Z-direction. Bottom arm is fixed in all other directions while top arm moves only in Y-direction. Simulated reaction force – displacement was used to calculate the moment – curvature diagram. The accuracy of the model was determined based on the agreement between the simulated and experimental moment – curvature diagram over the whole cycle of pure bending.

![FIGURE 4. a) Experimental cyclical bend test set-up b) Simulated cyclic pure bend test.](image)

**FEA - V- Die Bending set up**

The setup for the 15 mm V-bending is shown in Fig.5. The punch and the die were modelled as 3D rigid surfaces while the DP780 strip was modelled as a deformable body. The specimen dimensions were modelled with thickness 2.0 mm, width 20 mm and 75 mm length. In V-die bending, the specimen was forced down from the top of die surface into the die valley in a number of forming steps (one, three and five); each step included a punch reversal to allow the specimen to relax (Fig.6). When there were multiple steps the size of each step $S_{inc}$ was determined by equally splitting the total displacement $d$ for more details see [12].

![FIGURE 5. FEA - model of V-die forming test.](image)

A penalty contact condition was used with a friction coefficient of 0.2 between die surface and the blank, while between the punch and the blank ‘Frictionless Contact’ was defined. The other boundary conditions for this model were a displacement/orientation type of specified constraints and a symmetric central blank node that moves only in the vertical direction (Y-direction). 

![FIGURE 6. Schematic diagram for incremental forming steps in V-die forming.](image)
SPRINGBACK MEASUREMENT

After the punch had travelled the full stroke it was again allowed to release and springback was measured based on the coordinates (x, y) of 30 nodes on the flange of V-shaped part, where there was a regression coefficient R² = 1. The slope of the linear regression line through the data points was calculated for both loading and unloading.

\[
\text{Slope (radians)} = \frac{\sum_{n=1}^{30} (x_n - \bar{x})(y_n - \bar{y})}{\sum_{n=1}^{30} (x_n - \bar{x})^2}
\]

(5)

Where \( \bar{X} \) and \( \bar{Y} \) are the mean average of \( X \) and \( Y \) respectively. The angles in degree (\( \theta_l \) and \( \theta_u \)) were calculated for each condition as:

\[
\theta_l, \theta_u (\text{deg}) = \arctan(\text{slope}) \ast \frac{180}{\pi}
\]

(6)

where the \( \arctan(\text{slope}) \) is the inverse tangent function of the slope. The springback \( \Delta \theta \) is defined as the difference between the angles under loading and unloading Fig.7 giving

\[
\Delta \theta = \theta_l - \theta_u
\]

(7)

FIGURE 7. FEA – profile of R15mm V-shaped DP780 before and after springback.

RESULTS AND DISCUSSION

Cyclic Bending: It can be seen in Fig.8 that there is a larger deviation between the experimental moment-curvature diagram compared to the pure kinematic and isotropic models based on approach B1. Kinematic hardening underestimates the plastic behavior in cyclic bending, while pure isotropic hardening overestimates the reversed loading and reciprocal loading.

FIGURE 8. Comparison of FEA-hardening models prediction and experimental results for moment-curvature curve.
There was no effect of a changing Young’s Modulus on model accuracy, this is, no difference with regard to the predicted moment curvature diagram was observed between approaches B1 and B2. This may be due to the small overall plastic strain reached in the outer material fiber in the cyclic bending test (±1.0). For the investigation of springback in V- die bending, only the mixed hardening model was used, while the isotropic and kinematic hardening models were excluded due to the limited accuracy they have shown in the cyclic bending test.

V-Die Bending: The reduction in springback with increasing number of forming steps observed in the experiments and predicted by the numerical analysis is shown Fig. 9. A good correlation between the experimental results and the numerical model is achieved for approach B2, while approach B1 underestimates the reduction in springback with forming steps. Even though the accuracy is less approach B1 predicts a reduction in springback with increasing number of forming steps.

Even though instinctively one would expect that the reduction in the elastic modulus would increase the springback, the decrease in springback with incremental forming in V- die bending appears to be influenced by the amount of plastic strain in the bending region. Figure 10 indicates that there is a slight increase in the plastic strain in the outer fibres of the bending region with incremental forming steps (three and five) in comparison to the single step. This plastic strain is possibly influences the accumulated elastic energy, leading to a springback reduction. Even though there are no previous numerical studies in the literature on the evaluation of springback in incremental V- die bending, these results are agreed with the experimental observation and consistent with other incremental forming investigations [16, 17]. Springback can be reduced by material pre-plastifications [16].

Although both of approach B1 and B2 were capable of predicting the reliable behavior of DP780 in cyclic bending, approach B2 results in a higher accuracy of springback, regardless of the number of forming steps. This
suggests that the change in Young’s Modulus with plastic strain needs to be taken into account in the numerical model to achieve a good representation of the effect of incremental forming on springback.

SUMMARY

The present study has shown the important strong relationship between the springback in V-die bending and the material behavior in cyclic pure bending. For accurate springback prediction, the material should be simulated over the whole cycle of bending. DP780 was selected as an example of AHSS, and two material models were chosen to explain the material behavior in V-bending, where both models used a Von Mises Yield function and a mixed hardening model. First material model (B1) used a constant Young’s modulus, while the second (B2) used a varying elastic modulus. Reduction in the springback for incremental forming was found to be related to including a varying elastic modulus to the simulation model. This reduction in springback is explained by the significant amount of plastic strain in the bending area generated under the sequential loading steps. These results are confirmed with experimental observation and with the characterization of other incremental forming processes. We hope that these results can be used in the future to improve the process and tool design of incremental forming processes.

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