Finite Element Simulation of the Stretch-Forming of Aircraft Skins

H.H. Wisselink* and A.H. van den Boogaard†

* Netherlands Institute for Metals Research, P.O. Box 5008, 2600GA, Delft, The Netherlands.
† University of Twente, P.O. Box 217, 7500AE, Enschede, The Netherlands.

Abstract. In the aerospace industry stretch forming is often used to produce skin parts. During stretch forming a sheet is clamped at two sides and stretched over a die, such that the sheet gets the shape of the die. However for complex shapes it is necessary to use expensive intermediate heat-treatments in order to avoid Lüders lines and still achieve large deformations.

To optimize this process FEM simulations of this process are performed. A leading edge skin part, made of aluminium AA2024, has been chosen for a preliminary study. The material is modelled with the Vegter yield function, to account for the anisotropic behaviour of the aluminium sheet. Each annealing step is considered to reduce the work hardening completely. The strains in the part have been measured and are used for validation of the simulations. The used FEM model and the experimental results will be presented and conclusions and recommendations for future research will be given.

INTRODUCTION

The sheet stretch-forming process is used for large parts in small series [1]. In the aerospace industry this process is mainly used to produce skin parts, for example the leading edge of a wing. The basic principles of stretch forming process are shown in Figure 1. Only one die is needed to shape the part. The sheet is clamped at both sides with gripper jaws. The trajectories of these grippers, which are CNC controlled, determine the deformation of the sheet. First, the sheet is draped around the die by moving the grippers towards each other. The final shape is obtained by stretching the sheet around the die. In this way, accurately shaped products can be obtained with minimal springback. As well single as doubly curved products can be made with this process.

![FIGURE 1. Basic principle of stretch-forming.](image)

A commonly used material for aircraft skins is the heat-treatable aluminium alloy AA2024. Possible failure modes during forming of this material are necking, wrinkling, Lüders lines or orange peel. In order to avoid these failures and still achieve large deformations it is often necessary to use expensive intermediate heat-treatments, especially for complex shapes.

An important topic in industry is the production of good parts for minimal costs. Therefore the main factors in the cost price, e.g. the amount of material and the number of heat treatments needed during forming, have to be minimised for an optimal process.

Models of the stretch-forming process are needed to achieve such an optimal process control. The advantage of models is that they can be used before the tools are made, avoiding lengthy trial and error runs and modifications of the tools. “Quick and dirty” models can be used in an early stage of an order to make an estimation of the costs before offer and acceptance and to create a first design of the tools. “Thorough” models, such as models based on the finite element method (FEM) [2] are needed to gain fundamental knowledge of the stretch forming process.

The development of such a FEM model of the stretch-forming process will be described in this paper. FEM simulations and experiments have been carried out on a saddle shaped skin part. It is known from practice that these kind of products are difficult to stretch. First the material models for the aluminium sheet, which are used in the simulations are presented. Next some experiments will be shown, which will be used to validate the finite element simulations. Results of these simulations of the stretch forming process are given. Several topics from the used finite element model will be treated. Some conclusions and recommendations for further research are given in the last section.
MATERIAL MODEL

Material models for plastic deformation commonly apply a separation in a model for a yield surface and a model for the yield stress (hardening). The yield surface determines the plastic flow in a multi-axial stress state, while a hardening law determines the evolution of the yield surface. This approach is also used here.

Yield surface

For the yield surface the Vegter criterion is applied. The Vegter yield criterion [3, 4] is a very flexible criterion that defines a yield function for plane stress situations, directly based on experimental measurements on sheet material. The yield function is defined in the principal stress space. For planar anisotropic material, therefore, the yield function depends on the angle between the principal axes and the rolling direction. For a particular loading direction with respect to the rolling direction, four experiments are necessary to determine the model parameters: a pure shear test, a uniaxial tensile test, a plane strain tensile test and an equibiaxial tensile test. Between the measured stress points a Bezier curve is used to describe the yield locus.

At yielding, not only the yield stress, but also the direction of plastic strain is determined. Based on Drucker’s postulate, the normal to the yield locus has the same direction as the plastic strain rate. From the stress points and the strain rate directions, a set of Bezier curves can be constructed such that the resulting yield locus is \(C^1\) continuous.

For every part of the yield locus between two reference stress points a second order Bezier function is defined. The Bezier function is determined by the two reference stress points and the direction of the yield locus at the reference points. The intersection of the two tangents at the reference points define the hinge point. A complete yield locus is presented in Figure 2, including all reference and hinge points and the tangents. Such a constructed yield locus gives a better representation of the plastic behaviour of aluminium than models that are only based on the uniaxial yield stress and \(R\)-values [5].

To investigate the influence of the yield locus on the analysis results, simulations were performed with three different yield loci. Only planar isotropic results are presented here. Analyses were run with a Von Mises, Hill ’48 and Vegter material model. The same \(R\)-value was used for the Hill ’48 and the Vegter models, namely \(R = 0.8\). The biaxial stress ratios are given in Table 1 where \(f_{bi}\), \(f_{ps}\) and \(f_{sh}\) are the ratios between the yield stress in equibiaxial, plane strain and shear with respect to the yield stress in uniaxial tension. The data for the Vegter model have been taken from an other aluminium alloy. The same ratios have been calculated for the Von Mises function and the Hill ’48 function based on the \(R\)-value. In Figure 3 the yield loci for these 3 models are plotted. Clearly, for the Hill ’48 model, the low \(R\)-value results in a lower equibiaxial yield stress.
Hardening

The flow stress $\sigma_f$ defines the resistance to plastic deformation of a material. A typical phenomenological model is the Nadai or Swift relation:

$$\sigma_f = C(\varepsilon + \varepsilon_0)^n$$

(1)

another model is the Voce model, which often gives a better representation of the hardening curve for aluminium alloys:

$$\sigma_f = \sigma_{y0} + \Delta \sigma \left[ 1 - \exp \left( -\frac{\varepsilon}{\varepsilon_0} \right) \right]$$

(2)

Both hardening models will be used in the simulations. The curves and parameters are given in Figure 4. Although the two hardening models may seem to be equivalent in the first 15% of deformation, the difference in hardening rate at higher strains will significantly affect the predicted necking strain. The Young’s modulus is 70 GPa and the Poisson ratio is 0.3.

Heat Treatment

During the complete forming process, the sheet was annealed 5 times. Tensile tests on stretched and annealed specimens showed that after sufficient annealing time, the initial mechanical properties are almost restored [6]. In the simulation the annealing step was included by re-setting the (equivalent) plastic strain to zero, thus deleting all work hardening.

EXPERIMENTS

A number of sheets with a grid have been stretched using a manually determined trajectory of the grippers. Five intermediate heat treatments are used. To measure the strain in the final part a grid of dots with known dimensions is attached to the undeformed sheet. The dimensions of the sheet are 1130x1920x3.5mm. Due to these large dimensions only that part of the sheet which is known to deform most will be measured. The final part is shown in Figure 5 with the measured area encircled.

The PHAST$^T$M strain measurement system [7] has been used to measure the strains after stretch-forming. This system is based on 3D image processing. Digital photos are taken from different positions of the product and some beacons. The strains are calculated from the recognised gridpoints and beacons (Figure 6) using photogrammetry.

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FEM SIMULATIONS

The initial shape of the sheet and the tools is shown in Figure 7. As it is a symmetric product only one-half of the sheet has been modelled. A penalty method is used to describe the contact between the
rigid tools and the sheet. A coulomb friction coefficient of 0.1 is used. Apart from the die a cylindrical tool has been modelled, which is attached to the grippers and determines the contact conditions in the clamping area. The part of the sheet that is clamped between the gripper jaws is not modelled. A displacement boundary condition is prescribed to the new edge of the sheet.

It can be seen that the initial shape of the sheet is not flat, which agrees with the industrial practice as the sheet is slightly bend before clamping it into the machine. This initial bend is modelled by taking the initial shape of the sheet equal to a shallow cosine. This shape is equal to the shape of the buckle mode of a compressed sheet. In this example the edges of the sheet are moved only 15mm inwards. It is assumed that this bend causes no stresses and plastic deformations in the sheet. By modelling this initial sine shape it is ensured that the sheet deforms in the correct upward direction, when the sheet is draped around the die in the first part of the stretch process. This avoids numerical instability while simulating buckling of the sheet.

The used trajectory of the grippers is shown in Figure 8. It is a piecewise linear approximation of the trajectory used for the experiments. The rotations of the grippers have been neglected, as they are relatively small. The positions at which an intermediate heat treatment is given to the sheet are marked.

**RESULTS**

In Figure 9 the FLDs per stretching phase (between heat treatments) are presented, using the Vegter yield function and Nadai (left) and Voce (right) hardening model. The major strain per stretching phase is less than 0.14 and the difference between the two hardening models is very small. In the pictures FLCs are presented using the Nadai and Voce hardening model respectively, which have been calculated with a Marciniak–Kuczynski analysis [11]. According to the calculation with the Voce model, the first stretching phase is critical.

When comparing the simulations with and without heat treatments, the difference between the Nadai and Voce hardening models is more pronounced. In Figure 11 the thickness along the symmetry line is presented after all stretching phases without and with heat treatments. With heat treatments, the deformations are stable and they are almost completely kinematically determined. The shape of the yield locus and hardening function hardly influence the global strain distribution and tool forces. Without heat treatments, the Voce hardening model leads to local necking in the last stretching phase. Now, the deformations are not kinematically determined anymore and a crack
FIGURE 9. Forming Limit Diagrams per stretching phase (midplane).

FIGURE 10. Vertical Force on die with and without heat treatment (ht).

CONCLUSIONS

In stretching operations, the global strain field hardly depends on the chosen yield function and hardening model. This is attributed to the high degree of kinematic constraints. In local necking, a neck can develop independent of the constraints and at this point the predictions become highly dependent on the exact material models and parameters. A warning is in place that, although the deformations can still be correct with a bad material model, the residual stresses will often be wrong, potentially leading to a bad prediction of spring back. The delicate relation between material model and forming limits was demonstrated. In the stretching region the work hardening and the shape of the yield function both influence the FLC significantly. For this region, the relevant part of the yield locus is the relatively small part between plane strain and equi-biaxial stress states. If accurate material models are available, necking can be calculated from a FEM analysis. FEM models have the additional benefit that boundary conditions, non-proportional deformation and e.g. friction with the tools are completely included. For a correct localisation prediction, however, the accuracy of the material model is critical.
FIGURE 11. Thickness along symmetry line without (left) and with heat treatments (right).

FIGURE 12. Influence friction on thickness along symmetry line (left) and perpendicular to this line at \( y = 0 \) (right) for Vegter yield locus and Voce hardening.

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