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Finite element modeling of tube deformation during cold pilgering

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Abstract. A three-dimensional finite element model of cold pilgering of stainless steel tubes is developed in this paper. The objective is to use the model to increase the understanding of forces and deformations in the process. The focus is on the influence of vertical displacements of the roll stand and axial displacements of the mandrel and tube. Therefore, the rigid tools and the tube are supported with elastic springs. Additionally, the influences of friction coefficients in the tube/mandrel and tube/roll interfaces are examined. A sensitivity study is performed to investigate the influences of these parameters on the strain path and the roll separation force. The results show the importance of accounting for the displacements of the tube and rigid tools on the roll separation force and the accumulative plastic strain.

1 Introduction

Cold pilgering is one of two major cold forming methods for producing seamless tubes. These tubes are used in a wide range of industries where high precision is needed, e.g.: nuclear, aerospace, automotive, oil and gas. This method enables cost efficient production of tubes with tight geometric tolerances and high surface quality. Most metals are suitable for this method, even though they have different ductility, which limits the maximum possible cross-sectional area reduction. Typical area reductions for stainless steels are up to 80 percent [1].

The ingoing material is a tube preform obtained from extrusion. The preform is subjected to repeated compressions using a mandrel on the inside and roll dies pressing from the outside. In a stroke, the diameter and wall thickness of the tube are reduced according to the profiles of the mandrel and the roll dies. After the stroke, the tube is fed forward and turned. A full stroke consists of a forward and backward stroke. The possibilities for feeding and turning in the two ends of a stroke vary between different mills. These small forming steps deform the tube in many small strain increments in various directions to ensure virtually homogenous wall thicknesses and material properties.

Understanding the relation between forces and deformations during pilgering of tubes is important in order to design a process with increased productivity and quality. However, the deformation process is complicated because the material is subjected to large plastic deformation with frequent changes of strain direction. The elastic deflections of the roll stand and the mandrel, together with the response of the formed tube, are very important parameters as they influence the gap between the mandrel and the groove of the roll dies. This gap is critical as it determines the amount of deformation at each axial position of the formed tube.

In the literature, several papers have been published where the roll separation force and deformation history during pilgering have been studied using experimental and/or computational methods [2-14]. However, there are only a few studies where the influence of roll stand deflection and/or the displacement of the mandrel and the tube are included [4, 5, 6, 10]. Yet, none of these studies have investigated the influence of displacements of the tube and the rigid tools on the roll separation force and the accumulative plastic strain by use of a three-dimensional (3D) finite element (FE) model.

In the present work, a 3D mechanical FE model of cold pilgering of stainless steel tubes is used to investigate the influence of vertical displacements of the roll stand and axial displacements of the mandrel and tube. The displacements are achieved by supporting them with elastic springs in the model. Furthermore, the influence of friction in the tube/mandrel and tube/roll interfaces is investigated. A sensitivity study is performed to investigate the influence on the strain path and the roll separation force.

1.1 Background

The first numerical model of cold pilgering was developed by Siebel and Neumann in 1954 by using an analytical axisymmetric 2D approach [2]. The vertical rolling force and contact lengths were estimated. A strip rolling analogy was used without considering the oval shape of the roll grooves.
In 1975, Yoshida et al. [3] published a comprehensive experimental study in which they measured the vertical and axial rolling forces, the contact pressure and the contact length by using pressure sensors buried in the groove surface. They also measured the strains from a deforming grid printed on the preform surface and calculated the stress tensor.

Later on, in 1984, Furugen and Hayashi developed an analytical 3D model for the cold pilgering process [4]. The tube deformation was modeled by tracing the axial and tangential movements of a metal piece during the process. To include the ovalisation, the cross-section of the tube was modeled as two parts: the groove and the flange. Analytical equations were used to calculate stresses from estimated strain values. The roll separating force was also calculated and compared with experiments. This model was the first attempt to account for the elastic deformation of the roll stand as they estimated the gap between the groove and the mandrel.

Aubin et al. [5] made an important study where a number of experiments and numerical analyses were performed. They measured the roll separation force, the axial forces applied to the mandrel and the preform and the axial displacements of the preform, tube end and mandrel end. By comparing the forces and the displacements, the stiffnesses of the mandrel-mandrel rod system and the preform-carriage system were estimated and integrated in the numerical model giving axial displacements of the mandrel and tube. They stated, but did not show, that these motions are very important for the state of stress in the tube. Moreover, the roll gap during the pilgering was estimated by the deformation of an indium wire positioned between the lateral treads of the two rolls. The measured gap was related to the measured roll separation force so that the roll stand stiffness could be estimated. Additionally, the roll flattening due to the elastic deformation of the rolls and the wave in front of the compression area were included by modifying the contact length formula of Siebel and Neumann [2], using the Hitchcock formula for the flattened roll radius and a wave factor for the increased contact length. They used their results to update the analytical model by Furugen and Hayashi [4].

Harada et al. [6] employed a 2D generalised plane strain model for the simulation of cold pilgering of a Zircaloy tube. They traced the tube-fed portion during the pilgering. They included the deflection of the roll stand in the estimation of the distance between the mandrel and the rolls. They also included the flattened roll radius and the wave factor as in [5]. With this model, they investigated the influence of tool design, the flattened roll radius and the wave factor on the roll separation force. They concluded that the wave factor has a large influence but the flattened roll radius has a fairly low influence, which is not in agreement with Aubin et al. [5]. They also examined the effect of tube spring-back on the roll separation force and showed that the model is in better agreement with experiments once the spring-back is considered.

In 1996, Mulot et al. [7] presented the first 3D finite element simulation of cold pilgering. They simulated one forward stroke. The main focus of their work was to evaluate if FE analysis (FEA) is a feasible method for the modeling of the cold pilgering process. They showed that FEA can predict the stress and strain states in the flange and bottom of the groove, which other models cannot. They also showed the influence of friction, at the inner and outer tube surfaces, on the horizontal forces acting on the mandrel and the preform.

Montmitonnet et al. [8] further developed the 3D FE model in [7] and simulated three strokes. They concluded that stresses and rolling forces were almost identical in all strokes for an ideal material without plastic hardening. Work hardening was included in the model by Lodej et al. [9]. A post-processing procedure was created to decrease the calculation cost of their model. This procedure was employed to estimate the strain and stress trajectories of a material point for multiple strokes from the FE results of a single stroke.

Nakanishi et al. [10] developed a 3D FE model of the pilgering process where the rigid rollers were supported by springs in the vertical direction. Thus they included the roll stand stiffness. Moreover, the rigid mandrel and the tube were supported by springs in the axial direction to capture axial motions during pilgering. The tube elongation was measured by laser equipment in an industrial pilger mill. The elongation of the tube in the numerical model was fit to the measurements by changing the friction coefficients between the tube and the mandrel and the tube and the roll dies. The displacements of the mandrel, tube inlet, tube outlet and the elongation were calculated using the model. The elongation was in a good agreement with measured values but the tube and mandrel displacements were uncertain.

Pociecha et al. [11] used a measurement system based on the principle of photogrammetry to measure the deformation on the surface of an aluminum tube by printed grids and non-destructive testing using a calibrated ultrasonic gauge to measure thicknesses. They also developed a 3D FE model of the pilgering process. Strain distributions along the tube in a single cycle were computed and compared with measurements. Recently, the present authors developed a 3D thermo-mechanical FE model of the cold pilgering process to investigate the influence of temperature and strain rate on the roll separating force [12].

2 Model

2.1 Material

The material of the tube is subjected to multi-axial, and quasi-random non-proportional loadings during the pilgering operation. The flow stress depends on the deformation path, strain rate and temperature [12-14]. There exists a wide variety of material models used for simulating pilgering. However, there is still no material model applicable for macroscopic simulations that can describe various load paths and evolution of texture.

In the present work, the material behavior is simplified and only total accumulated plastic strain is accounted for, i.e. the classical isotropic von Mises
plasticity model is used. A high-alloy austenitic stainless steel, Alloy 28, is considered. The flow stress has been evaluated from uniaxial compression tests at room temperature and strain rate of 0.01 s⁻¹, see Fig. 1. Young’s Modulus is 195 GPa and Poisson’s ratio is 0.29.

![Flow stress curve](image)

**Figure 1.** Flow stress curve.

### 2.2 Geometry

The geometry of the 3D mechanical FE model is shown in Figures 2 and 3.

![Geometry used in the FE model](image)

**Figure 2.** Geometry used in the FE model.

![Details of roll groove in working part of roll](image)

**Figure 3.** Details of roll groove in working part of roll.

This model consists of two rolls (upper and lower), the mandrel, the steering plate and the tube preform. The rolls consist of three parts: the working, sizing and opening part. In the working part, the distance between the roll grooves and the mandrel is decreasing. In the sizing part, this distance is constant. Similarly, the mandrel has a conical shape in the working part and a constant radius in the sizing part. The length of working part is normalized to 1. In the opening part, there is no contact with the tube; instead feeding and rotation of the preform is possible. The profiles of the mandrel and roll dies are the most important design parameters to determine the quality of the final product. The virtual steering plate is only used in the model to feed forward and rotate the preform when the rolls are in the beginning and end of the stroke.

### 2.3 Boundary conditions

Figure 4 illustrates the components and the operation positions in the pilgering process. The motion of the rolls is obtained by prescribed displacements and rotations. The rolls are assumed to be rigid and supported with a spring in the vertical direction to account for the deflection of the roll stand during the operation.

![Configuration of pilgering process](image)

**Figure 4.** Configuration of pilgering process.

The mandrel is also assumed to be rigid and supported with a spring in the horizontal direction to include the elastic deflection of the mandrel rod. A third spring is inserted between the preform and the steering plate accounting for the elastic deflection of the preform. The locations of the springs are indicated in Figure 5.

![Inserted springs to mimic deflections of rigid tools](image)

**Figure 5.** Inserted springs to mimic deflections of rigid tools.

The spring stiffnesses of the mandrel rod and preform are calculated by use of Equation 1, where $E$ is the elastic modulus, $A$ is the cross-sectional area and $l$ is the length of the rod or preform.

$$k = \frac{EA}{l}$$

The distance between the mandrel chuck and the inlet position is constant during the process. Thus, only one stiffness value is calculated, 9 MN/m, for the mandrel rod. Contrary, the inlet chuck of the preform moves...
forward while feeding the preform. Therefore, two
limiting cases are considered for the preform: the
maximum and minimum lengths, where the stiffnesses
are 13 and 58 MN/m, respectively. The stiffness of the
roll stand, 6000 MN/m, is estimated by using
measurements of the roll deflection for various roll
separation forces.

The friction depends on the contact surfaces, the
lubrication and the contact conditions [15]. A simple
Coulomb friction model has been used in the present
work. The friction coefficients 0.075 and 0.030 are used
between both tube and rolls and tube and mandrel
surfaces.

2.4 Process parameters

The process parameters used in the simulations are shown
in Table 1.

Table 1. Process parameters.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Feed step (mm/stroke)</td>
<td>9</td>
</tr>
<tr>
<td>Stroke rate (rpm)</td>
<td>200</td>
</tr>
<tr>
<td>Total number of strokes</td>
<td>32</td>
</tr>
<tr>
<td>Dimensions of preform tube</td>
<td>42 x 5</td>
</tr>
<tr>
<td>Diameter x thickness (mm)</td>
<td>42 x 5</td>
</tr>
<tr>
<td>Finishing dimensions of tube</td>
<td>25.4 x 2.4</td>
</tr>
</tbody>
</table>

The feed step is the total feed of the tube, distributed
as 60% before the forward stroke and 40% before the
backward stroke.

Figure 6. Computed roll separation forces used to choose mesh
in the axial direction.

2.5 Mesh and computation

The preform was meshed with eight-node solid elements.
Mesh convergence tests were performed for a smaller
preform (100 mm). First, tests were performed with 20,
40, 60 and 80 elements in the rolling direction, whereas
three elements in the radial direction and 24 elements in
the tangential direction were used. The calculated roll
separation forces are shown in Figure 6. Numerical
oscillations in the roll separation forces were smoothed to
simplify the comparison between the different cases for
the mesh convergence test and the final model. The
number of elements in the rolling direction is important
since the contact zone affects the rolling force. Furthermore, the deformation causes the elements to
become three times longer at the end of deformation. To
keep the CPU time at a reasonable level, the mesh size of
the 60 elements case was used in the axial direction of the
final model.

Mesh sensitivity studies were also performed in the
radial and tangential directions, with 60 elements in the
rolling direction. Figure 7 shows the calculated roll
separation forces where three, four and five elements
were used in the radial direction. Three elements were
used in the radial direction of the final model.

Figure 7. Computed roll separation forces used to choose mesh
in the radial direction.

In Figure 8, roll separation forces are given for three
different numbers of elements in the tangential direction:
24, 36 and 48. No significant difference was obtained in
the tangential direction. Thus, 24 elements was used in
the tangential direction. Similar convergence trends were
observed for the axial forces acting on the mandrel and
the steering plate which determine the axial
displacements.

Figure 8. Computed roll separation forces used to choose mesh
in the tangential direction.
The model was solved using the finite element code MSC.Marc, version 2013.r1, with implicit time stepping. The nonlinear system of equations was solved by an incremental, iterative procedure using the full Newton-Raphson method. The model consists of 12960 elements eight-node brick elements for the 300 mm long preform. Adaptive time-stepping was used, where 0.145 ms was assigned for almost all time steps. The cpu time for 32 strokes was 58 h using a computer with an Intel Xeon E5-2680 v3 processor (24 cores, 3.07 GHz, 128 GB RAM).

3 Results and discussion

The results are presented in two groups. The influence of the spring stiffnesses on the tool displacement, the accumulative strain and the roll separation force are examined in the first group, Figures 9-10, 12-14. Four cases were simulated for this study. Two different stiffnesses, 9.0 and 4.5 MN/m, were used for the mandrel rod (ms). For the stiffness of the preform (ts), the values 13.6 and 58.0 MN/m were used to represent the maximum and minimum preform length, respectively. The stiffness of the roll (rs) was 6000 MN/m. The case with a rigid roll stand (rigid rs) was also investigated. For comparison the model without springs (rigid) is presented in Figures 12-14. A feed step of 9 mm per stroke was used in all cases. The case where ts = 13.6 MN/m, ms = 9.0 MN/m, rs = 6000 MN/m is used as the reference case.

The influence of the friction coefficient µ is investigated in the second group, Figures 15-18. In the first case, µ=0.075 was assigned for both contacts between roll/tube (µrt) and mandrel/tube (µmt). In the second case, 0.030 and 0.075 were assigned for µrt and µmt, respectively. For the last case, 0.075 and 0.030 were used for µrt and µmt, respectively.

Figures 9 and 10 show axial displacements of the tube inlet, relative to the ground and relative to the mandrel inlet, in the last stroke for different spring stiffnesses. In these figures, solid and dashed lines represent the forward and backward part of the last stroke, respectively. During the forward stroke, the displacement of the tube is first positive and then negative, Figure 9. The direction of the friction force depends on the direction of the velocity of the roll surface. The velocity of the roll surface depends on the distance from the groove to the roll center. This distance changes around the circumference of the roll die, Figure 11. At one point, the neutral point P, the sum of the friction forces is zero. In area A, the sum of the friction forces is in the same direction as the stroke. Contrary, in area B, the sum is in the opposite direction. During the forward stroke the tube is allowed to elongate in the same direction as the tube is pushed. Therefore, the direction of the friction force mainly determines the direction of the axial force acting on the tube.

During the backward stroke, the displacement of the tube is always negative. This is because the friction force is not the only force acting on the tube from the roll. The roll also pushes material in front of it which causes a large compressive force on the constrained tube inlet. This affects the axial force acting on the tube.

Yoshida et al. [3] measured the reaction force acting on the tube inlet during the forward and backward strokes. The displacement curves in Figure 9 are in good agreement with their measured results. The stiffness of the preform (ts) has great influence on the displacements in both Figures 9 and 10. The mandrel stiffness (ms) influences only the tube movement relative to the
mandrel movement during the backward stroke, Figure 10. The stiffness of the roll stand has limited influence on the displacements, since the results for the reference case and the case with rigid roll stand are fairly close.

Investigation of the strain history in the pilgering process is of interest for several reasons. Greater accumulative strain yields greater redundant work, which may affect the temperature in the material. Another reason is that the work hardening depend on the strain path, strain rate and temperature [13, 14]. Knowledge of the plastic strain makes it possible to estimate damage and failure during forming [5]. Figure 12 shows the accumulated equivalent plastic strain of a material point on the tube surface that is traced from the beginning to the end of the process. The results show that the use of rigid tools overestimates the plastic deformation. There is no significant difference between the four other cases.

Figure 12. Accumulated strain of a material point during the cold pilgering process.

Figures 13 and 14 show the roll separation force during the forward and backward part of the last stroke. The curves, both for the forward and backward stroke, are consistent with [3, 5, 7]. In the forward stroke, there is no significant deviation between the cases.

Figure 14 shows that the influences of spring stiffnesses on the roll separation force are more significant during the backward stroke. This is due to the larger displacements in this stroke (see Figures 9 and 10). The spring stiffnesses also affect the axial position of the maximum force because of the tube displacement relative to the mandrel (see Figure 10). This underlines the importance of mandrel and tool design.

Figure 14. Roll separation force vs roll position during the backward stroke.

Figures 15 and 16 show relative displacements of the tube inlet, for varying friction constants, on the outer ($\mu_{rt}$) and inner ($\mu_{mt}$) surfaces of the tube.

It is evident in these figures that $\mu_{mt}$ has limited influence on the tube displacement relative to the inlet. However, it has considerably large influence on the tube displacement relative to the displacement of the mandrel inlet. Contrary, $\mu_{rt}$ has an influence on both of the displacement curves.

Figure 15. Axial displacement of tube during the forward and backward stroke.

Figure 16. Axial displacement of tube during the forward and backward stroke.
In Figure 16, the direction of displacement during the forward stroke almost never changes for the case when \( \mu_r \) is greater than \( \mu_{ms} \), which does not agree with the expected position of the neutral point. The experiments in [5] suggested that \( \mu_{nt} \) is greater than \( \mu_r \) in the pilgering process. It can also be seen in this figure that the friction coefficient affects the axial position, where the direction of displacement changes, in the forward stroke.

Figure 17 shows accumulative plastic strains for the various friction cases. There are only small differences, even though the case with high friction on the both surfaces has slightly greater accumulative plastic strain.

Figure 18 shows roll separation forces for the various friction cases during the forward and backward strokes. The difference is small between the three cases in the forward stroke, which is consistent with [5], and relatively larger in the backward stroke. The reason is that the material flow is more complex and the friction causes even more inhomogeneous deformation in the backward stroke. The friction coefficient also influences the axial position of the maximum force during the backward stroke.

4 Conclusions

The influence of tube and mandrel displacements and roll stand deflection on the roll separation force and strain history were investigated by use of a 3D FE model. The influences of friction in the tube/mandrel and tube/roll interfaces were also investigated.

The displacements and roll separation forces showed good agreement with measurements in the literature [3, 5, 7]. The stiffness of the tube preform had a significant impact on the roll separation force in the backward stroke. The effect of the supporting spring stiffnesses in the tools (the rolls and the mandrel) seemed to be of minor importance. However, the cases with springs had considerably different roll separation forces and accumulative plastic strains compared to the rigid case. Therefore, it is recommended that the displacements of the tools and the tube should be included in FE models for more accurate prediction of the roll separation force and the strain history.

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