AN ADVANCED TRIBOLOGY MODEL FOR HOT FORGING SIMULATIONS

S. R. SCHMID
Department of Aerospace and Mechanical Engineering, University of Notre Dame, Notre Dame, IN 46556, USA; e-mail: schmid.2@nd.edu

K. KANNA, S. VAZE, M. PANDHEERADI
Concurrent Technologies Corporation, Johnstown, PA 15904, USA

SUMMARY
Simulation of hot forging is limited by the low sophistication of tribology models available in commercial codes. It is demonstrated that these simple models lead to erroneous predictions of strains in simple forging operations. A more sophisticated model, involving estimations of friction from asperity-scale interactions between surfaces, is developed and applied to hot forging of a nickel-based super alloy. The resulting simulations better reproduce experimentally observed behaviour.

Keywords: Friction, Lubrication, Forging, Manufacturing, Simulation.

1 INTRODUCTION
Manufacturing operations present extremely demanding applications of tribology. Elevated temperatures, high processing speeds, demanding surface finish and reliability requirements, and environmental considerations all play a role in determining tooling life and product quality. Tribological phenomena are important to the viability and optimisation of most manufacturing processes, but relatively little attention has been paid to this subject compared to the efforts directed towards tribology in machine elements, for example.

Forged components like airfoils, cases, integrally bladed rotors, rings and shafts comprise over 30 percent of the cost of aerospace propulsion and structural systems. Common aerospace alloys such as René-88DT (R88), a nickel-based super alloy, are especially challenging for a number of reasons. The forging temperatures are very high, around 1000 °C, and limited ductility encourages isothermal forging, further complicating die design. R88 also displays strain softening over a certain temperature range, introducing an additional complication for process modellers.

2 FRICTION IN HOT FORGING
Dry friction is easiest to model for bulk deformation processes. A common approach, and one that works quite satisfactorily, is the Tresca friction model, where the friction force is proportional to the work piece shear strength and the area of contact, with the constant of proportionality known as the friction factor.

It is well known that plastic deformation causes roughening of surfaces. An excellent discussion of surface roughness evolution is given by Tong, et al. [1]. For small strains, the work piece surface roughness is linearly proportional to applied strain.

In practice, the asperities are never free to deform in an uncontrolled fashion. Wilson and Schmid [2] investigated the evolution of roughness in bulk metal forming. For very thin lubricant films, they found that the tooling surface finish is impressed onto the work piece. At very large film thicknesses, the tooling leaves the work piece unaffected, while at intermediate values, the work piece surface is strongly dependent on the film thickness. Wilson and Schmid gave an approximate relationship between surface roughness and film thickness in rolling 5052-O aluminium strip as

\[ R_{a} = R_{at} + Ch \]  

where \( R_{a} \) is the surface roughness of the work piece, \( R_{at} \) is the tooling roughness, \( C \) is an experimental constant for a given material and \( h \) is the film thickness. Wilson and Schmid reported a value of \( C = 0.154 \) for their experiments on 5052-O aluminium, while Wilson and Stilleto [3] reported a value of \( C = 0.25 \) in forging of 6061 aluminium alloy.

Forging lubrication is a challenge in that no general film thickness relationships exist. Ratnagar, Cheng and Schey [4] discuss the difficulties in modelling such an operation, not least of which are that the film is a squeeze film and the rheological properties of the lubricants are unknown at hot forging temperatures. The tooling initially squeezes some lubricant from the contact zone until its outer periphery forms a seal against the die, entrapping a film of lubricant in the centre of the contact zone. As upsetting continues, the lubricant film thins and can break down because of work piece strain. This process is referred to as a “stretch” mechanism, since the contact area increases during contact.

Therefore, the approach used here is to assume that an initial lubricant film has been generated through a squeeze mechanism, and that the stretch mechanism can be used to track the film thickness evolution. It can be shown that the instantaneous film thickness is inversely proportional to the strain in the surface layers of the work piece.

Once the lubricant film thickness and the work piece surface roughness are calculated, the lubrication regime can be determined and an appropriate friction model applied. If the film thickness is greater than three times
the composite surface roughness, then fluid film lubrication occurs and the friction will arise from the lubricant alone.

If the film thickness is between one and three times the surface roughness, the real area of contact can be calculated from the model of Christensen [5]:

\[ A = \frac{35}{32} \left( \frac{16}{35} - z^3 + \frac{3}{5} z^7 + \frac{1}{7} z^9 \right) \]  

(2)

where \( A \) is the contact ratio and \( z \) is a surface parameter given by the ratio of film thickness to surface roughness. Christensen derived this relationship for a Gaussian distribution typical of a random surface. Wilson and Marsault [6] investigated other, more ordered surfaces but found that the contact areas that result are very close to Christensen’s predictions.

Given the large number of process and material variables that affect friction and the dynamic nature of some of these variables, a more elaborate friction model, based on the regime of lubrication and on the real area of contact, is needed.

In full film lubrication, asperities never contact each other, and it is reasonable to use a constant friction factor which corresponds to the shear strength of the lubricant. Therefore, for full film lubrication, the friction stress is given by

\[ \tau = m \cdot \theta \]  

(3)

where \( \tau \) is the friction stress associated with the lubricant and \( k \) is the material shear strength. \( m \) is the friction factor, and the subscript \( l \) means that the friction factor is associated with the lubricant only.

In mixed lubrication, the friction force depends on both the lubricant and the asperities. Wilson, Huang, and Hsu [7] suggest a friction stress expression of

\[ \tau = c \cdot k \cdot A + \theta \cdot k \cdot A + \tau (1 - A) = m_l \cdot k \cdot A + m_l \cdot (1 - A) \]  

(4)

where \( c \) is an adhesion coefficient, \( A \) is the fractional contact area, \( k \) is the material shear strength, \( \theta \) is a plowing coefficient (and is proportional to the surface slope and therefore the roughness), and \( H \) is the surface hardness. \( m_l \) is the combined effect of adhesion and plowing, given by

\[ m_l = c + \theta \cdot H \]  

(5)

In the boundary regime, Wilson [8] proposes that only the adhesion and plowing terms in Equation (4) need to be considered since these dominate friction. These friction rules are implemented in the tribology module produced in this research.

3 RESULTS

3.1 Experimental Investigations

Ring compression tests were performed on René-88 rings using dies made of TZM. Preliminary metallography indicated a fairly uniform microstructure and distribution of \( \gamma \) precipitates across the block. Rings with an outer diameter of 25 mm with the standard 6:3:2 OD:ID:thickness configuration were machined. They were grit-blasted to an initial surface finish of 8.0-12.0 µm, similar to a grit-blasted René-88 billet, while the TZM dies had an initial finish of 1.5 µm. A few rings were given an initial surface finish of 1.3 µm to investigate if this had any effect on the measured friction. The lubricant used was DAG-634 (Boron Nitride suspended in water). The tests were conducted on a 200 kN MTS servo-hydraulic machine, equipped with a vacuum furnace. Two tests were performed for each condition.

The inner diameters (ID) of compressed R88 rings were measured to compute the percent change in ID. Table 1 provides an illustration of the typical change in dimensions of the ring. Standard calibration curves [9] were used to quantify friction in each case. This was done either in the form of a constant Coulomb-Amonton friction coefficient (\( \mu \)) or a constant Tresca friction factor (\( m \)). Sample experimental results and the corresponding \( \mu \) and \( m \) values are also listed in Table 1.

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Height Reduction (%)</th>
<th>Decrease in ID (%)</th>
<th>Mean Friction Coeff. (( \mu ))</th>
<th>Mean Friction Factor, (( m ))</th>
</tr>
</thead>
<tbody>
<tr>
<td>1000</td>
<td>30</td>
<td>5.2</td>
<td>0.179</td>
<td>0.388</td>
</tr>
<tr>
<td></td>
<td>60</td>
<td>42.1</td>
<td>0.233</td>
<td>0.436</td>
</tr>
<tr>
<td>1100</td>
<td>30</td>
<td>11.0</td>
<td>0.272</td>
<td>0.57</td>
</tr>
<tr>
<td></td>
<td>60</td>
<td>48.9</td>
<td>0.26</td>
<td>0.482</td>
</tr>
</tbody>
</table>

Table 1: Sample results from ring compression tests.

3.2 Constant Friction Factor Simulations

A number of ring compression test simulations were carried out in both DEFORM-2D and DEFORM-3D, commercial finite element codes developed for metal forming simulations. Two materials were considered in these simulations, titanium alloy Ti-6Al-4V and R88, although most of the simulations were with the latter material and these are emphasized here. The stress-strain curve for R88 is given in Figure 1. Note the pronounced strain softening in the curve, especially at 1000 °C. This effect is often encountered with thermally softening materials.

At 1100 °C, the finite element simulations result in apparently reasonable force predictions and strain fields. Note that at this temperature, Figure 1 shows that the material is very nearly perfectly plastic.

![Figure 1: Constitutive behaviour of R88 at hot forging temperatures.](image)

The thermal softening at 1000 °C can lead to peculiar behaviour. Figure 2 shows simulations of R88 at 1000 °C, where strain localization and its associated
softening has resulted in an unusual shape for the ring next to the die. The behaviour occurs with 6:3:1.5 rings as well, as shown in Figure 2 (b), but the behaviour is less pronounced since deformations are more likely to occur in the substrate for larger aspect ratios. However, note that a fold has been predicted at the inner radius. Such geometries are not seen in experiments, indicating a shortcoming in simulations using constant friction factors.

3.3 Implementation of Tribology Module

A tribology module was developed using the theory described earlier. The difficulty in applying the tribology module is the uncertainty in selecting initial conditions as described below:

- The initial film thickness \( h_0 \) of the lubricant is unknown. The volume applied and the lubricant rheology are also unknown, making the direct determination of initial thickness impossible. The lubricant is based on molybdenum disulfide and is applied at room temperature prior to forging.
- Friction factors for the lubricant and the work piece are unknown.
- Therefore, the simulation requires an initial determination of \( h_0, m_l \), and \( m_m \). Also, \( m_m \) depends on the material hardness, and Sheu and Wilson [10] showed that the asperity hardness drops rapidly with a superimposed bulk strain rate. The current model ignores strain rate effects, although it is hoped that future research will incorporate this effect.
- The initial values of these variables are determined as follows:
  - \( m_l \) is obtained from ring compression tests at low reductions in height. The rationale for using low reduction experiments is that the stretch effects described above are very low for these cases, and direct asperity contact should be less intimate or nonexistent in these cases. \( m_l \) was determined to be 0.4.
  - \( m_m \) is assigned the value of 1. The reason for this is that un lubricated experiments result in the ring welding to the die surface. This is understandable; the very hot work piece is producing new surface area during compression testing, exposing nascent material without a protective oxide layer.
  - \( h_0 \) is varied until the ring compression test result is achieved. In effect, this controls the extent of asperity contact.

The current tribology module is a stand-alone program, and does not interface with DEFORM. An interface is planned for the future. The results shown here required a manual change of friction values at each user-defined location and an update of the friction factor values every 5 time steps of a 100 step simulation; seven friction windows were defined across the surface of the ring. This manual transfer of data made each simulation extremely time consuming, with the computational time being a small fraction of the total.

Using the tribology module, a simulation of ring compression testing of R88 at 1000 °C was performed. Sample results for a 6:3:2 ring are shown in Figure 3(a). Note that the strain softening-induced instability has been suppressed by the tribology module. The reasons for this can be best understood by comparing Figures 2(a) and 3(a). The shear band that develops in Figure 2(a) forces the largest strain to occur at the outer and inner radii of the ring. The associated stretch term thins the lubricant film and the roughness simultaneously increases to form an intimate contact at the inner and outer edges. At the same time, comparatively little strain towards the centre of the ring means that the friction factor maintains its initial value. The die/work piece instability suppression still occurs for a 6:3:1.5 ring, as shown in Figure 3(b) at the die/outer radius interface.

It should be noted that the suppression of instabilities as shown here holds for isothermal forging. For non-isothermal forging, heat transfer will aid in instability suppression because the material adjacent to the die will be cooler and therefore stronger than the centre, as discussed by Schey [9].

In Figure 3(a), there is still a small extrusion along the die interface. This may be attributable to the limit in the contact area in the current model, or the fact that the friction stresses are updated only every five time steps, or it may indeed be a real phenomenon that can be duplicated under the right experimental conditions. Further work in this area is underway.
4 CONCLUDING REMARKS

It was clearly shown that friction plays a significant role on the strains and strain distributions in hot forging. Also, since friction is an evolutionary variable, a more elaborate friction model based on internal process and material variables was shown to be very valuable. As a result of the research performed to date, it can be concluded:

- The advanced tribology module gave results that consistently matched experimental values of inner diameter reduction.
- Advanced materials such as R88 cannot be modelled accurately with a conventional friction model. Using a more advanced understanding of friction gives results that more closely match experimental results.
- Once a material/lubricant combination has been characterized, extension to different height reductions or geometries is readily accomplished.

5 ACKNOWLEDGEMENT

This work was funded by the National Center for Excellence in Metalworking Technology, operated by Concurrent Technologies Corporation (CTC) under Contract number N00140-92-C-BC49 to the U.S Navy as a part of the U.S. Navy Manufacturing Technology Program.

6 REFERENCES