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# A technique to compensate for temperature history effects in the simulation of non-isothermal forging processes

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**Industrial Summary**

An empirical method was developed to "correct" isothermal flow stress data and thus account for temperature history effects in non-isothermal forging processes. The method is essentially an iterative procedure that uses an FEM code, such as ALPID/DEFORM, and results of non-isothermal forging trials to adjust the isothermal flow stress data. Simulation results, conducted with such data for Ti-6Al-4V investigated here, give predictions that are comparable to the experimental load and metal flow measurements in non-isothermal ring tests. Hence, the approach developed in this study, can be used to: (1) obtain the flow stress behavior under non-isothermal forging conditions; and (2) improve the accuracy of FEM simulations of conventional hot forging processes in general.

## 1. Introduction

Correct representation of material flow behavior is needed for reliable FEM (finite element method) simulation of forming processes. Often considerable differences exist between the results of forging experiments and FEM simulations which use flow stress data reported for that material in the literature: these data are usually obtained from isothermal tests. There may be questions on the reliability of other input data used in a FEM simulation, such as friction

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factor, interface heat transfer coefficient etc. However, the friction factor can be obtained with reasonable accuracy from a ring compression test. The interface heat transfer coefficient, based on the research conducted by Semiatin et al. [1] and Burte [2], can also be obtained quantitatively. The "weak"portion of the input data for FEM simulation is thus the flow stress. Errors in flow stress, besides experimental errors, may be due to: (i) differences in materials in term of chemistry, microstructure, impurities, and other prior history effects; or (ii) differences between the flow behavior of the same material obtained under isothermal versus non-isothermal conditions.

In this study, an iterative empirical method was developed to take into account differences in flow stress data for isothermal and non-isothermal conditions. By this means, the non-isothermal FEM simulation of forging processes may be carried out more accurately by using the isothermal flow stress data after compensation for temperature history effects, instead of measured isothermal flow stress data. The proposed method uses the FEM analysis technique and results of non-isothermal ring tests to correct isothermal flow stress. As an example, the application of this method to simulate the metal flow behavior for the non-isothermal compression of Ti–6Al–4V rings is presented.

## 2. Background

There is a considerable literature on the study of the material flow stress behavior. Most of these data were obtained from isothermal hot compression tests in which temperature changes within the test sample are relatively small and are a result primarily of deformation heating effects.

Altan and Boulger [3] reviewed a large number of domestic and foreign metal forming articles and presented flow stress data for selected materials. Examples were given to illustrate the use of flow stress data with simple formulae in predicting pressures in upset forging, closed die forging, and cold extrusion. In related work, Douglas and Altan [4] conducted uniform isothermal compression tests and ring tests to determine flow stress data for various metals at different forging rates and temperatures as did Suzuki et al. [5].

Considerably less work has been conducted to assess the effects of large changes in temperature during testing on the flow stress. Work such as that of Farag et al. [6] and Glover [7] are notable exceptions. In the former investigation, the torsional flow stress in nominally isothermal tests on pure aluminum was much higher than that from tests in which samples were *cooled* continuously during testing (Fig. 1 (a). In the related work by Glover (Fig. 1 (b), a similar effect was noted for iron; furthermore, for this later material, tests in which the material was continuously *heated* revealed flow stresses considerably *higher* than those from isothermal tests. It may be concluded from both investigations, that the effect of temperature changes on metallurgical structure, which determines the flow resistance, does not occur instantaneously. In

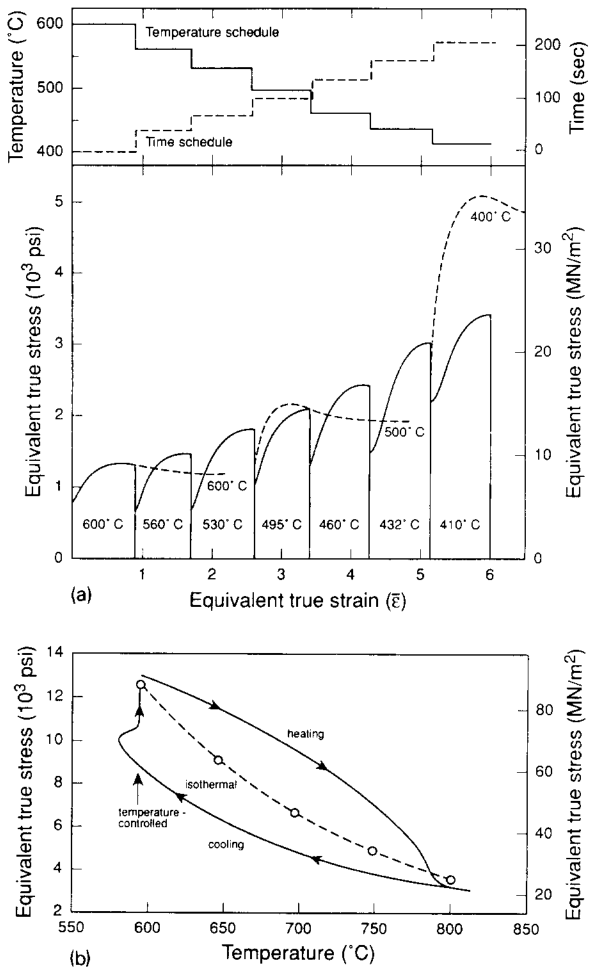


Fig. 1. (a) Temperature-time schedule for a series of torsion tests on super-purity aluminium conducted at a strain rate of 2.3/s (top). On the temperature curve, a horizontal segment indicates the temperature of deformation and a vertical segment the cooling between stages. On the time graph, an almost horizontal segment indicates the short time elapsed during a stage deformation, and a vertical segment indicates the delay between stages. In the lower graph, the solid lines indicate the flow stresses recorded, whereas the broken lines are for continuous isothermal tests at the temperatures noted [6].
(b) Flow stresses measured during continuous heating or continuous cooling versus flow stress measured in a series of isothermal tests. The material was vacuum melted bcc iron, the strain rate in the torsion tests was $1.5 \times 10^{-3}$/s, and the rate of heating or cooling was about 50 °C/s [7].

other words, a "soft" structure that is developed at a high temperature may persist for some time after a temperature drop. Conversely, the effect of a "hard" structure developed at a low temperature may persist for some finite interval of strain when the material temperature increases rapidly.

In a more recent investigation, Burte [2] investigated heat transfer and friction during hot forging. Data from ring experiments were analyzed by generating heat transfer coefficient and friction shear factor calibration curves using FEM simulations. Burte observed an interesting phenomenon in simulations for Ti–6Al–4V ring tests: the load–stroke and the friction calibration curves, estimated with the FEM simulation using isothermal flow stress data obtained from the literature, differed greatly from the experimental values [8]. These observations suggested that there may be a difference between flow stress data obtained from isothermal and non-isothermal tests. The current study was therefore carried out to investigate this important question.

## 3. Method for correcting isothermal flow stress data for use in simulation of non-isothermal forging processes

Because of the difficulty of measuring flow stress in a material undergoing large temperature changes during conventional forging, an iterative technique has been developed for "correcting" isothermal flow stress data so that they can be used to simulate conventional non-isothermal processes. In essence, the flow stress dependence on strain is ignored, a good approximation for the material of interest here (Ti–6Al–4V) as well as many others deformed in the hot-working regime. The stress dependence on temperature at fixed strain rate is then iteratively adjusted, relative to that found from isothermal testing, until simulation predictions agree with observations. The magnitude of the required adjustment can be estimated from the experimental work of Farag et al. [6] and Glover [7] on the effect of temperature history on flow stress. In the present work, the ring upset test was simulated, and the principal measurements used to confirm the applicability of this approach were the load versus stroke behavior and the material flow pattern. Comparison of measured load–stroke curves provides only a gross check of the accuracy of the process simulation. By contrast, metal flow comparison (i.e., percent change of inner diameter as a function of reduction, or friction factor calibration curves, and free surface bulge profiles) yield a more sensitive gage of the efficacy of the proposed approach.

The precise method of accounting for temperature history effects made use of a "compensated" temperature to correct for non-isothermal effects during deformation. In this method the temperature $T$ used in the constitutive relation $\bar{\sigma}=C(T)\dot{\bar{\epsilon}}^{m(T)}$ in the FEM model is not the actual (local) workpiece temperature but instead a somewhat different value $T_c$ (i.e., compensated temperature) calculated from the expression

$$T_c = T + F(t)\,(T_{\text{initial}} - T) \tag{1}$$

in which: $T_c$ is the nodal temperature, compensating for temperature history effects, which is used for the flow stress calculation in the FEM simulation; $T$ is the current actual nodal temperature obtained from FEM simulation; $F(t)$ is a compensation coefficient which varies with time during deformation; and $T_{\text{initial}}$ is the initial workpiece temperature (i.e., preheat temperature).

In eqn. (1), the term $F(t)(T_{\text{initial}}-T)$ is used to define or "correct" the difference between the "isothermal" and the "non-isothermal" flow stress of the material. This difference may be characterized as a time related microstructure history effect. In the non-isothermal ring compression test and in particular in the region where the ring material is chilled down by the dies, the flow stress may be conjectured to be not as high as the flow stress obtained from the isothermal test, but much lower. This difference may arise due to the following specific reasons:

(1) *Differences in dislocation substructure.* When the temperature of a material decreases during deformation, the dislocation substructure developed at the higher temperature does not change instantaneously to that characteristic of the lower temperature. Rather, some time (and additional deformation) are required for the former, softer structure to "re-adjust" to the harder one characteristic of the lower temperature. For this reason, the flow stress transient would be expected to be manifested by a lower value than that measured in isothermal deformation modes.

(2) *Metastability of a second phase in a two phase alloy.* When an alloy such as Ti–6Al–4V is preheated high in the two phase (alpha/beta) field, the microstructure consists of a large percentage of soft (beta) phase and a smaller amount of hard (alpha) phase. If the temperature of the workpiece falls suddenly during non-isothermal deformation, however, the kinetics of decomposition of the softer phase to yield the larger (equilibrium) volume percent of harder phase will not be instantaneous. Hence, a transient consisting of a lower flow stress than that obtained in the corresponding lower temperature isothermal deformation might be expected due to this source as well. Unfortunately, cooling–transformation data (in the presence of deformation), or data that would be needed to this end, do not exist in the technical literature for materials such as Ti–6Al–4V.

(3) *Variation in grain size and phase morphology.* Because the flow stress at hot working temperatures is dependent on grain size and, in two phase alloys, phase morphology, temperature changes during deformation may lead to flow stress transients due to changes in such microstructural features.

Examination of eqn. (1) reveals that in regions of the workpiece in which the temperature is close to its initial or preheat temperature, the flow stress data obtained from isothermal tests are used with little compensation, because the microstructure of the material at this region is likely to be close to the equilibrium microstructure that would pertain to the corresponding isothermal test. On the other hand, in regions of the workpiece in which the temperature drops a great amount from the initial temperature, flow stress data obtained from isothermal tests are used with a large compensation, because of the large temperature history effect. The influence of time on microstructure evolution during temperature transients is reflected by $F(t)$, a function which generally decreases monotonically with the time. Therefore, larger temperature compensations are imposed: (i) during the early stages of the deformation; and (ii) in the regions where the temperature drops are large. By iteration with different $F(t)$ factors, different load–stroke and friction shear factor calibration curves can be created. Load–stroke and friction shear factor calibration curves which match the experimental results can be obtained from the flow stress corrected thusly, and the relationship between the flow stress obtained from an isothermal test and a non-isothermal test can therefore be established.

The advantages of using the temperature compensation method to correct the flow stress are twofold. First, this scheme uses only one parameter, i.e., temperature, to adjust the flow stress. Thus by iterating with different temperature compensation schemes, i.e., values of $F(t)(T_{\mathrm{initial}}\text{-}T)$, the flow stress that pertains to non-isothermal conditions can be obtained. Second, this scheme establishes the relationship between the flow stress obtained from isothermal and non-isothermal tests.

An expression similar to eqn. (1) may be derived to treat cases in which a flow stress difference due to lot-to-lot variations in chemistry, grain size, etc., for a given material, may arise. The required temperature compensation in this case does *not* arise from the temperature history effect. The reader is referred to Shen et al. [9] for further discussion of such situations.

## 4. Experimental conditions and process simulations

The approach described above, to take temperature history effects into account, was validated by comparison of FEM simulation predictions to experimental data for non-isothermal ring tests on Ti–6Al–4V. The material had been alpha/beta processed to yield a microstructure of equi-axed alpha in a transformed beta matrix. Initial ring dimensions were OD:ID:height =35.56:17.78:11.85 mm, i.e., 6:3:2 rings were employed. Process conditions and other input data for the FEM simulations (using ALPID [10]) are summarized in Tables 1 and 2. Isothermal flow stress data for this specific lot of material were not available. However, examination of literature data for various lots of Ti–6Al–4V and a sister alloy, Ti–6Al–2Sn–4Zr–2Mo, with equiaxed alpha microstructure, suggested that the isothermal flow stress values chosen are probably reliable to $\pm 5$ to 10 percent.

As shown in Table 1, two ram speeds were used in the Ti–6Al–4V ring compression tests. Therefore, the procedure employed consisted of correcting the flow stress for the low speed test (ram speed=5.1 mm/s (0.2 in/s)), and then testing the corrected flow stress in high speed test simulations (ram speed=56 mm/s (2.2 in/s)). This approach was chosen because in the low

TABLE 1

Process conditions for Ti-6Al-4V ring compression tests [2]

| | |
|---|---|
| Die material | H-13 hot working tool steel |
| Initial billet temperature | 954°C (1750°F) |
| Initial die temperature | 315.5°C (600°F) |
| Die velocity | 5.1 mm/s (0.2 in/s) for low speed tests |
| | 56 mm/s (2.2 in/s) for high speed tests |
| Pre-deformation transfer time | 3 s |
| Pre-deformation dwell time | 3 s |

TABLE 2

Simulation matrix for Ti-6Al-4V ring compression tests

| Ram velocity (mm/s) | Flow stress used | Heat transfer coefficient ($kW/m^2$ °C) | Friction shear factor |
|---|---|---|---|
| 5.1 | literature[a] | 20 | 0.18 |
| 5.1 | TC[b] | 20 | 0.18 |
| 5.1 | TC | 20 | 0.14 |
| 56 | literature | 20 | 0.16 |
| 56 | TC | 20 | 0.16 |
| 56 | TC | 20 | 0.12 |

[a]Literature flow stress (after correction for deformation heating) [2].
[b]Flow stress data with temperature compensation eqn. (1) with $F(t)$ given by eqn. (2).

speed tests the deformation time was longer (1.1 s), and the temperature drop was larger. As a result the flow stress transient could be observed over a wider range of temperature and time, including the time regime that pertained to the high speed test (0.1 s).

FEM simulations with temperature compensated flow stress made use of eqn. (1) with $F(t)$ determined in a iterative manner to give the best agreement between experiments and model predictions. The $F(t)$ function so determined was

$$F(t)=\begin{cases}0.38-0.073t & 0<t<0.41 \quad \text{s}\\ 0.37-0.059t & 0.41<t<0.75 \quad \text{s}\\ 0.37-0.057t & 0.75<t<1.1 \quad \text{s}\end{cases} \tag{2}$$

The temperature compensated flow stress data for a strain rate of 1/s and the initial test temperature of 1750°F (954°C) are compared to the corresponding isothermal values in Fig. 2. Note that the flow stress for Ti–6Al–4V in either case is independent of strain, as been found for similar titanium alloys [11].

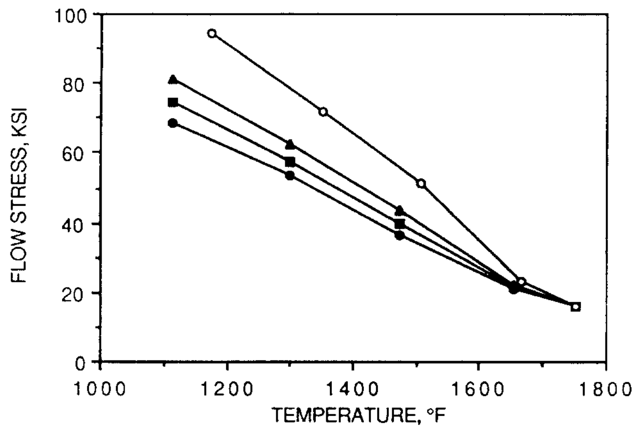


Fig. 2. Comparison of isothermal Ti-6Al-4V flow stress data and non-isothermal flow stress deduced from simulation of non-isothermal ring compression tests. (Strain rate = 1/s; strain range = 0.0–1.0; initial workpiece temperature = 1750°F; it is assumed that flow stress is not affected by strain; hollow round points refer to isothermal tests (after correction for deformation heating alone), whilst full round, square and triangular points refer to non-isothermal tests, at 0, 0.5 and 1 s after deformation started, respectively; 1 KSI = 6.895 MPa.)

## 5. Results and discussion

The load–stroke and friction calibration curves obtained from the low speed experiments and the FEM simulations are compared in Figs. 3 and 4. Since the FEM program used is based on rigid–visco-plastic formulation, the elastic portion of the experimental load–stroke curves was not considered to make the load–stroke curves from FEM simulations and the experiments comparable. To accomplish this, the measured data were adjusted slightly by: (a) moving the load value that corresponds to the starting point of plastic deformation to zero stroke; and (b) dividing the total load range corresponding to plastic deformation by the difference between the initial ring height and the final ring height obtained from experiments. The load–stroke curves obtained with the temperature compensation scheme with two different friction factors agreed well with experiments (Fig. 3). This contrasts to the poor agreement between FEM predictions based on isothermal flow stress data and experimental results. Figure 3 also shows that the load–stroke curves obtained from FEM simulations with a small variation of friction shear factor (from 0.18 to 0.14) are very close to each other. However, the friction shear factor calibration curves (Fig. 4) vary over a larger range for the two friction factors. The friction shear factor in the experimental ring compression tests can thus be estimated as being between 0.14–0.16.

Comparisons of predicted load–stroke and friction calibration curves with experiments verify the validity of proposed corrections for flow stress from a

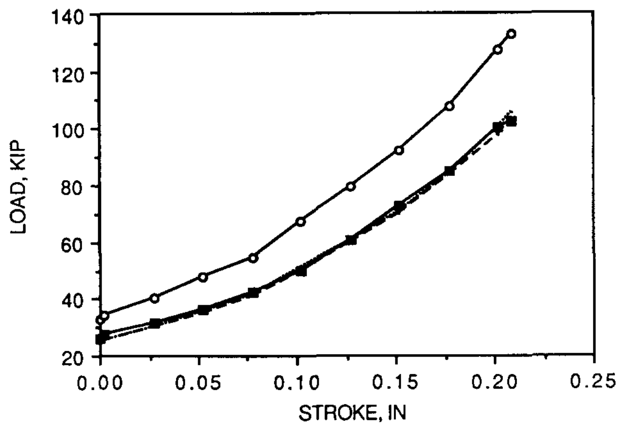


Fig. 3. Comparison of load-stroke curves for Ti-6Al-4V ring compression tests obtained from experiments and FEM simulations. (Ram speed = 5.1 mm/s (0.2 in/s); flow stress data were corrected with a temperature compensation scheme, eqn. (1), with $F(t)$ from eqn. (2); square points - experimental data from [2]; round points - with literature flow stress (after correction for deformation heating alone, $m=0.18$); thin dotted line - with flow stress after temperature compensation ($m=0.18$); thick dotted line - with flow stress after temperature compensation ($m=0.14$); 1 KIP = 4.45 kN.)

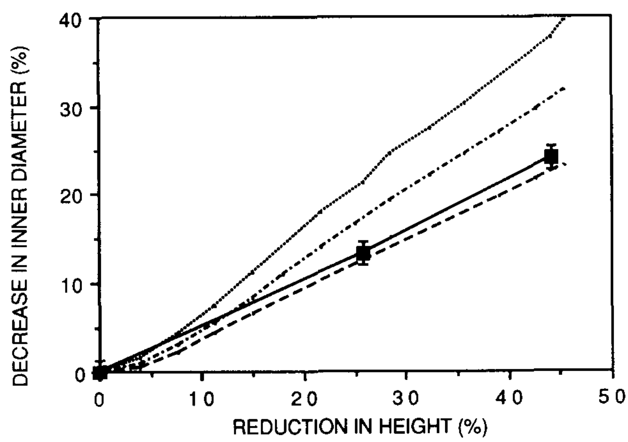


Fig. 4. Comparison of friction calibration curves for Ti-6Al-4V ring compression tests obtained from experiments and FEM simulations. (Ram speed = 5.1 mm/s (0.2 in/s); flow stress data were corrected with a temperature compensation scheme, eqn. (1), with $F(t)$ from eqn. (2); square points - experimental data from [2]; thin dotted line - with literature flow stress (after correction for deformation heating alone, $m=0.18$); chain-dotted line - with flow stress after temperature compensation ($m=0.18$); thick dotted line - with flow stress after temperature compensation ($m=0.14$).)

global point of view. Comparison of FEM predictions to observed ring cross section shapes at 26 and 45% reduction in height (Fig. 5) adds credibility to the approach on a more detailed scale. It is seen that again the results obtained from the FEM simulation with the flow stress data corrected for temperature history effects agree well with experiments. Further, an exaggerated bulge is

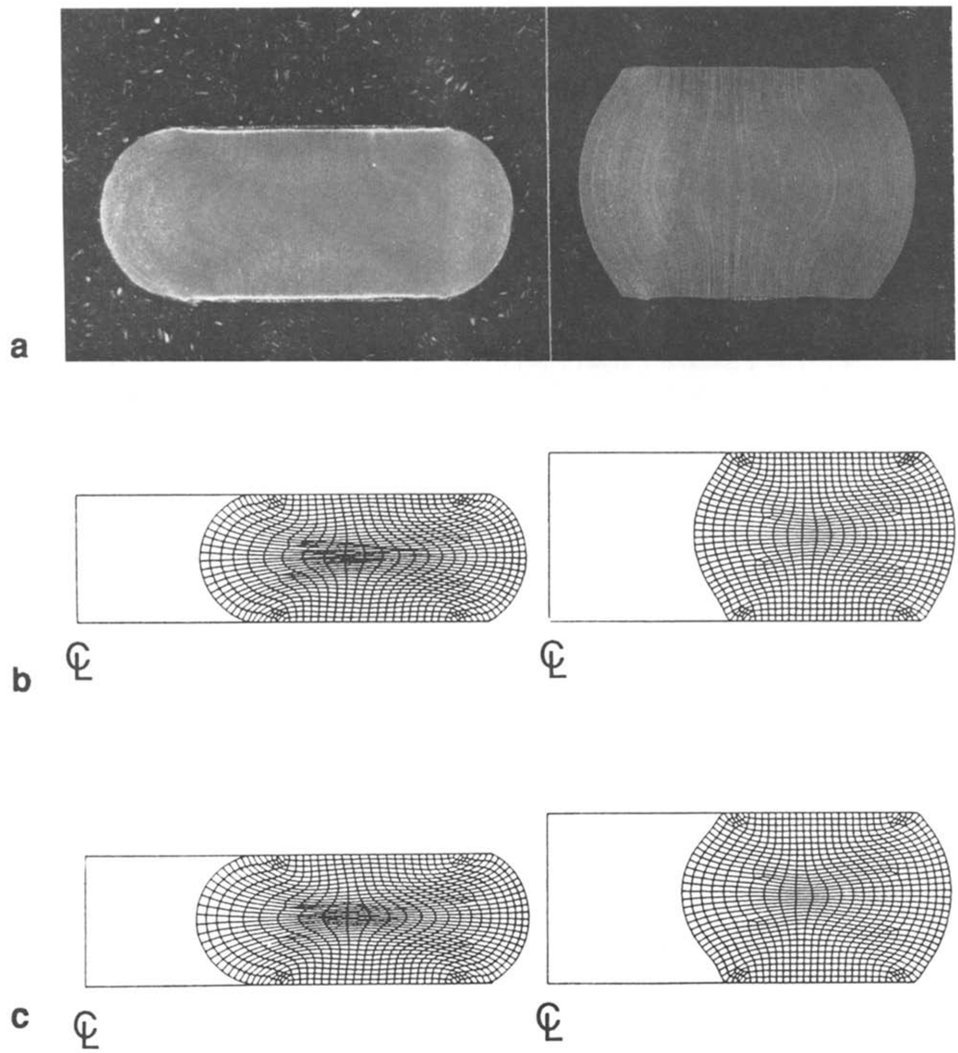


Fig. 5. Comparison of ring cross sections at 26% and 45% reductions for Ti-6Al-4V ring compression tests: (a) experimental observations [2]; (b) FEM simulations with flow stress corrected by temperature compensation scheme, eqn. (1), with $F(t)$ from eqn. (2); and (c) FEM simulations with the isothermal flow stress data after correction for deformation heating. (Ram speed = 5.1 mm/s (0.2 in/s); friction shear factor $m = 0.18$.)

seen to be predicted in the FEM simulation using the isothermal flow stress data.

The FEM simulations for the high speed ring compression tests were carried out to verify the temperature compensation scheme for the correction of flow stress derived from the low speed tests. The results for high speed tests are summarized again by the comparison of: (a) load–stroke curves; (b) friction calibration curves; and (c) ring cross sections.

Predicted load–stroke curves for high speed Ti–6Al–4V ring compression tests (Fig. 6) agree very well with experimental results. At the final stage, the simulations predict higher load than the experiment. This might due to the fact that, at the final stage, the ram in the experiment slowed down. However, the FEM simulations assumed a constant ram velocity.

The friction shear factor calibration curves, for high speed Ti–6Al–4V ring compression tests, obtained from the experiments and the FEM simulation, are presented in Fig. 7. It is seen from this figure that the FEM simulation results with flow stress after temperature compensation and variations of friction shear factor from 0.12 to 0.16 cover a large range. At large reductions, the experimental data show a lower friction shear factor. This phenomenon is contrary to most experimental observations, in which the friction shear factor *increases* due to the increasingly poor lubrication conditions, and must therefore be ascribed to experimental errors. Nevertheless, from the general behaviors of the friction calibration curves, it can be predicted that, if the friction

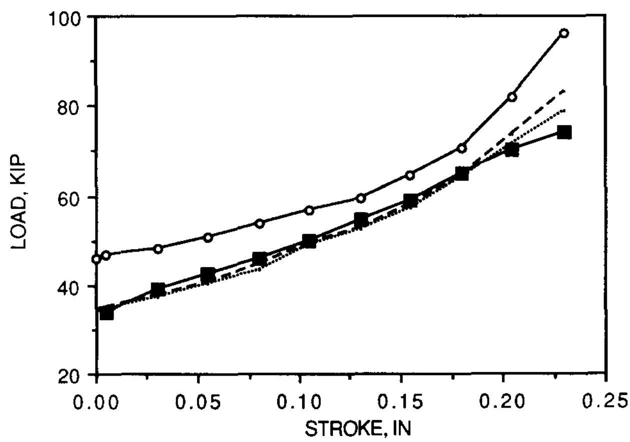


Fig. 6. Comparison of load-stroke curves for Ti-6Al-4V ring compression tests obtained from experiments and FEM simulations. (Ram speed = 56 mm/s (2.2 in/s); flow stress data were corrected with a temperature compensation scheme, eqn. (1), with $F(t)$ from eqn. (2); square points - experimental data from [2]; round points - with literature flow stress (after correction for deformation heating alone, $m = 0.16$); thick dotted line - with flow stress after temperature compensation ($m = 0.16$); thin dotted line - with flow stress after temperature compensation ($m = 0.12$).)

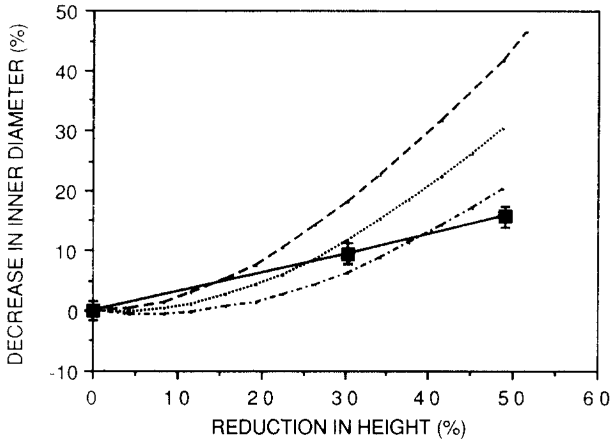


Fig. 7. Comparison of friction calibration curves for Ti-6Al-4V ring compression tests obtained from experiments and FEM simulations. (Ram speed=56 mm/s (2.2 in/sec); flow stress data were corrected with a temperature compensation scheme, eqn. (1), with $F(t)$ from eqn. (2); square points - experimental data from [2]; thick dotted line - with literature flow stress (after correction for deformation heating alone, $m=0.16$); thin dotted line - with flow stress after temperature compensation ($m=0.16$); chain-dotted line - with flow stress after temperature compensation ($m=0.12$).)

shear factor varied over a large range, say from 0.1 to 0.18, the FEM predictions would be within the range of the experimental data.

In Fig. 8, ring cross section from the high speed experiments, at 30% and 49% reductions in height, are compared with the results obtained from the FEM simulations at the same stages. Here, it is seen that the cross sections predicted using the temperature compensation scheme (Fig. 8 (b)) are very close to the cross sections obtained from the experiments. On the other hand, the cross sections obtained from the FEM simulation with isothermal flow stress data with correction for deformation heating [2], but without temperature compensation, are quite different from the experimental results (Fig. 8 (c)).

Figure 8 reveals another interesting phenomenon – the upper surface of the cross sections are wider than the lower surfaces. This is most likely due to die chilling during the dwell period before the start of the ring compression despite the fact that a piece of wire was used in the experiments on the lower die to reduce specimen–die contact. Therefore it is necessary to take the pre-deformation heat transfer during the dwell period into account in the FEM simulation. A similar effect is seen at 26% reduction in the low speed ring test (Fig. 5 (a)). However, after the influence of a longer contact time during deformation (1.23 s), the temperature difference was very small, and the inequality of the contact lengths was not as obvious as in the high speed tests.

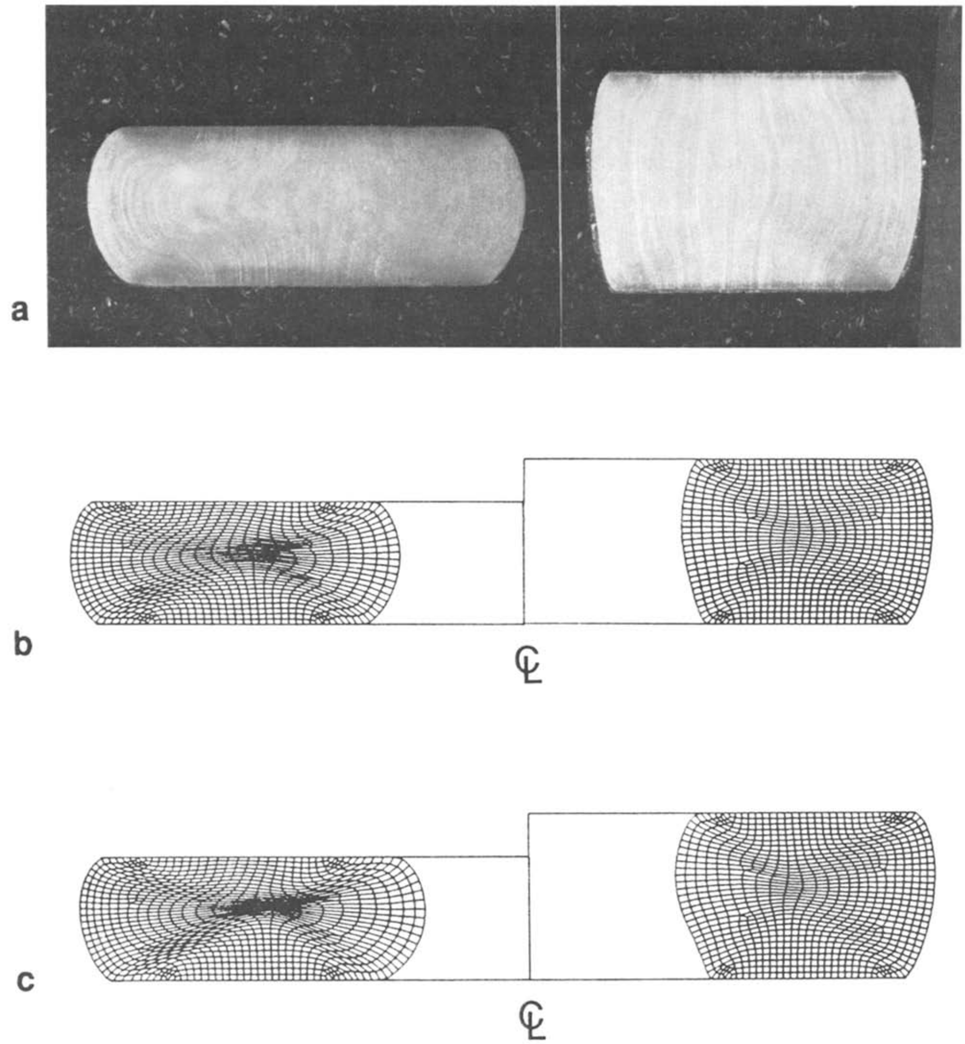


Fig. 8. Comparison of ring cross sections at 30% and 49% reductions for Ti-6Al-4V ring compression tests: (a) experimental observations [2]; (b) FEM simulations with flow stress corrected by temperature compensation scheme, eqn. (1), with $F(t)$ from eqn. (2); and (c) FEM simulations with the isothermal flow stress data after correction for deformation heating. (Ram speed=56 mm/s (2.2 in/s); friction shear factor $m=0.16$.)

## 6. Summary and conclusions

FEM simulations of the non-isothermal ring test were compared with experimental results for Ti–6Al–4V. FEM simulations with isothermal flow stress data, corrected for temperature history effects, yielded much better results than those based directly on isothermal flow stress data.

The flow stress behavior derived for simulation of the non-isothermal Ti–6Al–4V ring compression test indicates that temperature history effects are important in this and related non-isothermal forging operations. The term $F(t)(T_{initial}\text{-}T)$, in the temperature compensation, eqn. (1), takes this effect into account. Thus, strong evidence has been provided that the instantaneous temperature in non-isothermal forging is not a state variable for describing the constitutive behavior of materials. In other words, for a given strain and strain rate, there is no unique corresponding flow stress at each temperature. Nevertheless, the flow stress transient present at a given instant in a given location of the non-isothermally deforming material, can be related to the isothermally obtained flow stress as a function of initial forging temperature, current forging temperature, and deformation time, for a given strain and strain rate condition. At the initial temperature, the "non-isothermal" flow stress lies on the "isothermal" flow stress curve. With decreasing temperature in the forging, this value departs from the "isothermal" flow stress curve according to the process time and the difference between the preheat temperature and the instantaneous local temperature. It can be inferred that, if the initial forging temperature were different, the flow stress may depart from the isothermal flow stress curve at different points, according to other $F(t)$ functions.

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