Uniform Hot Compression of Nickel-based Superalloy 720Li under Isothermal and Low Friction Conditions

Satoko HORIKOSHI,1)* Akira YANAGIDA1) and Jun YANAGIMOTO2)

1) School of Engineering, Tokyo Denki University, Senju-Asahi-Cho 5, Adachi-ku, Tokyo, 120-8551 Japan.
2) School of Engineering, The University of Tokyo, Hongo 7-3-1, Bunkyo-ku, Tokyo, 113-8656 Japan.

(Received on April 9, 2020; accepted on September 30, 2020)

Isothermal compression tests at 1 000°C and 0.1 s⁻¹ strain rate, in which mica or glass sheets were used as a lubricant, were conducted. Isothermal condition was achieved by placing high-heat-resistant (HHR) alloys between a workpiece and ceramic tools in the induction-heating configuration to prevent heat from escaping to the ceramic tools. To perform high compression tests, it was necessary to increase the diameter of the HHR alloy, for which a new single-turn coil was designed using FEM calculation coupled with deformation-temperature electromagnetic fields. In order to obtain the correct flow stress, inverse analysis was conducted using the FEM calculation, in which temperature and strain rate fluctuations were compensated. However, the compensation was insufficient when the distribution of temperature and strain rate was large. The use of glass sheets as a lubricant considerably reduced friction and uniform deformation was achieved. Thus, flow stress obtained using the inverse analysis became extremely reliable. The flow stress of mica obtained using inverse analysis with a constant friction coefficient was different from that of glass. Introducing new friction model that the friction coefficient changed from 0.02 to 0.3, the flow stress of mica was consistent with that of glass. Therefore, the flow stress obtained using the inverse analysis for the new configuration proposed in this study proved to be reliable.

KEY WORDS: isothermal compression; inverse analysis; superalloy; induction heating; low friction.

1. Introduction

Precipitation-strengthened forged Ni-based alloys are commonly used for producing aircraft and gas turbine components.3,4) Strict control of the microstructure and properties of the Ni-based alloys used in jet engine components is necessary because of strict safety requirements. Therefore, accurate data with respect to hot-deformation behavior during hot forming under isothermal and constant-strain-rate conditions are needed. The flow stress provides processing load and strain distribution information, and flow-stress data have been collected by many researchers. 720Li alloy is a typical Ni-based superalloy with high strength, high corrosion resistance, and good structural performance at high temperatures; it is widely used in the production of turbine components.1–3) It is a precipitation-strengthened alloy with ~40% L12-ordered γ′ (Ni3Al,Ti) precipitates; secondary γ′ and spherical tertiary γ′ impart it with high strength.5) The diameter of the primary γ′ is typically between 1 and 3 µm, whereas the diameters of the secondary γ′ and tertiary γ′ are in the ranges 50–500 nm and 5–30 nm, respectively.5) 720Li alloy undergoes structural changes such as precipitation and solid-solution formation in response to temperature changes; therefore, maintaining temperature control during testing is important. However, testing at a uniform temperature is very difficult; in the case of small workpieces, in particular, the temperature fluctuation is severe and the temperature distribution of the workpiece becomes large. Compression tests have been conducted by many researchers using apparatuses with various heating systems to ensure adequate temperature control.

The first such apparatus used was a heat-insulated chamber installed on a servo-hydrostatic press. In this system, the workpiece, tools, and surrounding atmosphere could be maintained at a constant forming temperature.6–9) The heat-insulated chamber was very effective at obtaining accurate flow stress; however, data related to the evolution of the microstructure could not be obtained because the workpiece could not be rapidly quenched. The second type of apparatus used direct-current heating in a Gleeble thermomechanical simulator.10–13) This apparatus required the use of tantalum, a conductive material, to assist lubrication between the tools and the workpiece; however, sufficient lubrication could not be achieved. Numerical analysis of the Gleeble compression test was performed; however, uneven deformation resulting from friction and a temperature distribution (50°C for the
experiment in which the target temperature was 1,100°C have been reported to occur in the longitudinal direction of the workpiece. The third method is to use induction heating to maintain a constant temperature. However, because the ceramic tool component was not heated, the initial workpiece temperature distribution and the decrease in workpiece temperature during low-strain-rate forming could not be avoided. An integrated model developed by Matsui et al. uses Ni-based superalloy heating tools (CMSX-4) positioned between ceramic tools and a workpiece to achieve the desired isothermal conditions. However, the reduction in the height of the workpiece was limited by 50% because of the size of the superalloy tools. When the size of the superalloy tools was increased to subject the workpiece to high compression, the temperature increase due to induction heating caused microstructure changes and decreased the strength of the superalloy, thereby requiring further optimization of the heating coil.

In the present study, the isothermal condition was achieved by placing high-heat-resistant (HHR) alloys between a workpiece (720Li alloy) and ceramic tools in the induction heating configuration to prevent heat from escaping to the ceramic tools. To perform tests under high compression, it was necessary to increase the diameter of the HHR alloy; a new single-turn coil was designed for this purpose using finite element method (FEM) calculations coupled with deformation-temperature electromagnetic fields. To obtain the correct flow stress, inverse analysis was conducted. Inverse analysis is a method of obtaining flow stress by adjusting flow-stress parameters until the load obtained by FEM calculation matches the load obtained by experiment. In the FEM calculation used in the inverse analysis, temperature and strain-rate fluctuations were compensated for if the fluctuations of temperature and strain rate were small. Therefore, we proposed a test method using heating-assist HHR alloys with a single-turn induction heating coil and achieved isothermal tests. Furthermore, the effects of the lubrication conditions and friction model on the flow stress were investigated.

2. Experimental and Calculation Methods
2.1. Materials
The test material used was a 720Li 10-inch billet purchased from Allegheny Technologies Inc. The chemical composition of the material is shown in Table 1. The γ' solvus temperature was ~1,160°C, the γ' solvus temperature simulated by the thermodynamic software (JMatPro) was 1,154°C. There is a distribution of grain sizes in the billet, and the average grain sizes of workpieces at the center position, the position R/2 (R = radius) from the center, and the position 3R/4 from the center are 15.8 μm, 13.3 μm, and 9.4 μm, respectively. The billet was sub-solus solution treated at 1,104°C for 4 h, air-cooled, and then machined to a cylindrical shape with a diameter of 8 mm and height of 12 mm. The workpiece used was at the R/2 position of the billet. The average grain size of the workpiece was 19 μm, indicating an average grain grade of approximately ASTM 8.5.

2.2. Equipment
The experiments were conducted using ThermecMastorZ (Fuji Electronic Industrial Co., Ltd.) The configuration of the experimental components inside the processing chamber, which consisted of tools as well as coils for induction heating surrounding the tools and workpiece, is shown in Fig. 1. The conventional configuration using the four-turn coil is shown in Fig. 1(a). Figure 1(b) presents the proposed configuration of a single-turn coil, in which the HHR alloys were introduced between the workpiece and the tools, as reported in Section 3.3.

2.3. Configurations for Measurement
Figures 2(a)–2(c) show the conventional configuration (config. A), the configuration of preliminary tests for determining thermal parameters (config. B), and the configuration proposed in the present study (config. C), respectively. The four-turn coil was used for config. A and config. B, and a single-turn coil was designed for config. C in this study. In config. A, ceramic tools made of Si3N4 were arranged above and below the workpiece. Mica sheets, which functioned as both a lubricant and an insulator, were placed between the workpiece and the tools. Although the mica sheets provided thermal insulation during the experiments, the insulation capacity of mica was insufficient and the temperature decreased toward the end of the workpiece because of heat transfer to the low-temperature tools. Subsequently, the HHR alloys, which were heated by induction heating, were introduced. A pair of the HHR alloys made from Nimowal (Hitachi Metals Co., Ltd.) were placed above and below the workpiece in config. B and config. C. The

<table>
<thead>
<tr>
<th>Alloy</th>
<th>Ni</th>
<th>Cr</th>
<th>Co</th>
<th>Ti</th>
<th>Al</th>
<th>Mo</th>
<th>W</th>
<th>C</th>
<th>Zr</th>
<th>B</th>
</tr>
</thead>
<tbody>
<tr>
<td>720Li</td>
<td>Bal.</td>
<td>16.13</td>
<td>14.63</td>
<td>5.01</td>
<td>2.57</td>
<td>2.99</td>
<td>1.22</td>
<td>0.018</td>
<td>0.03</td>
<td>0.014</td>
</tr>
</tbody>
</table>

The γ' solvus temperature is around 1,160°C.
diameter and height of the HHR alloys were 15 mm and 6 mm for config. B and 17 mm and 5 mm for config. C, respectively. The diameter of the HHR alloys for config. C was larger than that for config. B to achieve greater compression. The heights of the HHR alloys were determined so that the heat capacities of the alloys were approximately equal. Nimowal is a conventional casting material that is easier to obtain than single-crystalline Ni-based superalloy CMSX-4. The compressive yield strength of Nimowal at a strain rate of $10^{-3} \text{s}^{-1}$ is 710 to 580 MPa in the temperature range from 1 000 to 1 100°C.22 Because HHR alloys were also heated to almost the same temperature as the workpiece by induction heating, heat transfer from the workpiece to the ceramic tools was suppressed and the temperature became uniform. Mica sheets were placed as an insulator between the tools and the HHR alloys, with mica or glass sheets placed between the workpiece and the HHR alloys to act as a lubricant. In the case of the glass sheets, grooves were carved into the upper and lower surfaces of the workpiece at 1 mm pitch and 0.2 mm depth from the center of the surface to hold the melted glass.

2.4. Experimental Method

Temperature measurements were recorded from points located 3, 6, and 9 mm from the bottom of the side surface of the cylindrical workpiece. The point 6 mm from the bottom was defined as the control point. The R-type thermocouples were spot-welded at the measurement points on the surface of the workpiece, and the control-point temperature of the workpiece was maintained at the target temperature using a proportional–integral–derivative (PID) controller.

The temperature was also measured through thermocouples spot-welded to the mid-height and surface of the HHR alloys. The temperature of the ceramic tool was measured by attaching a thermocouple to the lower ceramic tool at a position 10 mm from the workpiece side using Kapton tape. All of the temperature measurement points are indicated by black dots in Fig. 2. The temperature of the material in contact with the die decreases during forging; thus, the flow stress used for the deformation analysis was given as a function of the temperature, strain rate, and some other parameters. In a previous study,21) the following recrystallization-type equations:

$$\sigma = F_0 \left( \varepsilon_0 + \dot{\varepsilon}^\gamma \right)^{\nu} \left( \varepsilon \leq \varepsilon_c \right) \quad \text{.......................... (1)}$$

were conducted to reproduce the compression test. The flow stress used for the deformation analysis was given as a function of the temperature, strain rate, and some other parameters. In a previous study,21) to express the flow stress of a high-yield-point material such as the 720Li used in the present study, we proposed the following equations in which the initial strain $\varepsilon_0$ was introduced into dynamic-recrystallization-type equations:15,16)

The workpiece was heated to 1 000°C at a heating rate of 10°C/s and then maintained at this temperature for 400 s to eliminate the thermal gradient prior to loading. The workpiece was then compressed to a 75% reduction in height at a strain rate of 0.1 s$^{-1}$.

2.5. Calculation Method

The analyses of the deformation, electromagnetic field, and thermal properties were intercoupled, and the heat generated by induction heating, as well as that arising from plastic deformation and friction, were taken into account during the temperature analysis. The deformation was calculated for the entire workpiece, and thermal analysis was calculated for the workpiece, HHR alloys, and ceramic tools. We performed the electromagnetic field analysis for the entire region, including the coil. Because the eddy current is generated only in the metal portion where the current flows, the resulting heat generated by the eddy current was reflected in the thermal analysis. During the analysis, the electric current for the induction heating was controlled by a PID controller. The mesh used for axisymmetric calculation of config. C is shown in Fig. 3. To reduce the calculation time, the calculation was performed using a 1/4 model. Figures 3(a) and 3(b) show the mesh used for electromagnetic field analysis and that used for thermal and deformation analysis, respectively. Because the volume ratio of the groove for glass lubrication is small, the influence on the measured stress is small; thus, the shape of the groove is not included in the FEM model. The effects of friction and heat transfer due to provision of grooves are taken into account.

An overview of the inverse analysis method is shown in Fig. 4.15,16) Initially, FEM calculations coupled with deformation, temperature, and electromagnetic field properties were conducted to reproduce the compression test. The flow stress used for the deformation analysis was given as a function of the temperature, strain rate, and some other parameters. In a previous study,21) to express the flow stress of a high-yield-point material such as the 720Li used in the present study, we proposed the following equations in which the initial strain $\varepsilon_0$ was introduced into dynamic-recrystallization-type equations:

\begin{equation}
\sigma = F_0 \left( \varepsilon_0 + \dot{\varepsilon}^\gamma \right)^{\nu} \left( \varepsilon \leq \varepsilon_c \right) \quad \text{.......................... (1)}
\end{equation}
where $\sigma$ is equivalent stress, $\varepsilon$ is equivalent strain, and $F_1, F_2, F_3, \varepsilon_0, n, a, \varepsilon_c, \varepsilon_{\text{max}}$ are material parameters. The independent parameters are $F_1, \varepsilon_0, n, a, \varepsilon_c$ when a continuity of the stress condition and its first- and second-order derivatives at $\varepsilon_c$ is applied. Appropriate values were substituted for the five independent parameters as initial values, and the FEM calculation was conducted using this function. The load obtained by FEM calculation using Eqs. (1)–(3) for flow stress is shown in Fig. 4(c). The load obtained through the experiment is shown in Fig. 4(b). The experimental and calculated loads are compared in Fig. 4(d), and the parameters of the flow-stress function are optimized to reduce the difference between the calculated and experimental values. The calculations for optimization were repeated until the difference between calculated and experimental values was reduced and an optimal flow-stress function was obtained. In the FEM calculation, the distribution of temperature and strain rate inside the workpiece was calculated. Equation (3) was used to compensate for the inhomogeneous distribution of the temperature $T$ and the strain rate $\dot{\varepsilon}$, where $A$ and $m$ represent the temperature and strain rate sensitivity, respectively. We assumed that $m = 0.1$ and $A = 8300 \text{ K}^{-1}$. These values were regressed from the stress at a strain of 0.05 for several strain-rate and temperature tests. Because this equation applies only around the target temperature $T_0$ and strain rate $\dot{\varepsilon}_0$, outcomes were better at narrower distributions of temperature and strain rate.

3. Results

3.1. Determination of Thermal Parameters

To obtain the thermal parameters, a preliminary experiment was conducted using the current four-turn coil. We conducted the experiments in config. A and config. B using HHR alloys. The four-turn coil arrangement used in both configurations is shown in Fig. 1(a). The results of the measured temperature are depicted by dashed lines in Fig. 5. Figures 5(a), 5(b), and 5(c) show the temperature of config. C. Mesh used for FEM calculation of config. C: (a) for electromagnetic field analysis; (b) for temperature and deformation analysis. (Online version in color.)
A, the temperature of config. B with mica, and the temperature of config. B with glass, respectively. Figure 5(a) shows that the temperature at the control point in config. A is 1000°C; however, the temperature decreases by 20°C or more at points 3 mm above and below the control point. By contrast, in config. B with mica and config. B with glass, where HHR alloys were used, the temperature difference from the control point is less than ~10°C.

Literature values were used for the thermal parameters of the materials. However, the heat transfer between the different materials should be determined experimentally so that the calculated temperature can be made to reproduce the experimental temperature through optimization of the thickness of the intermediate layer. Table 2 shows the thermal parameters. The specific heat, electric conductivity, and thermal conductivity parameters exhibit temperature dependence. Calculations were performed in consideration of their temperature dependence. The values in Table 2 are representative values for the parameters at 1000°C.

Calculation results obtained using the estimated thermal parameters are represented by the solid lines in Fig. 5. The figures suggest that the results closely match the experimental results and that the optimum calculations were performed.

3.2. Designing an Optimal Coil Arrangement

Experimental tests with the existing four-turn coil were conducted with a reduction of 75% using HHR alloys with a diameter of 15 mm and height of 6 mm. However, the workpiece after compression was observed to bulge from the HHR alloy when the workpiece was not located directly on the center axis. Performing a 75% compression test required an increase in the diameter of the HHR alloy. When the diameter of the HHR alloy was increased, the temperature of the HHR alloys might have been too high in the existing four-turn coil arrangement and might therefore have exceeded the maximum allowable temperature. In response, we designed the optimum coil using numerical calculations. The calculations were performed for several coil arrangements with mica used as the lubricant. Figure 6 gives a cross-sectional view of each coil layout, along with the temperature distribution when the control point was heated to 1000°C and maintained at this temperature for 400 s. With the existing four-turn coil, the temperature of the HHR alloys was observed to increase to approximately 1200°C. We then tested several arrangements in an attempt to suppress the temperature rise of the HHR alloys and maintain the workpiece temperature at 1000°C. The results presented in Fig. 6 show that suppression of the temperature rise of the HHR alloys was greatest when using a single-turn coil.

We performed simulations to determine the optimum winding radius of the single-turn coil for the HHR alloys with a 17 mm diameter and 5 mm height. The calculations...
were performed by varying the winding radius of the coil, which had a square cross section of 5 mm × 5 mm.

**Figure 7** presents the calculation results obtained when varying the winding radius of the single-turn coil. Figure 7(a) indicates the maximum temperature at the surface of the mid-height of the HHR alloys when the coil winding radius was varied from 17 to 29 mm. The maximum temperature recorded for the HHR alloy during heating (at the time of overshoot), as well as the temperature after holding for 400 s and just before processing, is shown in Fig. 7(a). The maximum and minimum temperatures of the workpiece and their difference just before processing, is shown in Fig. 7(a). The maximum and minimum temperatures of the workpiece and their difference just before processing, is shown in Fig. 7(a).

As the coil radius \( R \) [mm] was increased, the temperature of the HHR alloys also tended to increase. The temperature distribution analysis of the workpiece revealed that there was an optimum radius at which \( \Delta T \) was smallest. We found that the \( \Delta T \) value obtained was the smallest for the coil with a winding radius of 22 mm.

### 3.3. Examination Using a Single-turn Coil

The single-turn coil was designed as described in Section 3.2, the coil with a winding radius of 22 mm was constructed, and a compression test was performed.

#### 3.3.1. Comparison between Measured and Calculated Temperatures

**Figure 8** shows temperature properties before compression using the single-turn coil. The experimental results (dashed lines), together with the calculation results (solid lines), are given in Fig. 8. Figures 8(a) and 8(b) show the results for config. C with mica and config. C with glass, respectively. These figures show that the calculated results and the experimental results are approximately the same. The experimental results obtained using a single-turn coil show that the temperature asymmetry is reduced compared with those obtained using four-turn coil. In the case of the four-turn coil, the arrangement of the center axis of the HHR alloy strongly influenced the temperature because the HHR alloy was positioned very close to the coil. However, in the case of the single-turn coil, the influence on the temperature arising from the deviation of the axis was small.

For best results, the coil should be located at the center of the height. However, in the case of the four-turn coil, the number of coil turns is large and the arrangement becomes complicated; determining the optimal position is therefore difficult. Conversely, in the case of the single-turn coil, ensuring correct positioning is easy because the structure is relatively simple.

**Figure 9** shows temperature properties during compression. The calculation results are indicated on the left with the experimental results on the right. The experimental results tended to agree, the temperature drop measured during the experiment was set as the target temperatures for the calculation. The calculation results indicate that the internal temperature decreased with elapsed compression time, with

---

**Table 2.** Thermal parameters used in the calculations.

<table>
<thead>
<tr>
<th>Layer</th>
<th>Workpiece</th>
<th>HHR alloy</th>
<th>Tool</th>
<th>Mica*</th>
<th>Glass</th>
<th>Intermediate layer</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
<td>Udiment720Li</td>
<td>Nimowal</td>
<td>Si₃N₄</td>
<td>Mica*</td>
<td>Borosilicate glass*</td>
<td>–</td>
</tr>
<tr>
<td>Density ( \rho ) [kg m⁻³]</td>
<td>7 934</td>
<td>8 760</td>
<td>3 200</td>
<td>2 100</td>
<td>2 600</td>
<td>4 300</td>
</tr>
<tr>
<td>Specific heat capacity ( c ) [J kg⁻¹ K⁻¹]</td>
<td>888***</td>
<td>565***</td>
<td>710</td>
<td>880</td>
<td>880</td>
<td>790</td>
</tr>
<tr>
<td>Electric conductivity ( \sigma ) [S m⁻¹]</td>
<td>0.640***</td>
<td>0.595***</td>
<td>–</td>
<td>–</td>
<td>–</td>
<td>–</td>
</tr>
<tr>
<td>Thermal conductivity ( k ) [W m⁻¹ K⁻¹]</td>
<td>35.910***</td>
<td>22.4***</td>
<td>29.3</td>
<td>1.22</td>
<td>1.24</td>
<td>0.034 (before compression)</td>
</tr>
<tr>
<td>Heat transfer coefficient ( H ) [W m⁻¹ K⁻¹]</td>
<td>11</td>
<td>11</td>
<td>11</td>
<td>11</td>
<td>11</td>
<td>0.102 (during compression)</td>
</tr>
<tr>
<td>Thickness ( d ) [μm]</td>
<td>–</td>
<td>–</td>
<td>–</td>
<td>200</td>
<td>30</td>
<td>15 (b/w mica-metal)</td>
</tr>
<tr>
<td>–</td>
<td>–</td>
<td>–</td>
<td>–</td>
<td>–</td>
<td>3 (b/w glass-metal)</td>
<td></td>
</tr>
<tr>
<td>Radiation coefficient ( E )</td>
<td>0.78</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

* Natural mica: Muscovite KAl₂(AlSi₃O₁₀)(OH)₂
** Borosilicate glass (MATSUNAMI GLASS IND., LTD.)
*** Value at 1 000°C
For the results obtained for config. C with glass, the experimental and calculated results show similar values. The results suggest that, even though we used the experimentally measured temperature as the target temperature for the calculations, the timings of temperature increases and decreases of the HHR alloy were similar between the experimental and calculated results. The values measured at locations 3 mm above and below the control point of the workpiece varied, and some asymmetry was observed. In particular, after 7 s had elapsed from the start of compression, we observed a temperature difference between the upper and lower HHR alloys, which caused the temperature 3 mm above the control point of the workpiece to increase. The temperature decreased at the point located 3 mm below the control point. We found that the average value of the temperature measurements obtained from locations 3 mm above and below the control point of the workpiece were similar to the calculated result.

For the calculated results for config. C with mica, the temperature of the control point agrees with the experimental value because we set the experimental value as the target temperature of the control point in the calculation. However, the temperature of the HHR alloy differs substantially from the experimental results. In the experiment, the electric current was observed to approach zero immediately after the start of compression, whereas the electric current increased in the calculation; accordingly, the temperature of the HHR alloy also increased. As a result, the small peak of the HHR alloy temperature appeared. For the experiment with config. C with mica, the temperature rise was expected to be large in response to the deformation heat generated by the workpiece because the heat insulation capacity of the mica was large. However, for the calculations, because the heat insulation of mica was insufficient, the temperature rise could not be reproduced. In the calculation, because the experimental temperature of the control point was used as a target temperature of the control point, the temperature of the control point was adjusted by increasing the current; therefore, a small peak for the HHR alloy temperature was observed. We thus considered that the actual value for the thermal insulation may be higher than that used in the calculation. When the calculations were performed under the assumption that the mica has greater heat-insulating ability, the small peak of the HHR alloy disappeared but the temperature of the HHR alloy increased and the value obtained did not match the experimental value. For the calculations, the thermal parameter of the mica was estimated to be the

Fig. 7. Calculation results obtained by varying the winding radius of the single-turn coil: (a) maximum temperature at the surface of the mid-height of the HHR alloys during heating and just before processing and (b) maximum and minimum temperatures of the workpiece and their difference ($\Delta T$) just before processing.

Fig. 8. Temperature values obtained through experiments (dashed lines) and numerical calculations (solid lines): (a) config. C with mica and (b) config. C with glass. These configurations used a new single-turn coil.
same for all regions; however, this assumption may not be reasonable. For example, at the boundary where the mica is in contact with the HHR alloys, the mica did not completely adhere to the HHR alloy surface, possibly allowing heat to escape and contributing to the difference between the experimental and calculated values.

In addition, although we assumed the mica between the workpiece and the HHR alloys, as well as the mica between the HHR alloys and the tools, exhibited the same properties for purposes of calculation, the values may actually be different. We observed that the mica between the HHR alloy and the ceramic tools did not substantially change after the compression test, whereas the color of the mica positioned between the HHR alloys and the workpiece turned silver because of the high heat. Therefore, the thermal parameters of the mica may actually differ depending on its location.

Experimental results obtained for config. C with mica suggested that the temperature decreased at locations 3 mm above and below the control point of the workpiece. However, these results could not be reproduced by the calculations. As mentioned previously, the heat transfer coefficient between the HHR alloy and the mica sheets was set as a constant value for the calculations; however, because the end of the mica was not in complete contact with the HHR alloy, heat might have escaped. The highest temperature obtained in the calculation was at the corner of the HHR alloy on the coil side; however, in the actual experiment, these temperatures might not have been reached because of
the escaping heat. Additionally accurate measurements in an electromagnetic field are known to be difficult to obtain because of the thermocouple structure. Because the relative positional relationship with the coil changed in the points 3 mm above and below the control point, there might have been a problem with the measurement accuracy.

3.3.2. Compression Test Results

The top and side views of the workpiece after compression are shown in Fig. 10. The results obtained were 68%, 76%, and 73% reductions in height for config. A, config. C with mica, and config. C with glass, respectively. All of these configurations were targeted for 75% compression, but 68% compression was observed for config. A. In this configuration, the temperature of the end portion of the workpiece decreased; consequently, the load increased, the deflection of the apparatus increased, and the compression amount decreased.

From the side-view image of config. A, a barrel-shaped deformation is observed. In the side-view of config. C with mica, the curvature of the side surface is large and the barrel shape is also observed. By contrast, config. C with glass has a small curvature of the side surface and does not exhibit a barrel shape. The top views show a difference in Φ after deformation; that is, the diameter of the circle that was the contact surface of the cylinder before deformation changed. For config. C with glass, the grooves carved on the upper and lower surfaces of the cylinder spread over the entire surface. For config. C with mica, the contact surface of the cylinder did not spread as much and Φ was estimated to be approximately 11 mm. From the values of Φ, the friction coefficient μ was estimated using numerical calculation. We thus obtained values of μ = 0.18 for config. A and config. C with mica and μ = 0.02 for config. C with glass.

The experimental results for the load are depicted as dashed lines in Fig. 11(a). The load of the proposed configurations was lower than that of the conventional configuration. Config. C with mica and config. C with glass exhibited almost the same load; however, the load of config. C with mica increased in the region where the reduction was large.

We considered that the load fluctuations during compression were in accordance with the temperature fluctuations. The temperature increased immediately after the start of compression because of the heat from plastic deformation and the heat transferred from the HHR alloy, causing activation of the PID control. Because the PID control reduced the current, the temperature decreased. When the temperature at the control point decreased to less than the target temperature, the PID control increased the current, resulting in an increase in temperature. We found that the load value decreased in response to the temperature rise and increased in response to decreasing temperature. By comparing the temperature and load data obtained from the experiments, we confirmed that the timing of the temperature and load changes coincided with each other.

3.3.3. Inverse Analysis Results

The results of the flow stress are shown in Fig. 11(b). The dashed lines indicate the stress values simply obtained from the experimental load, which were calculated under the assumption that the deformed workpiece maintained a cylindrical shape. The solid lines indicate the flow-stress values obtained by inverse analysis. For the calculations, the friction coefficients estimated from the shapes measured in Section 3.3.2 were used. The independent parameters obtained by inverse analysis are shown in the upper three rows of Table 3. Because the material of the workpieces was the same for all three experimental configurations, the flow stress obtained using inverse analysis should agree among

![Fig. 10.](image-url)  
Top and side views of the workpiece after compression: (a) config. A (68% reduction), (b) config. C with mica (76% reduction), and (c) config. C with glass (73% reduction). (Online version in color.)

![Fig. 11.](image-url)  
Experimental results (dashed lines) and numerical calculation results (solid lines): (a) the load results and (b) flow stress obtained by experiments and calculations (inverse analysis). (Online version in color.)
the three configurations provided that the calculations were performed to correctly reflect the experimental conditions.

For config. A, the temperature was observed to decrease at the end of the workpiece with a large temperature distribution; thus, the compensation was not sufficient. Config. C with mica and config. C with glass showed different trends, though we observed both to have a small temperature distribution. In the case of config. C with glass, the stress decreased as strain increased. Conversely, config. C with mica showed less stress softening compared with config. C with glass.

4. Discussion

Images of the workpiece compressed by 60% are shown in Fig. 12. The workpieces of config. C with mica and config. C with glass are not barrel-shaped. The image of the 76% compressed config. C with mica shows that the contact surface of the original cylinder was not enlarged compared with that of the config. C with glass. However, at 60% compression, the shape of the config. C with mica sample was almost the same as that of the config. C with glass. In the images of the config. C with mica sample, the contact boundary of the original cylinder was found to occur approximately 1 mm inside the entire contact surface. To express the change in \( \Phi \) and the change in load simultaneously during compression, the friction coefficient was changed as follows: the value of the friction coefficient of mica was approximately the same as that of glass in the early stage of compression but then increased during compression. The friction coefficient has been reported to increase during compression.\(^{23-26}\) In some studies, the friction coefficient has been approximated by an exponential function.\(^{24-26}\) However, if an exponential function is applied to our case, in which the friction coefficient changes rapidly from the middle of compression, a higher-order exponentiation is required and the friction coefficient becomes infinite as strain increases. We propose Eq. (4) for the friction coefficient to express the observed phenomenon by a single formula without case classification. Using the Eq. (4), we performed inverse analyzes,

\[
\mu = \mu_{\text{fin}} - \mu_{\text{ini}} \frac{\text{erf} \left( \frac{x - x_{\text{mid}}}{t} \right) + \mu_{\text{fin}} + \mu_{\text{ini}}}{2} \quad \cdots \cdots (4)
\]

where \( x \) [%] is reduction, \( x_{\text{mid}} \) [%] is the medium reduction value at which the friction coefficient changes, \( \mu_{\text{ini}} \) is the initial friction coefficient, \( \mu_{\text{fin}} \) is the final friction coefficient, and \( t \) is a value relating the slope. Figure 13 shows the assumed friction coefficient for mica with respect to reduction. In this case, \( \mu_{\text{ini}} = 0.02, \mu_{\text{fin}} = 0.3, x_{\text{mid}} = 47, \) and \( t = 5. \)

The flow-stress results obtained by the inverse analysis are shown in Fig. 14(b); the flow stress of config. C with mica is in close agreement with that of config. C with glass. The independent parameters after the friction coefficient improvement obtained by inverse analysis are shown in the bottom row of Table 3. The load after inverse analysis is depicted as a solid line in Fig. 14(a). The load calculated using Eq. (4) was closer to the experimental load (dashed line) than the load calculated assuming a constant friction coefficient of \( \mu = 0.18 \), which is shown in Fig. 11(a). Under the assumption that the friction coefficient increases during compression for config. C with mica, the flow stress of config. C with mica obtained by the inverse analysis appeared to be more realistic.

The temperature distribution obtained by calculation at 68% compression is given in Fig. 15. Figures 15(a)–15(c) show contour maps for the same temperature range and indicate that the temperature distribution is very narrow for the proposed configurations compared with that of the conventional configuration. Figures 15(d) and 15(e) show the contour maps for proposed configurations when the temperature range was set to 17°C for detailed analysis.

| Table 3. Independent parameters obtained by inverse analysis. |
| --- | --- | --- | --- | --- |
| | \( F_1 \) | \( \varepsilon_c \) | \( n \) | \( \varepsilon_i \) | \( \varepsilon_{\text{ini}} \) |
| conventional | 546.1 | 0.3196 | 0.2030 | 0.0303 | 0.191 |
| glass | 165.7 | 2.159 | 1.209 | −0.202 | 0.270 |
| mica* | 166.5 | 2.306 | 1.089 | −0.308 | 0.368 |
| mica** | 165.7 | 2.205 | 1.183 | −0.272 | 0.265 |

* before improvement of friction coefficient
** after improvement of friction coefficient
As shown in Figs. 9(c) and 9(e), the internal temperature of config. C with mica did not decrease after compression, whereas the internal temperature of config. C with glass decreased slightly. Figure 15(e) also shows that the internal temperature of config. C with glass decreased slightly.

The strain distribution at 68% compression is depicted in Fig. 16. The strain distribution is large for config. A. The results obtained for config. C with mica demonstrate that the strain increases at the location where shear deformation occurs because of a sudden rise in friction. In the case of config. C with glass, the workpiece undergoes uniform deformation.

The equivalent strain rate obtained by numerical calculation with reduction is given in Fig. 17. The equivalent strain rate is expressed as a volume-average value, and the range of the standard deviation is indicated by the figure bars. The black line in the figure shows the strain rate determined during the uniaxial compression experiment. As shown in config. A in Fig. 17(a), the experimental axial strain rate decreases when the reduction exceeds 50%. In addition, the average value of the equivalent strain rate of calculation becomes lower than that of the axial one when the reduction exceeds 30%. Therefore, the decrease in the equivalent strain rate for config. A was found to be large. For config. C mica in Fig. 17(b), the result reflects the condition that the friction coefficient increased during deformation. These results suggest that, when the reduction exceeds 50%, the average value of the equivalent strain rate slightly increases and its distribution of equivalent strain rate substantially increases. In the initial stage of compression, the workpiece deformed uniformly with low friction. However, friction increased and the contact surface could not expand further, with folding occurring when the reduction exceeded 45%. This result is consistent with the high-strain region illustrated in Fig. 16(b). For config. C with glass in Fig. 17(b), the equivalent strain rate was approximately constant, and the average value of the strain rate of the entire workpiece matched that of the uniaxial compression test, indicating
that the deformation was uniform.

5. Conclusion

In this study, we performed an experiment at uniform temperature by placing high-heat-resistant alloys above and below a workpiece, thereby preventing heat from escaping to the ceramic tools. A uniform compression test was performed to large compression using a new single-turn coil arrangement designed using FEM coupled with deformation-temperature electromagnetic field that can reproduce the compression test. The following results were obtained.

1. The use of glass sheets as a lubricant greatly reduced the friction and enabled uniform deformation; thus, the flow-stress values obtained by the inverse analysis became extremely reliable.

2. For the proposed mica configuration, the friction coefficient was expected to increase during deformation from the geometry of the workpiece after a compression test. The friction coefficient was adjusted so that the calculated geometry of the workpiece coincided with the experimental geometry during compression. From the result of inverse analysis using a model assuming that the friction coefficient changed from 0.02 to 0.3, the flow stress of mica was almost consistent with that obtained when a glass sheet was used.

3. The flow stress of the conventional configuration obtained by inverse analysis differs from that of the proposed configuration in shape; however, the approximate values are not so different. Therefore, the conventional configuration can be used to approximate the flow stress.

Acknowledgment

This work was supported by the Council for Science, Technology and Innovation (CSTI) and the Cross-ministerial Strategic Innovation Promotion Program (SIP), “Structural Materials for Innovation” (mit no. B21, Development of Innovative Forging Process Technology and Construction of Material/Process Database with the Largescale and Precise Forging Simulator). The experimental material was provided by SIP as a common material for the project.

REFERENCES


