

Article

Investigation of Tribological Performance in Multi-Stage Cold Forging Using Combined Upsetting—Extrusion Type Tribotests

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Abstract

Multi-stage cold forging processes are widely adopted in actual industrial manufacturing of complex components. However, the influence of deformation history on lubrication behavior during such processes has not been fully clarified. This study investigates the friction coefficient at the die–workpiece interface in a multi-stage cold forging process consisting of upsetting followed by forward extrusion, with a focus on the lubrication performance of a zinc phosphate coating. Two types of multi-stage tribotests were conducted: one in which upsetting was followed by a forward rod–backward can extrusion type tribotest, and another in which upsetting was followed by a forward and backward can extrusion type tribotest. As a result, the evaluated friction coefficients were comparable, and their 99% confidence intervals overlapped. These values were higher than those obtained from the corresponding single-stage forward rod–backward can extrusion tribotest, indicating that prior upsetting affects the lubrication state during subsequent extrusion. Although localized galling and coating damage were observed, no extensive seizure occurred, suggesting that the lubricant coating retained a certain degree of effectiveness after upsetting. Furthermore, a comparison of the tribological conditions in each tribotest reveals that, although the contact pressure is at a comparable level, differences are observed in workpiece temperature, surface expansion ratio, and relative sliding velocity. Therefore, under the conditions of the present study, it is suggested that the effects of workpiece temperature, surface expansion ratio, and relative sliding velocity on the evaluated friction coefficient are limited.

Keywords: cold forging; extrusion; friction coefficient; zinc phosphate coating; tribotest

1. Introduction

Cold forging is widely used in industrial manufacturing due to its capability to produce near-net-shape components with high dimensional accuracy and high productivity. However, severe contact conditions characterized by high normal pressure and large sliding distances impose significant mechanical and thermal loads on forming tools, often resulting in premature fatigue failure and fracture. Therefore, lubrication plays a critical role in reducing friction and wear at the tool–workpiece interface. Phosphate-based coatings, particularly zinc phosphate coatings, have been extensively applied in cold forging because of their excellent lubricating performance. Since lubrication performance strongly influences die life, productivity, and product quality, quantitative evaluation of lubrication behavior is essential for process optimization [1]. Numerous studies have investigated lubricant



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performance, demonstrating the effectiveness of phosphate coatings and the feasibility of environmentally benign alternatives [2–5].

Reliable prediction of metal forming processes using finite element analysis (FEA) requires accurate frictional parameters. However, friction coefficients obtained from bulk metal forming tests should be regarded as apparent parameters because they depend on deformation mode, contact pressure distribution, surface enlargement, and the friction law assumed in the inverse analysis. Consequently, friction coefficients obtained from different testing methods do not necessarily represent a unique material property.

To evaluate friction under severe forming conditions, various tribological test methods have been developed, including the ring compression test, forward extrusion test, and combined forward–backward extrusion tests [6–11]. These tribotests can reproduce severe tribological conditions such as high contact pressure, large sliding distance, and significant surface expansion. By combining experimental measurements with inverse FEA, friction coefficients can be identified indirectly from the deformation behavior of the workpiece. Although these methods have been widely applied to determine friction coefficients in forging processes, most previous studies have evaluated friction under single-stage deformation conditions. Therefore, the identified friction coefficients generally represent the tribological behavior of an undeformed workpiece surface.

In contrast, multi-stage cold forging is widely employed for manufacturing complex components, in which the workpiece first undergoes preceding processes such as upsetting, followed by subsequent processes such as forward and backward extrusion. During these processes, the lubricant coating is repeatedly subjected to plastic deformation, surface expansion, frictional loading, and contact with forming tools. Consequently, the tribological conditions in later stages may differ significantly from those assumed in conventional single-stage tribotests. Therefore, understanding the evolution of lubricant performance and frictional conditions during multi-stage deformation is essential for improving the reliability of friction parameters used in process simulation and die design. Wang et al. [5] developed a forward extrusion-type tribotest involving bulging deformation and evaluated the lubrication performance of lubricant coatings under conditions considering multiple forming stages. Furthermore, Wang et al. [12] also developed a multi-stage tribotest combining upsetting and ironing processes and demonstrated changes in galling resistance and lubrication performance due to deformation history. However, the influence of the number of forming stages, namely the difference between single-stage and multi-stage forming conditions, on the friction coefficient has not been investigated.

Furthermore, extrusion processes, which are commonly employed as subsequent forming stages in multi-stage cold forging operations, can be broadly classified into forward extrusion and backward extrusion. Forward extrusion is characterized by large sliding distances and locally high contact pressures due to line contact between the billet end surface and the die. In contrast, backward extrusion is characterized by severe plastic strain and high surface expansion ratios at the billet end surface, resulting in demanding lubrication conditions that require excellent seizure resistance. Although many studies have discussed lubrication characteristics at the workpiece–punch interface during backward extrusion, lubrication behavior at the workpiece–die interface has received comparatively less attention [13,14]. Furthermore, little information is available regarding the evolution of frictional conditions at the workpiece–die interface after prior deformation processes such as upsetting. To the best of the authors' knowledge, few studies have quantitatively evaluated friction coefficients at the workpiece–die interface after prior upsetting deformation using multi-stage tribotests that simulate practical cold forging processes.

The objective of this study was to clarify the effect of a prior upsetting process on the friction coefficient at the workpiece–die interface during subsequent extrusion and

to compare the tribological behavior of forward and backward extrusion under identical pre-deformation conditions. The upsetting–extrusion sequence investigated in this study was selected because such process combinations are widely employed in practical cold forging operations, including bolt manufacturing. To achieve this objective, two types of multi-stage tribotests were conducted. In the first stage, an upsetting test was performed to introduce deformation history into the workpiece and lubricant coating. In the second stage, RC and WC tribotests, representing forward and backward extrusion conditions, respectively, were conducted. The friction coefficient was identified from the final workpiece geometry without direct load measurement, enabling simplified friction evaluation based on previously established calibration procedures [8,15]. Using this approach, the influence of prior upsetting and extrusion mode on frictional behavior at the workpiece–die interface was investigated.

2. Experimental Methods

2.1. Workpiece Material

Chromium alloy steel JIS SCr415H was used as the workpiece material in this study. Table 1 shows the chemical composition of the material used.

Table 1. Chemical composition of JIS SCr415H.

C	Si	Mn	P	S	Ni	Cr	Cu	Fe
0.23	0.26	0.81	0.016	0.016	0.02	1.13	0.02	Balanced

2.2. Lubricants

A zinc phosphate coating combined with a reactive soap layer (Gardo-system; Chemetall, Frankfurt am Main, Germany) was used as the lubricant coating. This coating has a three-layer structure, as shown in Figure 1, based on [16]. The upper soap layer, composed of unreacted soap and metal soap, provides lubrication performance, while the underlying zinc phosphate layer prevents direct contact between the workpiece and the die surface. To produce this multilayer coating, a zinc phosphate conversion treatment followed by a reactive soap treatment was applied. The main steps of the lubrication process are summarized in Table 2 [17].

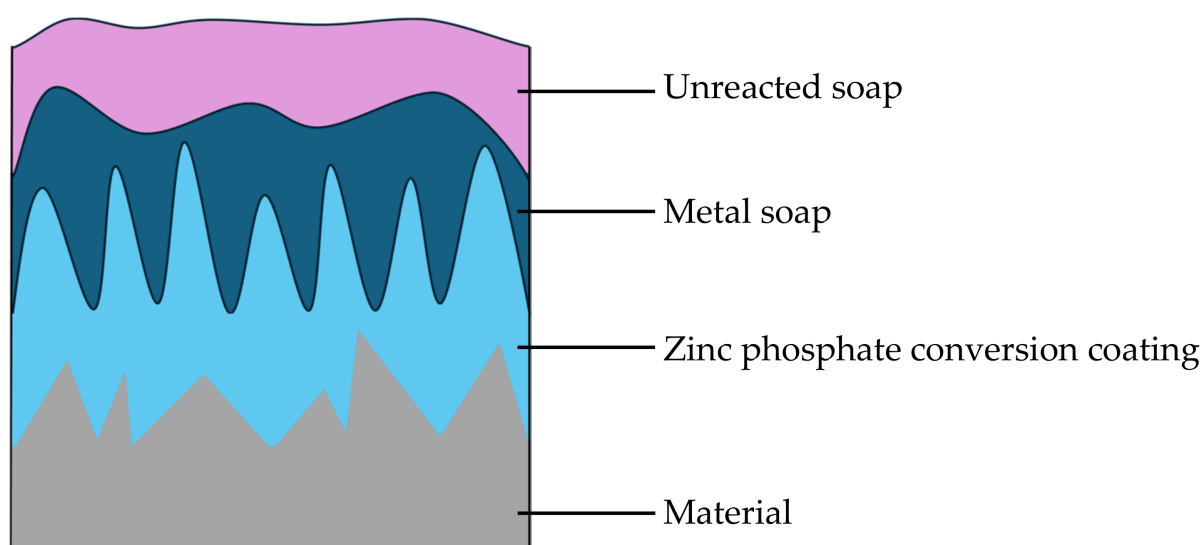


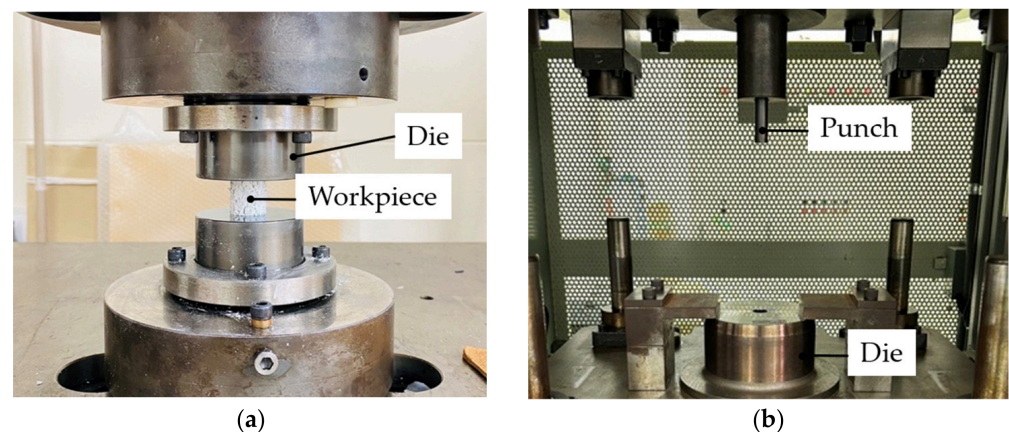
Figure 1. Schematics of the zinc phosphate coating [16].

Table 2. Application process of the lubricant system [17].

Step	Description
1. Cleaning	GardoClean 350, 50 g/L at 90 °C for 10 min
2. Pickling	Hydrochloric acid (15%) for 10 min
3. Activation	Gardolene V 6522, 2 g/L at 20 °C for 2 min
4. Phosphating	Gardobond Z 3190 at 65 °C for 6 min
5. Lubricant application	Gardolube L6176 (soap), 4%, at 85 °C for 5 min

2.3. Experimental Conditions of Multi-Stage Tribotests

In this study, two types of multi-stage tribotests were conducted to evaluate the effect of prior upsetting on subsequent extrusion. The UP–RC test consisted of upsetting followed by a combined forward rod–backward can (RC) extrusion type tribotest, which simulates the tribological behavior in forward extrusion. The UP–WC test consisted of upsetting followed by a combined forward-can and backward-can (WC) extrusion-type tribotest, which simulates the tribological behavior in backward extrusion. Figure 2 shows the experimental setup of the UP–RC and UP–WC tests. As shown in the figure, the experiments were conducted with the die, punch, and workpiece installed in the testing apparatus. The experimental conditions are summarized in Table 3. Detailed descriptions of the UP–RC and UP–WC tests are provided in the following sections. The purpose of introducing the upsetting stage prior to the RC and WC tests was to reproduce a typical multi-stage cold forging process and to evaluate the friction coefficient under conditions where the lubricant coating had already experienced deformation in its history. In practical multi-stage cold forging operations, lubricant coatings are subjected to deformation, surface expansion, and possible coating damage during preceding processes. Therefore, the upsetting stage was introduced to investigate friction behavior under more realistic conditions than those represented by conventional single-stage tribotests.

**Figure 2.** Experimental setup for the tribotests: (a) 1st stage; upsetting test; (b) 2nd stage; RC/WC test.**Table 3.** Experimental conditions for the UP–RC and UP–WC tests.

Testing Apparatus	Upsetting RC and WC Tests	Universal Testing Machine (UH-100A, Shimadzu Corp., Kyoto, Japan) Servomotor Press (NS1-1500, Aida Engineering Ltd., Sagami-hara Japan) SCr415H
Workpiece		Zinc phosphate coating (Gardo-system, Chemetall, Frankfurt am Main, Germany)
Lubricant		0.033
Punch speed for upsetting [mm/s]		20
Punch speed for RC and WC tests [spm]		RC test: 18, WC test: 16
Punch stroke [mm]		AlCrN (BALINIT ALCRONA PRO, Oerlikon Balzers, Balzers, Liechtenstein)
Coating of die and punches		

It is noted that only zinc phosphate-coated workpieces were employed in the present study. Uncoated workpieces were not tested because severe galling was expected to occur at an early stage of deformation under lubricant-free conditions, making a reliable evaluation of the friction coefficient difficult. It is noted that Wang et al. [18] employed DLC-coated tools for the friction tests conducted under lubricant-free conditions to suppress severe galling and enable friction evaluation.

2.4. UP–RC Test

Figure 3 shows a schematic illustration of the UP–RC test. The upsetting test in the first stage was carried out using UH-100A (Shimadzu Corp., Kyoto, Japan). Mirror-polished cemented carbide platens with an arithmetical mean roughness R_a , on the order of $0.01\ \mu\text{m}$ were used in the upsetting test. The initial dimensions of the workpiece were set to $d_0 = 17.4\ \text{mm}$ and $h_0 = 26\ \text{mm}$ for the following reasons. First, the height-to-diameter ratio of the initial workpiece was limited to 1.5 or less to prevent buckling during upsetting. In addition, the geometry of the workpiece after upsetting had to allow insertion into the die hole used in the subsequent RC test; $d_{\text{RC}} \leq D_{\text{RC}} = 20\ \text{mm}$ and $h_{\text{RC}} \approx H_{\text{RC}} = 20\ \text{mm}$. Based on these requirements, the target upsetting reduction ratio, $\{(h_0 - h_{\text{RC}})/h_0\} \times 100$, was set to 23%. It is noted that this study does not aim to establish a general relationship between the reduction ratio and the friction coefficient, but to evaluate the friction coefficient after prior upsetting under specific deformation conditions. The reduction ratio of 23% represented the maximum one under the present upsetting. Since larger deformation is expected to impose greater damage on the lubricant coating, the friction coefficient obtained in this study corresponds to the most severe condition attainable within the present experimental framework. A systematic investigation of the influence of reduction ratio on friction coefficient, including conditions with larger reduction ratios, will be one of the interesting open issues.

A taper angle of 10° and a height of 1.5 mm were introduced at the workpiece tip in order to prevent excessive adhesion and galling caused by full surface contact between the workpiece tip edge and the die.

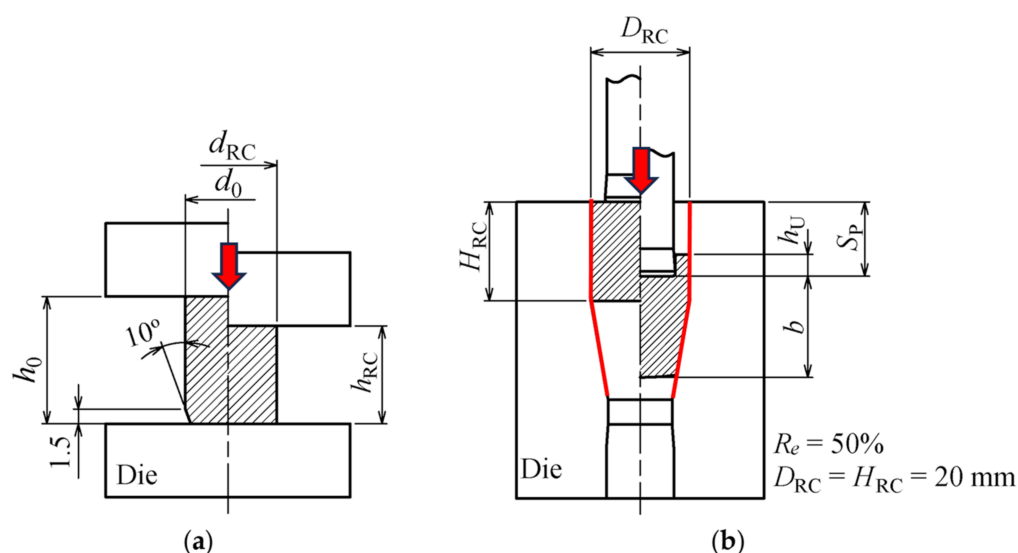


Figure 3. Schematics of the UP–RC test: (a) 1st stage; upsetting test; (b) 2nd stage; RC test [19]. The red arrows indicate the direction of stroke, and red lines in (b) indicate the area for identification of friction coefficients. Shaded part is the specimen before (left) and after (right) deformation.

The concept of the RC test was originally proposed by Nakamura et al. [19]. Figure 3b shows a schematic illustration of the RC test. It enables evaluation of the friction coefficient

μ_{RC} at the die tapered surface without direct load measurement. In this study, the pre-upset workpiece was inserted into a conical die inlet and was then compressed using a punch with a reduction in area $R_e = 50\%$. As the punch stroke S_p increases, forward extrusion occurs first. When the forward extrusion load, including the frictional resistance acting on the conical die surface, balances the backward extrusion load, backward extrusion subsequently occurs. Therefore, lower friction at the die–workpiece interface results in a larger forward extrusion length.

Calibration curves relating punch stroke, forward extrusion length b , and backward extrusion length h_U to μ_{RC} at the die surface were generated in advance using FEA. The experimentally obtained values were then plotted onto the calibration curves, and μ_{RC} was identified by interpolation. Details of the FEA procedure used to construct the calibration curves are described in the following section.

2.5. UP–WC Test

Figure 4 shows a schematic illustration of the UP–WC test. The workpiece dimensions were identical to those described in Section 2.4 for the UP–RC test, except that no taper was introduced at the workpiece tip, as shown in Figure 4a. The geometry of the workpiece after upsetting had to allow insertion into the die hole, as shown in Figure 4b, used in the subsequent WC test; $d_{WC} \leq D_{WC} = 20$ mm and $h_{WC} \approx H_{WC} = 20$ mm. Therefore, the required workpiece dimensions for the WC test were the same as those for the RC test in the present study.

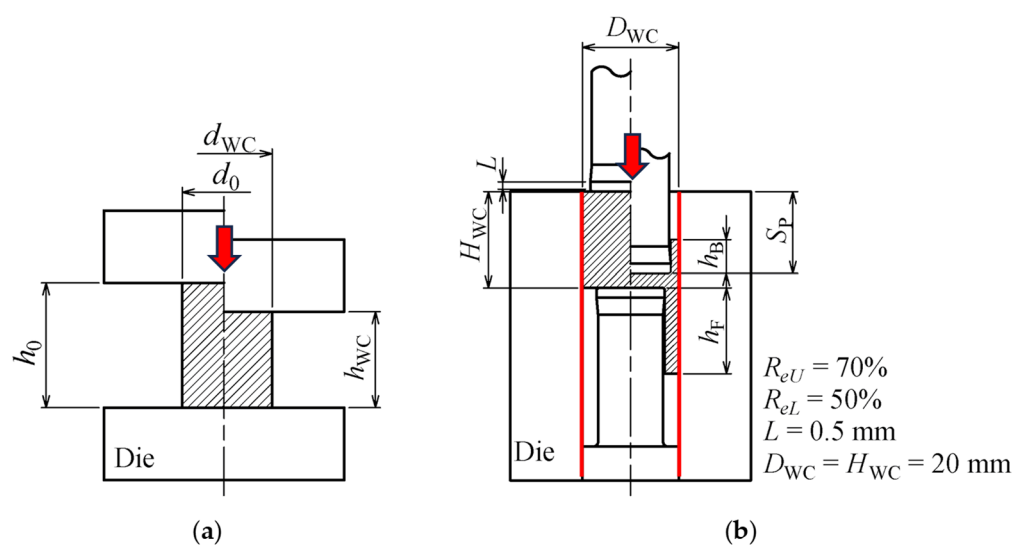


Figure 4. Schematics of the UP–WC test: (a) 1st stage; upsetting test; (b) 2nd stage; WC test [8]. The red arrows indicate the direction of stroke, and red lines in (b) indicate the area for identification of friction coefficients. Shaded part is the specimen before (left) and after (right) deformation.

In the WC test, frictional characteristics at the die–workpiece interface are evaluated from the lengths of forward and backward extrusion h_F and h_B based on Nakamura et al. [9]. As the influence of the friction coefficient μ_{WC} increases, the h_F decreases while the h_B increases. In this study, the upset workpiece was inserted into a die with an inlet diameter $D_{WC} = 20$ mm and height $H_{WC} = 20$ mm. To increase the sensitivity of the test to friction, the reduction in area of the backward can R_{eU} was set to 70%, whereas that of the forward can R_{eL} was set to 50%. This condition enhances the influence of μ_{WC} compared with the case in which both punches have identical reductions in area. The bearing length L was set to 0.5 mm for both punches so that the material flow is predominantly governed by friction acting on the die surface. The same FEA-based calibration procedure as that employed in

the RC test was used to identify μ_{WC} . The calibration curves were constructed using h_F and h_B and S_P .

2.6. Calibration Curve Construction and Determination of Friction Coefficient

In the present study, an upsetting test was first performed, followed by the RC and WC tests. Thus, μ_{RC} and μ_{WC} at the workpiece–die interface on the lateral surface of the workpiece were evaluated using a pre-deformed workpiece obtained by upsetting. Calibration curves were constructed using FEA that simulated the experimental procedures under the same conditions as the experiments. Since solid lubricant coatings, including zinc phosphate coatings, generally satisfy the Coulomb friction law over a wide range of contact pressure [2], the Coulomb friction coefficient was employed in the present analysis.

The FEA procedure for the upsetting is described below. To reproduce the post-upsetting workpiece dimensions in the FEA, the friction coefficient at the workpiece–die interface was calibrated to three decimal places so that the simulated post-upsetting geometry agreed with the experimental result. The analysis conditions are summarized in Table 4. The material behavior used in the FEA was defined by temperature- and strain-rate-dependent flow stress data generated using JMatPro 14. These data are shown in Figure 5 as equivalent stress–equivalent plastic strain curves.

The FEA was conducted as an axisymmetric model. Figure 6 shows the FE model used for the upsetting. As shown in Figure 6, the dimensions of the workpiece after upsetting were measured experimentally, and the friction coefficient was adjusted so that the maximum diameter of the simulated workpiece agreed with that of the experimentally obtained workpiece. This calibration was necessary to improve the accuracy of μ_{RC} and μ_{WC} identified in the subsequent RC and WC tests.

Table 4. Analysis conditions for upsetting tests.

FE Software	Simufact Forming 2025.3
Material model	Calculated by JMatPro 14
Die and Punch models	Rigid
Punch speed [mm/s]	0.033

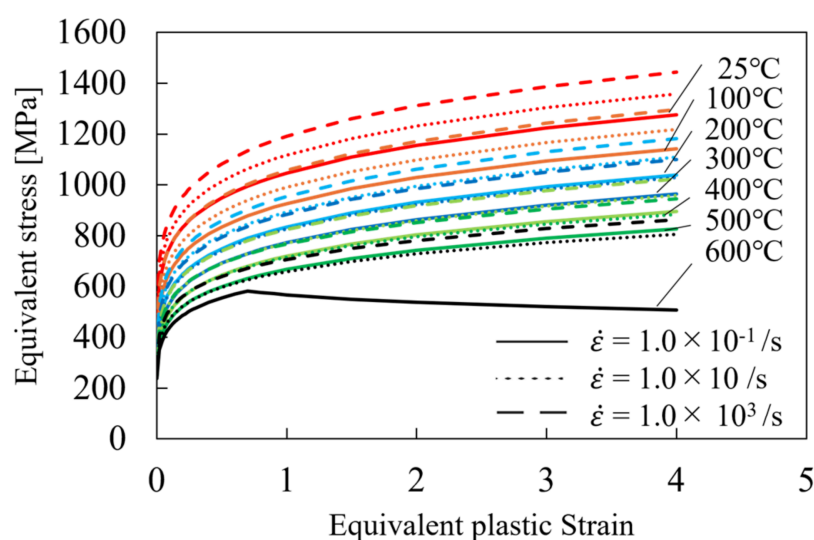


Figure 5. Strain rate and temperature-dependent equivalent stress–equivalent plastic strain curves (JIS SCr415H).

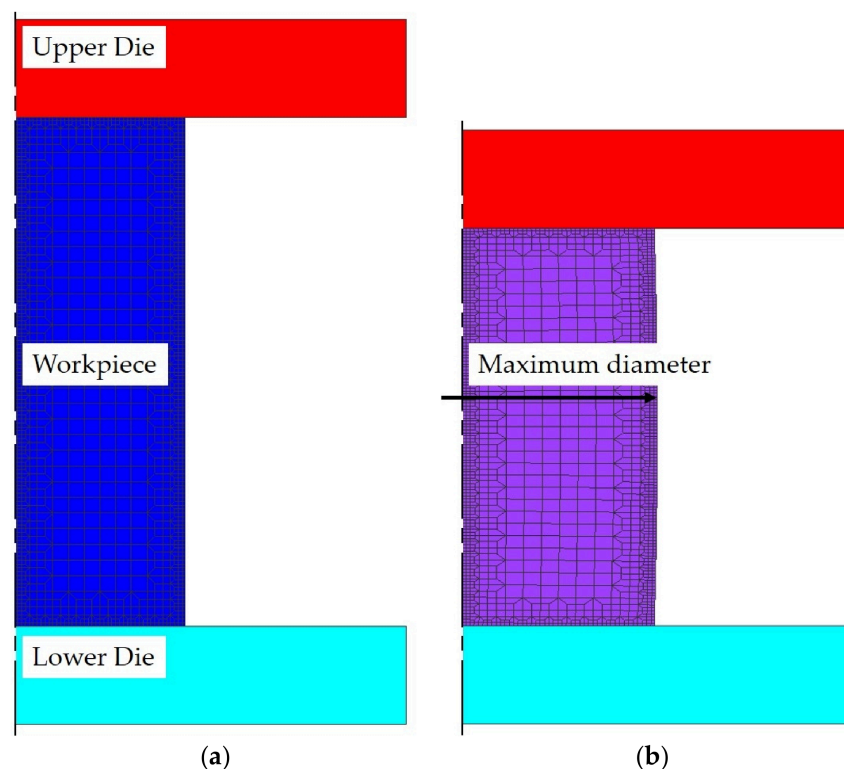


Figure 6. Finite element model used for the upsetting analysis: (a) Initial state; (b) After upsetting.

Next, FEA for the RC and WC tests are described. The workpiece model obtained from FEA of the prior upsetting was employed to the subsequent analyses. To construct the calibration curves used for friction evaluation, FEA were conducted by varying μ_{RC} and μ_{WC} and the S_p . The analysis conditions are summarized in Table 5.

Figure 7 shows the FEA models for the RC and WC tests, respectively. The models include the workpiece and die geometries, and the mesh discretization after the upsetting. The minimum mesh size employed in each analysis is summarized in Table 5. As shown in Figure 7, the upset workpieces exhibited barreling deformation and were subsequently inserted into the die cavities of the RC and WC tests. The workpiece was fully accommodated within the die cavity in the RC test, whereas the upper end of the workpiece protruded above the die surface in the WC test. Therefore, the top of the die was defined as the unified origin of S_p in both tests.

No external heating was applied during the experiments, and all tests were conducted at room temperature. The workpiece temperature change during forming was also evaluated by FEA, considering deformation-induced heat generation. Since the direct measurement of the workpiece temperature during the tribotests was difficult, the influence of temperature was discussed based on the FEA results.

Table 5. Analysis conditions for the RC and WC tests.

Die and Punch Models	Rigid
Punch stroke [mm]	RC test: 16.5, 17 and 17.5, WC test: 14, 15 and 16
Punch speed [spm]	20
Friction coefficient μ_{RC} and μ_{WC}	0.01, 0.03, 0.05 and 0.07
Press motion	Crank motion (slide stroke: 230 mm)
Minimum mesh size [mm]	RC test: 0.020, WC test: 0.035

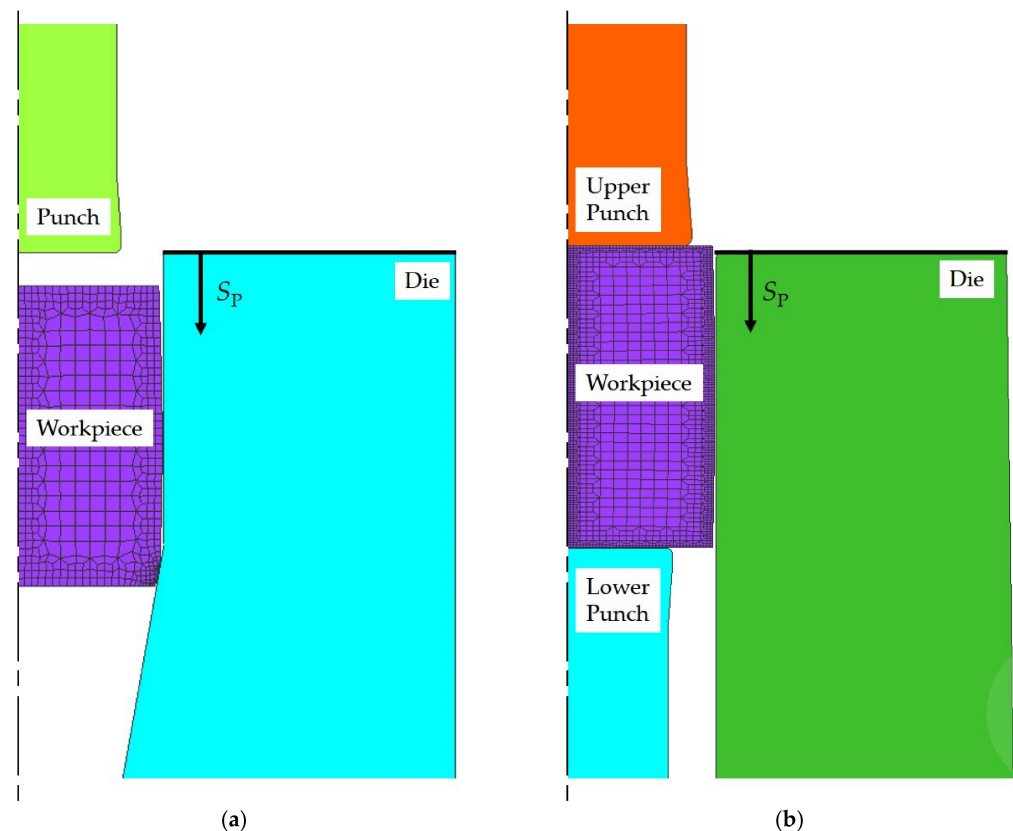


Figure 7. Finite element model used for the friction test: (a) RC test; (b) WC test.

2.7. Friction Coefficient Identification Procedure

The procedure used to identify the friction coefficient from the experimental results and the calibration curves is described below. The equations employed for the interpolation are also presented.

The interpolation method used for friction coefficient identification is explained with reference to Figure 8. In the present tribotests, the final workpiece geometry varies depending on the friction coefficient. As shown in Figure 9, the backward extrusion length h_U and the forward extrusion length b vary in the UP-RC test, whereas the backward extrusion length h_B and the forward extrusion length h_F vary in the UP-WC test.

Using this relationship between friction coefficient and workpiece geometry, the friction coefficient can be identified from the final workpiece geometry after the test.

The data obtained from the finite element analyses used to construct the calibration curves are denoted as (x_1, y_1) to (x_4, y_4) , where $x_1 = x_3$ and $x_2 = x_4$. The experimental result is denoted as $(x_{\text{exp}}, y_{\text{exp}})$, where $x_{\text{exp}} = x_a = x_b$. The values y_a and y_b correspond to the workpiece geometry values on the calibration curves for friction coefficients μ_1 and μ_2 , respectively, at the punch stroke of x_{exp} . Therefore, y_a and y_b can be calculated using Equations (1) and (2), respectively.

$$y_a = y_1 + \frac{y_2 - y_1}{x_2 - x_1} (x_{\text{exp}} - x_1) \quad (1)$$

$$y_b = y_3 + \frac{y_4 - y_3}{x_4 - x_3} (x_{\text{exp}} - x_3) \quad (2)$$

Finally, by using these values together with Equation (3), the friction coefficient μ corresponding to the experimental result can be identified.

$$\mu = \mu_1 + (\mu_2 - \mu_1) \frac{y_{\text{exp}} - y_a}{y_b - y_a} \quad (3)$$

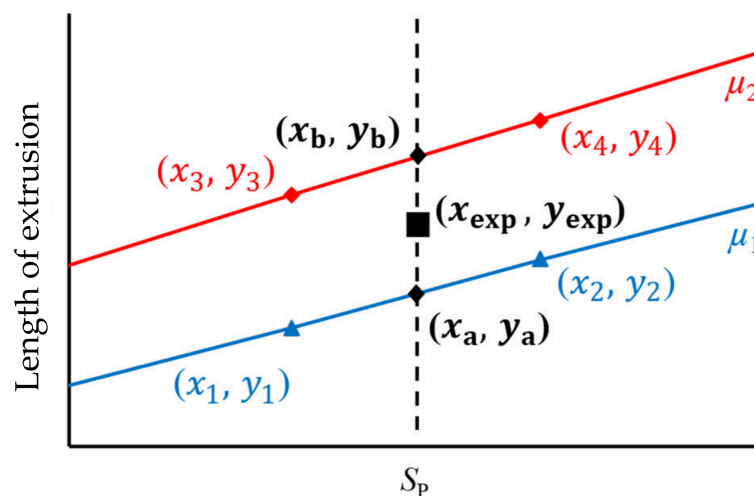


Figure 8. Interpolation method for friction coefficient identification.

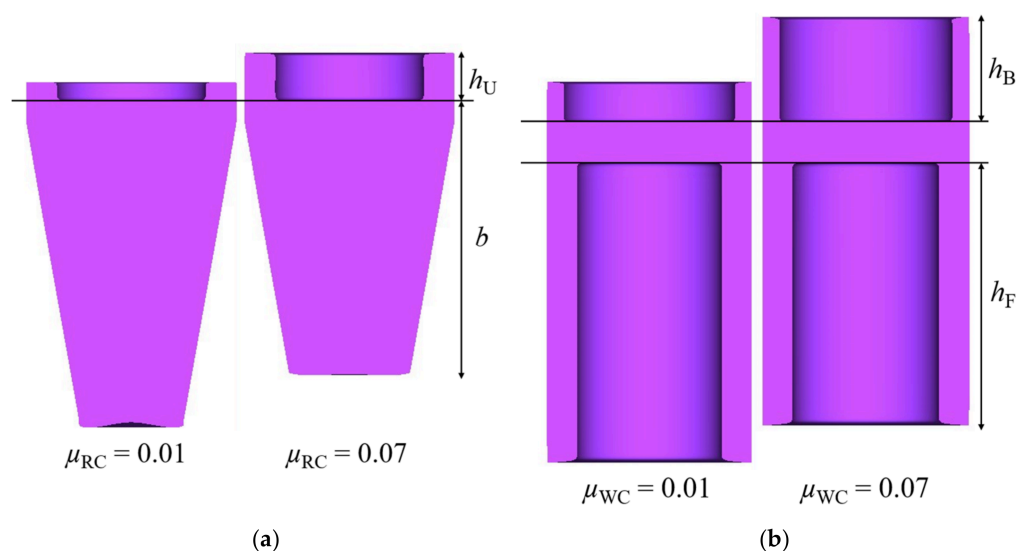


Figure 9. Influence of friction coefficient on workpiece geometry: (a) UP-RC test; (b) UP-WC test.

3. Results

3.1. Experimental Results for UP-RC Test

Figure 10 shows the experimental results plotted on the calibration curves. Nine tests were conducted. For clarity, only the average values of the experimental results are plotted in the figure by the black square symbol, and the error bars indicate the 99% confidence intervals. The experimentally obtained values were interpolated within the calibration curves to identify μ_{RC} . The average μ_{RC} values identified from h_U and b were 0.0437 and 0.0392, respectively, and their mean value, $\mu_{RC} = 0.0415$, was adopted as the representative friction coefficient. The standard deviation of μ_{RC} identified from h_U was 0.0018, and that identified from b was 0.0033. Details of the results from all nine tests are shown in Table A1 in Appendix A.

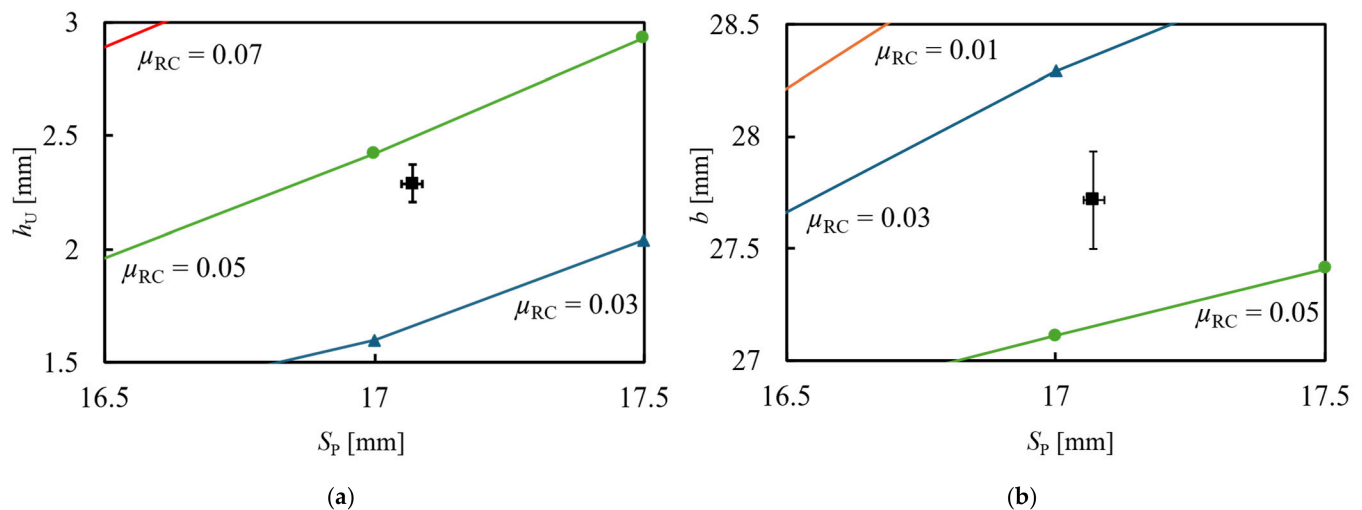


Figure 10. Friction coefficient identification by the UP-RC test: (a) From h_U ; (b) From b .

3.2. Experimental Results for UP-WC Test

Figure 11 shows the experimental results plotted on the calibration curves. Nine tests were conducted. As in the UP-RC test, only the average values of the experimental results are plotted in the figure by the black square symbol, and the error bars indicate the 99% confidence intervals. The experimentally obtained values were interpolated within the calibration curves to identify μ_{WC} . The average μ_{WC} values identified from the backward extrusion length h_B and the forward extrusion length h_F were 0.0399 and 0.0436, respectively, and their mean value, $\mu_{WC} = 0.0418$, was adopted as the representative friction coefficient. The standard deviation of μ_{WC} identified from h_B was 0.0042, and that identified from h_F was 0.0041. Details of the results from all nine tests are shown in Table A2 in Appendix A.

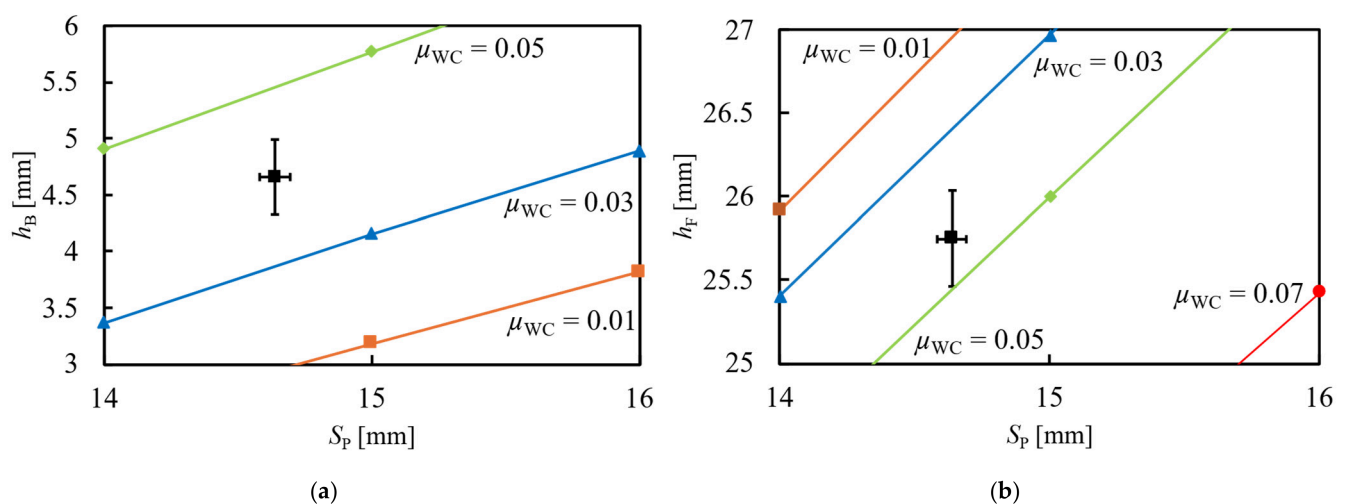


Figure 11. Friction coefficient identification by the UP-WC test: (a) From h_B ; (b) From h_F .

3.3. Comparison of Friction Coefficient

Figure 12 shows the average friction coefficients identified from the Single-RC, UP-RC and UP-WC tests together with the corresponding error bars. The range of the error bars represents the 99% confidence intervals. As shown in the figure, the evaluated friction coefficients were comparable, with overlapping 99% confidence intervals.

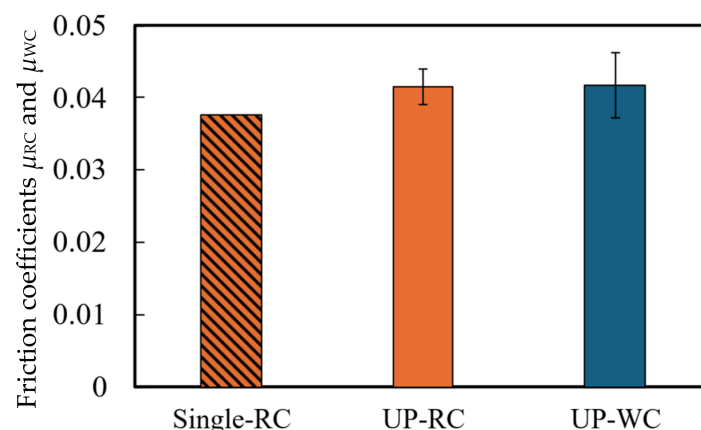


Figure 12. Comparison of the average friction coefficients identified from the Single-RC, UP-RC and UP-WC tests. Error bars represent the 99% confidence intervals.

4. Discussion

4.1. Effect of Prior Upsetting on Friction Coefficient in Subsequent Extrusion

The μ_{RC} without obtained without prior upsetting in the Single-RC test was reported as 0.0377 under identical testing conditions [17]. Therefore, μ_{RC} and μ_{WC} obtained from the multi-stage UP-RC and UP-WC tests were approximately 10% higher than that of obtained the Single-RC test. However, this observation should be regarded as a result obtained under the specific upsetting condition employed in the present study and should not be interpreted as a general relationship between upsetting reduction ratio and friction coefficient.

Although this comparison suggests a possible influence of prior upsetting on the friction coefficient, the available evidence is insufficient to draw a definitive conclusion because Single-WC results were not obtained in the present study. Therefore, further investigation, including Single-WC testing, is necessary.

One possible reason for this increase is the detachment of layers in the zinc phosphate coating during upsetting deformation. Figure 13 shows the workpieces before and after the upsetting test. As observed in Figure 13, the side surface of the workpiece was covered with a white powdery deposit after the upsetting test. This deposit is considered corresponding to the soap layer constituting the upper layer of the zinc phosphate coating, which precipitated on the surface during the upsetting process. The white powdery deposit was readily detached from the surface when the workpiece was transferred.

According to previous studies, a reduction in coating thickness of approximately 9.21 μm has been observed at a compression ratio of 45% [2]. In addition, surface enlargement of the lateral surface of the workpiece during upsetting likely contributes to the degradation of the zinc phosphate coating. Moreover, Wang et al. reported that severe galling occurred during a ball ironing test using workpieces subjected to upsetting at a compression ratio of 45% [20]. These findings indicate that upsetting deformation can deteriorate the lubricant coating.

To investigate the deterioration of the lubricant coating, SEM observations and EDS analyses were performed using a scanning electron microscope (SU3900, Hitachi High-Tech Corp., Tokyo, Japan). Figures 14 and 15 show the obtained SEM images and EDS elemental maps of the side surfaces of the workpieces before and after upsetting. Before upsetting, C was widely distributed over the workpiece surface as observed in Figure 14b, indicating that the substrate was covered with a carbon-containing lubricant layer. In contrast, after upsetting, Fe and Zn were observed over the surface as shown in Figure 15b,c, suggesting that the upper soap layer had been removed and that the underlying zinc phosphate layer and substrate had become exposed in some regions. These results suggest that upsetting

deformation caused not only the detachment of the unreacted soap layer constituting the upper layer of the zinc phosphate coating, but also partial removal of the underlying zinc phosphate layer, resulting in local exposure of the steel substrate. As a result, the subsequent extrusion processes may have been conducted under conditions where the zinc phosphate layer or the substrate was partially exposed, leading to an increase in the friction coefficient. Figure 16 shows the equivalent plastic strain distribution after upsetting obtained by FEA. The equivalent plastic strain at the surface observation point on the side surface of the workpiece, indicated by the solid square symbol in Figure 16b was 0.247. In addition to the deterioration of the lubricant coating, work hardening of the workpiece is also expected to occur as a result of the upsetting deformation. However, the influence of work hardening on the friction coefficient identified in the subsequent extrusion process could not be isolated and evaluated in the present study.

Although a direct quantitative comparison is difficult because Wang et al. evaluated friction using a friction shear factor rather than a Coulomb friction coefficient, their upsetting-extrusion tribotests also demonstrated that zinc phosphate coatings exhibited superior anti-seizure performance compared with an alternative coating after prior upsetting deformation [12]. The present results are therefore consistent with previous findings indicating that zinc phosphate coatings retain a certain level of lubrication performance even after deformation history and surface expansion.

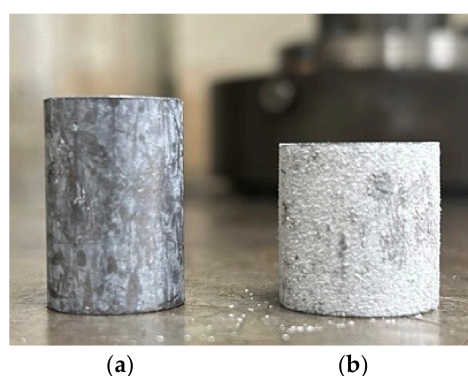


Figure 13. Appearance of workpiece: (a) Initial state; (b) After upsetting.

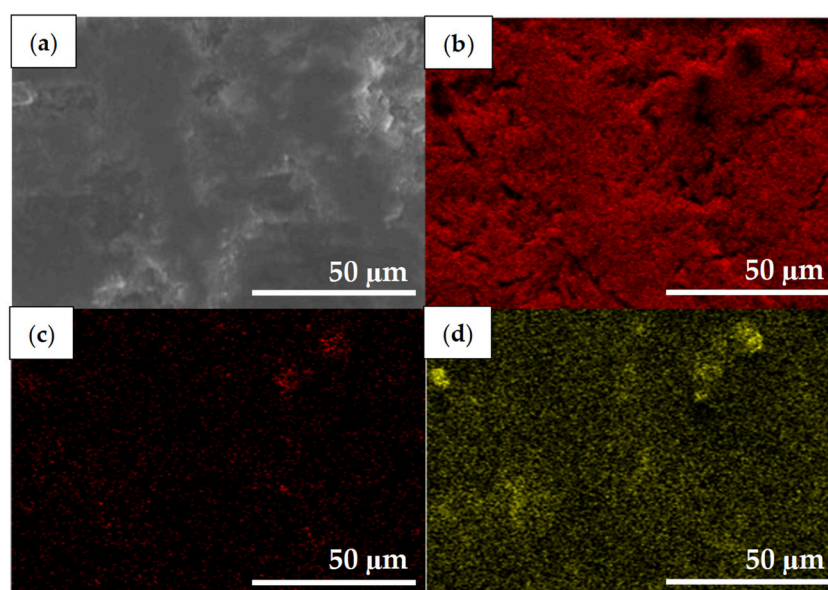


Figure 14. SEM image and corresponding EDS elemental maps of the workpiece before upsetting: (a) SEM image; (b) C distribution; (c) Fe distribution; (d) Zn distribution.

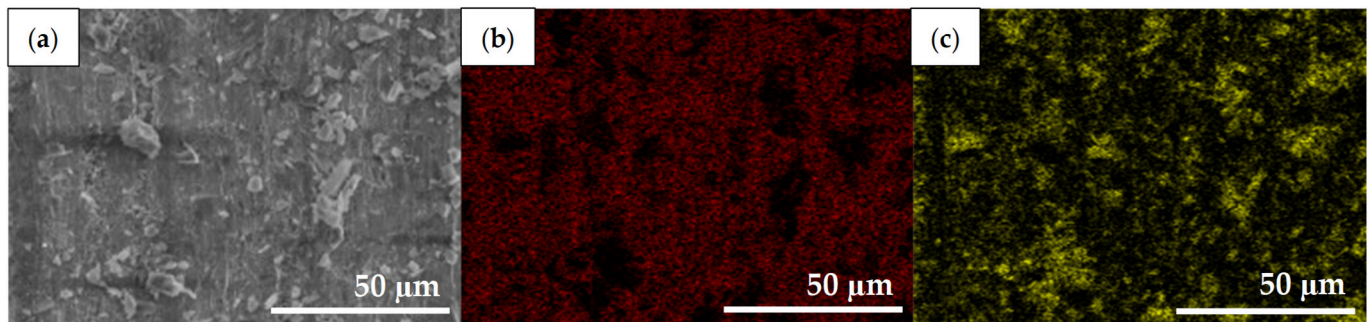


Figure 15. SEM image and corresponding EDS elemental maps of the workpiece after upsetting: (a) SEM image; (b) Fe distribution; (c) Zn distribution.

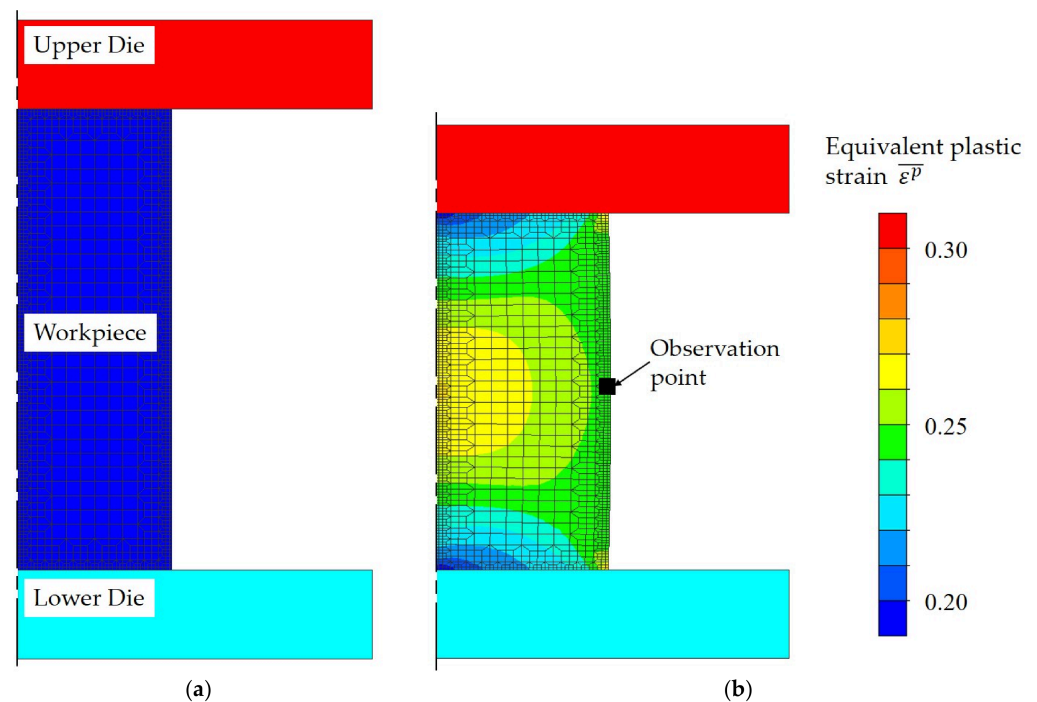


Figure 16. Equivalent plastic strain distribution: (a) initial state; (b) after upsetting.

4.2. Tribological Loading in RC and WC Tests

The friction coefficients obtained from the present experiments were used in the FEA. A comparative study of lubrication conditions in the two tribotests was conducted to characterize the features of each test and to evaluate the performance and lubrication mechanisms of lubricants in various extrusion-type forging processes.

Factors influencing the friction coefficient have been identified as tribological loads, including contact pressure, relative sliding velocity, deformation-induced heat generation, and surface expansion [17]. Furthermore, a friction modeling approach based on a feed-forward neural network (FFNN) has recently been proposed for cold forging processes, allowing simulations to account for five key factors affecting friction: contact pressure, relative sliding velocity, contact temperature, surface expansion, and sliding distance [21]. This approach is expected to establish a reliable method for predicting friction coefficients under various processing conditions.

In this study, the investigations of FEA results were conducted using the friction coefficients obtained experimentally to compare the lubrication conditions and mechanisms in the UP-RC and UP-WC tribotests, and to evaluate the impact of the extrusion mode on lubrication performance. Figure 17 shows the representative observation points for

tribological loading in the FEA. In the RC test, four representative locations were selected: the upper edge of the side surface of the initial workpiece (RC_1), the central region of the workpiece where the maximum diameter was attained due to barreling during upsetting (RC_2), and the start (RC_3) and end points (RC_4) of the tapered region in the initial workpiece. In the WC test, six representative locations were selected as shown in Figure 17 to evaluate tribological loading conditions.

RC_3 and RC_4 correspond to the regions where galling was observed after the tribotests. Therefore, the tribological loading conditions at these locations were investigated and compared with those at RC_1 and RC_2, which correspond to the upper and central regions of the workpiece, respectively. Similarly, WC_2, WC_3, and WC_4 correspond to the regions where metallic luster was observed after the tribotests. For comparison, WC_1, WC_5, and WC_6 were selected because characteristic tribological loading conditions were expected at the upper region, central region, and edge region of the workpiece, respectively.

Figure 18 shows the workpiece after the UP-WC test. In the WC test, a metallic luster was observed on the workpiece surface. Figures 19 and 20 show SEM images and EDS elemental maps of the metallic-luster and the non-metallic-luster regions on the workpiece surface after the WC test. Figures 19b, 19c, 20b and 20c show the distributions of Fe and Zn, respectively. In the metallic-luster region, Fe was distributed over a wider area than Zn, whereas in the non-metallic-luster region, Zn was distributed over a wider area than Fe. These results suggest that the metallic-luster region corresponds to an area where the substrate may have been exposed due to damage to the lubricant coating, while the surrounding non-metallic-luster region remained covered by the lubricant coating. This is attributed to a reduction in the coating thickness of the zinc phosphate coating, leading to partial exposure of the substrate material. However, no severe galling was observed. In the numerical analysis, these locations correspond to WC_2, WC_3, and WC_4.

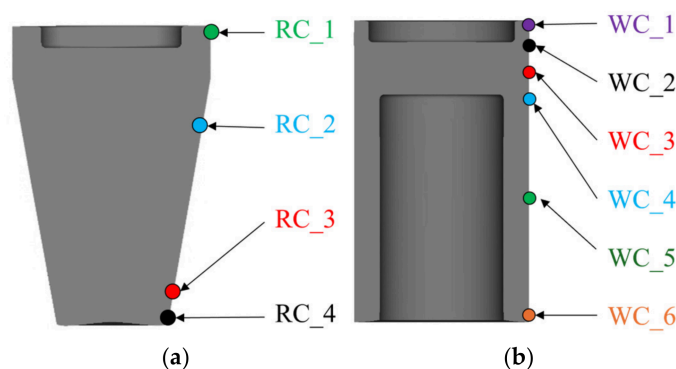


Figure 17. Workpiece measurement points: (a) UP-RC test; (b) UP-WC test.



Figure 18. Appearance of the workpiece after the UP-WC test.

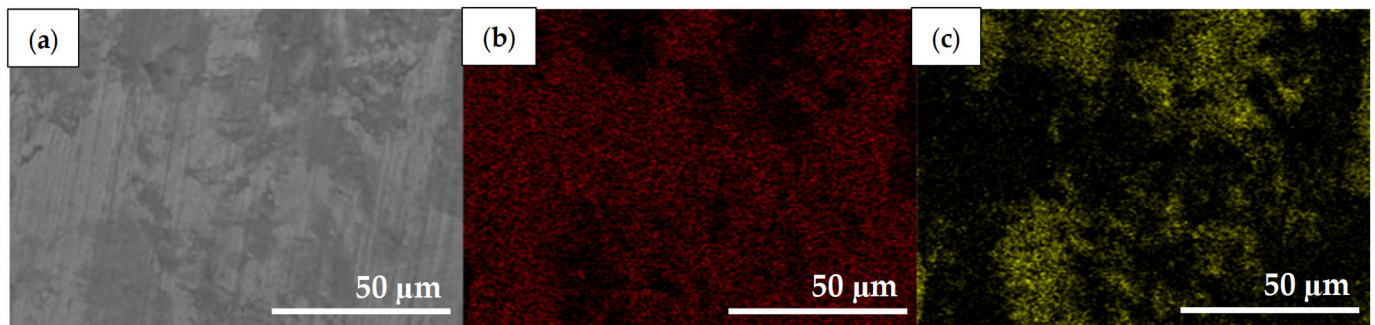


Figure 19. SEM image and corresponding EDS elemental maps of the workpiece area with metallic luster after the WC test: (a) SEM image; (b) Fe distribution; (c) Zn distribution.

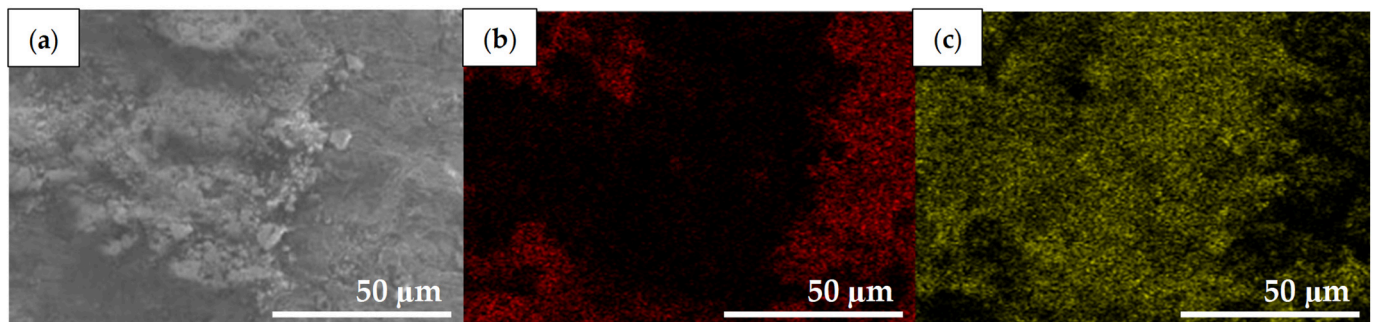


Figure 20. SEM image and corresponding EDS elemental maps of the workpiece area without metallic luster after the WC test: (a) SEM image; (b) Fe distribution; (c) Zn distribution.

4.3. Comparison of Contact Pressure

Figure 21a shows the relationship between punch stroke and contact pressure obtained from RC test analysis. As shown in Figure 21a, the maximum contact pressure reaches around 1800 MPa at RC_3, which corresponds to the onset of the tapered region. The maximum contact pressure was observed at the S_p of 2.52 mm, when the punch first came into full contact with the workpiece. Compared with other observation points, this region is subjected to a longer duration of high contact pressure, indicating that a large sliding distance is accumulated under high normal pressure conditions. The combination of high contact pressure and a large sliding distance is likely responsible for the galling in the tapered region. In contrast, although RC_2 was subjected to high contact pressure, galling is considered unlikely to have occurred because contact took place at a later stage.

The reason for the maximum contact pressure at the onset of the tapered region is attributed to the change in the workpiece geometry caused by the preceding upsetting process. The workpiece tip was initially machined with a taper angle of 10° to match the taper angle of the extrusion die. However, after upsetting, this angle increased to 18.8° owing to deformation of the workpiece. This increase in the taper angle leads to a transition from surface contact to line contact between the workpiece and the die at the onset of the tapered region, resulting in highly localized contact pressure and consequently promoting severe galling. In contrast, at other observation points, neither sufficiently high contact pressure nor long sliding distance was maintained, and therefore galling is not expected to have occurred.

Figure 21b shows the relationship between punch stroke and contact pressure obtained from the WC test analysis. As shown in Figure 21b, the maximum contact pressure of around 1600 MPa is observed at WC_4 at the S_p of 8.54 mm. Relatively high contact pressures are also observed at WC_2 and WC_3, indicating that these regions experienced

prolonged exposure to high-pressure contact conditions during forming. In the upper region of the workpiece where metallic luster was observed, the extended duration of high contact pressure is considered to have resulted in severe tribological conditions, leading to partial breakdown of the zinc phosphate coating. In contrast, in most regions represented by WC_5, continuous exposure to high contact pressure was not maintained, which is consistent with the absence of metallic luster.

Based on the analytical results of RC and WC tests, the effect of extrusion mode on contact pressure was evaluated. In forward extrusion, a characteristic feature is the localized high contact pressure observed in the tapered region. In contrast, in backward extrusion, a key distinction is that high contact pressure is observed at many measurement points, except near the workpiece edge, indicating that a large portion of the process is conducted under high-pressure conditions. Although there are differences in the duration of processing under high contact pressure, the maximum contact pressure reaches approximately 1600 MPa in both extrusion modes. Since the calculated friction coefficients are nearly identical, the similarity in maximum contact pressure may partly explain why comparable friction coefficients were obtained in the present study.

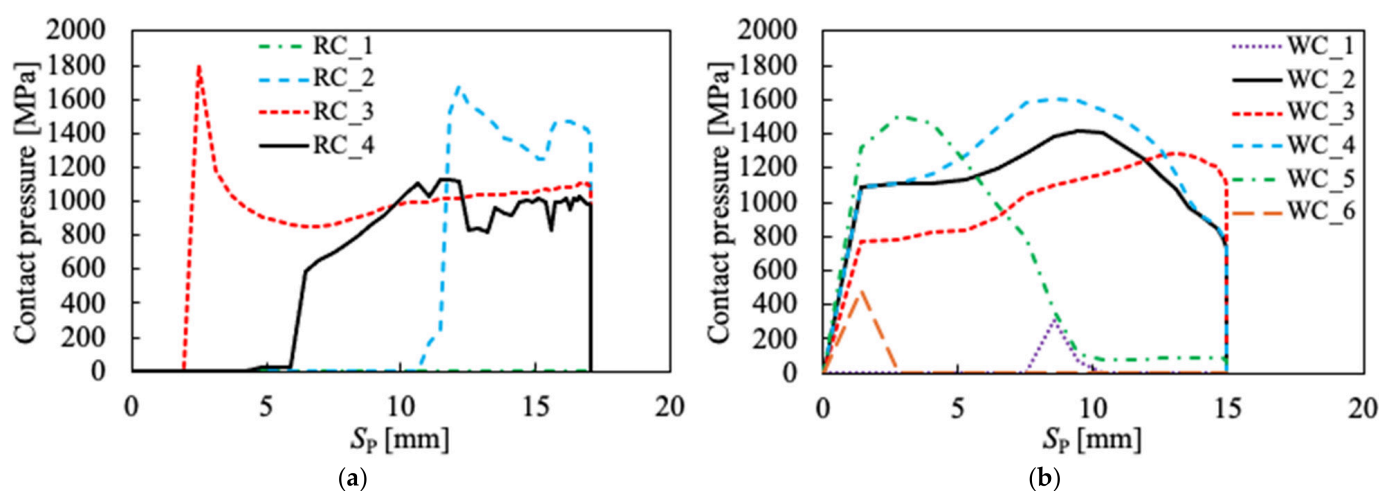


Figure 21. Contact pressure for tribotests: (a) UP-RC test; (b) UP-WC test.

4.4. Comparison of Workpiece Temperature

Figure 22a shows the relationship between punch stroke and workpiece temperature obtained from RC test analysis. As shown in Figure 22a, the maximum temperature rise of 258 °C is observed at RC_4. The temperature rise exceeds 200 °C at RC_3 and RC_4, indicating that these regions were subjected to severe thermomechanical loading conditions during forming.

The temperature on the side surface of the workpiece exceeding 200 °C suggests possible thermal softening of the zinc phosphate coating, which may have reduced its mechanical strength and promoted coating failure and delamination.

Figure 22b shows the relationship between punch stroke and workpiece temperature obtained from WC test analysis. As shown in Figure 22b, at WC_5, the temperature exceeds 240 °C from a punch stroke of 7.5 mm, reaching a maximum value of 283 °C. In addition, temperatures around 200 °C are also observed at other representative points, indicating that severe forming conditions were present throughout the process.

Based on the analytical results of RC and WC tests, the effect of extrusion mode on workpiece temperature was evaluated. A comparison of the results reveals that, in forward extrusion, workpiece temperature increases approximately linearly with the progression of the process, whereas in backward extrusion, a sharp increase in deformation-induced heat-

ing occurs at the final stage of the process. This difference is attributed to the characteristic features of each extrusion mode.

Regarding the maximum workpiece temperature, backward extrusion exhibits a larger value. In particular, a sustained high workpiece temperature is observed at WC_5 in the WC test, suggesting that backward extrusion imposes more severe thermal conditions. Backward extrusion exhibited higher and more sustained workpiece temperatures than forward extrusion. Nevertheless, despite this clear difference in thermal conditions, the evaluated friction coefficients remained nearly identical, indicating that workpiece temperature was not identified as the dominant factor controlling friction in the present study.

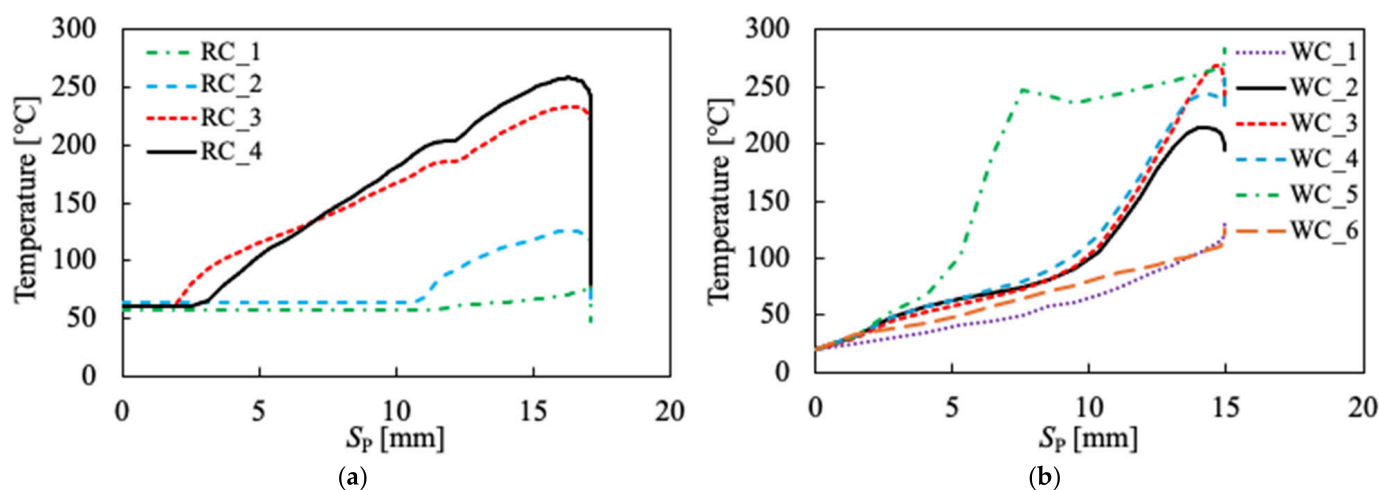


Figure 22. Workpiece temperature for tribotests: (a) UP-RC test; (b) UP-WC test.

4.5. Comparison of Surface Expansion

Figure 23a shows the relationship between punch stroke and surface expansion obtained from RC test analysis. The surface expansion was calculated as $((A - A_0)/A_0) \times 100$ [%], where A_0 and A represent the surface areas before and after deformation, respectively. As shown in Figure 23a, the surface area increases significantly at RC_3 and RC_4, which correspond to the onset and end of the tapered region. At these points, the surface area becomes approximately 30% larger than that in the initial state before upsetting. In addition, at the end of the tapered region, repeated local expansion and contraction of the surface area is observed. Such behavior suggests that regions where the upper soap layer of the zinc phosphate coating detached during the upsetting process were subsequently subjected to further expansion during forward extrusion, which may have contributed to an increase in the friction coefficient.

Figure 23b shows the relationship between punch stroke and surface expansion obtained from WC test analysis. As shown in Figure 23b, pronounced surface expansion is observed particularly at WC_3. The surface expansion increases sharply after the punch stroke exceeds 13 mm, reaching a maximum value of 443% at the final punch position. In addition, surface expansion of approximately 100% is also observed at WC_2, WC_4 and WC_5. This indicates that the region where metallic luster was observed experienced severe surface expansion, leading to coating breakdown and exposure of the substrate material.

Based on the FEA results of RC and WC tests, the influence of extrusion mode on the surface expansion was evaluated. A comparison of the results clearly indicates that backward extrusion exhibits a significantly larger surface expansion than forward extrusion. While backward extrusion is generally characterized by large surface expansion at the end face of the workpiece, similarly large surface expansion was also observed on the lateral surface of the workpiece. This represents the most pronounced difference between

forward and backward extrusion. Surface expansion was significantly larger in backward extrusion, reaching more than 400% in the specific regions. Although such severe surface expansion promoting coating damage and substrate exposure is predicted, its influence on the evaluated friction coefficient appears to be smaller than expected under the present testing conditions.

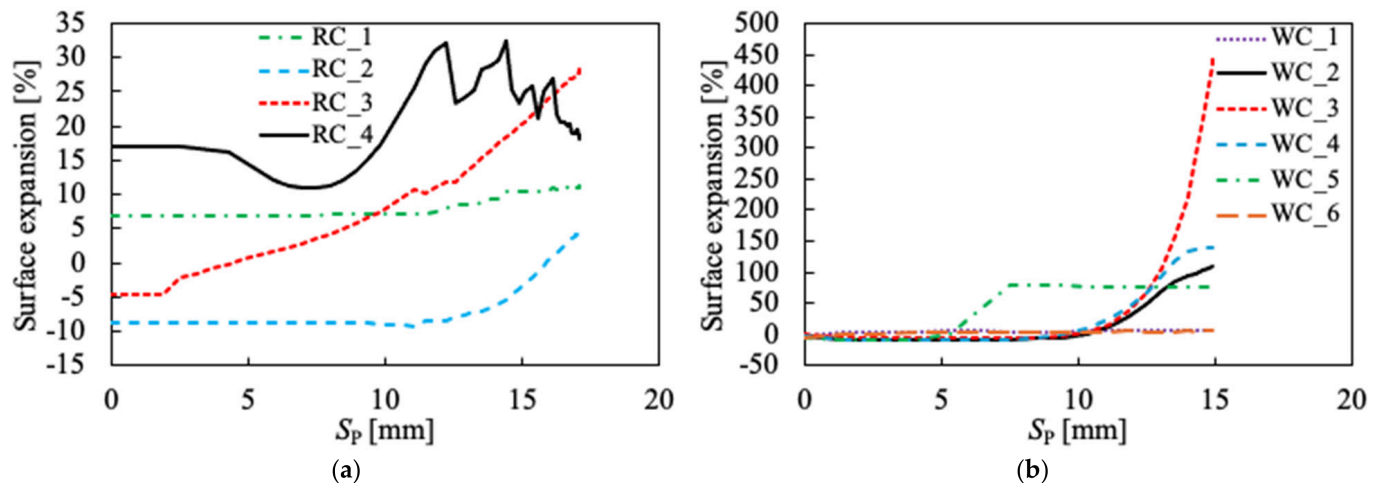


Figure 23. Surface expansion for tribotests: (a) UP-RC test; (b) UP-WC test.

4.6. Comparison of Relative Sliding Velocity

Figure 24a shows the relationship between punch stroke and the relative sliding velocity of the workpiece obtained from RC test analysis. As shown in Figure 24a, the variation in sliding velocity at different observation points is relatively small. This is attributed to the fact that material flow in the forward extrusion stage is predominantly governed by the punch velocity. The maximum relative sliding velocity reaches 145 mm/s at the end of the tapered region.

Figure 24b shows the relationship between punch stroke and relative sliding velocity obtained from the WC test analysis. The maximum value of 247 mm/s is observed at WC_6, where material is significantly extruded in the forward direction by the punch. In contrast, the relative sliding velocity at the other observation points remains in the range of approximately 100–120 mm/s.

Based on the analytical results of RC and WC tests, the influence of extrusion mode on the relative sliding velocity was evaluated. A comparison of the results shows that the maximum value is higher in backward extrusion. However, the maximum value is observed at WC_6, where the contact pressure is significantly lower than at other locations. Therefore, the contribution of WC_6 to the lubrication characteristics is considered to be limited. Except for WC_6, it is found that a high relative sliding velocity appears only at WC_5 in backward extrusion. In contrast, forward extrusion exhibits a high relative sliding velocity over a large portion of the workpiece's side surface.

This behavior represents a key characteristic of forward extrusion, indicating that, from the viewpoint of relative sliding velocity, forward extrusion imposes more severe lubrication conditions. However, these differences were not reflected in the evaluated friction coefficients, suggesting that relative sliding velocity was not identified as the primary factor governing friction in the present experiments.

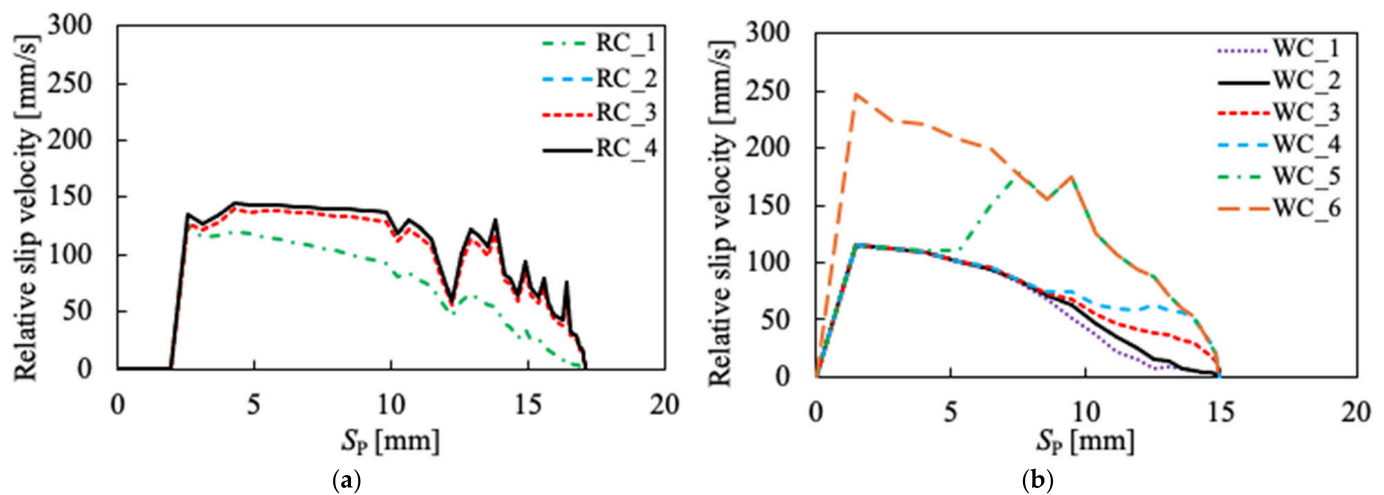


Figure 24. Relative sliding velocity for tribotests: (a) UP-RC test; (b) UP-WC test.

4.7. Effect of Tribological Loading on the Friction Coefficient

Based on the experimental and analytical results of the UP-RC and UP-WC tests, the influence of tribological loading on the friction coefficient has been investigated. The present findings suggest that, under the test conditions employed, forming heat generation, surface expansion ratio, and relative sliding velocity are not primary factors affecting the friction coefficient. However, the similarity in the calculated coefficients of friction for the UP-RC and UP-WC tests may be attributed to the combined effects of multiple tribological loads. In other words, in this study, it was not possible to clearly isolate and evaluate the individual contributions of each tribological loading factor.

One possible reason for this limitation is that the condition and degradation state of the lubricant coating could only be evaluated qualitatively in the present study. Further characterization of the lubricant coating would enhance the understanding of the tribological behavior observed in the present study. In particular, quantitative evaluation of the surface morphology and metallographic examination of coating cross sections would provide additional insight into the condition and integrity of the coating after deformation. Such information could also support more robust verification of the proposed methodology and contribute to a deeper understanding of coating degradation during multi-stage forming processes. Although these analyses were not included in the present work, they represent an important direction for future research.

5. Conclusions

1. In this study, the UP-RC and UP-WC tests, which reproduced multiple forming processes, were conducted to evaluate the friction coefficient between the workpiece and the die. The friction coefficients identified from the UP-RC and UP-WC tests were both approximately 0.04, indicating nearly identical values.
2. The results showed that the friction coefficient between the workpiece and the die was higher in the multi-stage conditions than in the single-process conditions. When a zinc phosphate coating was used as the lubricant and upsetting was performed at a reduction ratio of 23%, the friction coefficient determined from the RC test increased by approximately 10%. Surface observations suggested deterioration of the lubricant coating after upsetting, and this deterioration may have contributed to the increase in the friction coefficient.

3. In the UP–RC test, localized coating failure may have occurred due to the combined effects of high contact pressure and long sliding distance, leading to partial breakdown of the lubricant film.
4. In the UP–WC test, a metallic-luster region was observed on the workpiece surface. Surface observations suggest that severe surface expansion may have led to deterioration of the lubricant coating, resulting in a partial exposure of the substrate material and the formation of a metallic-luster region.
5. Significant differences in tribological loading, including workpiece temperature, surface expansion ratio, and relative sliding velocity, were observed between forward and backward extrusion. Nevertheless, under the present test conditions, no distinct change in the friction coefficient attributable to these differences was identified. This finding suggests that, within the range of the present test conditions, workpiece temperature, surface expansion ratio, and relative sliding velocity were not identified as dominant factors governing the friction coefficient.

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Appendix A

Table A1. Experimental results for the UP–RC test.

Exp. No.	$S_{P_nominal}$	S_P	h_U	b	μ from h_U	μ from b	Avg. μ
1	18	17.08	2.41	27.41	0.0461	0.0443	0.0452
2	18	17.06	2.40	27.55	0.0465	0.0418	0.0442
3	18	17.06	2.30	27.91	0.0442	0.0360	0.0401
4	18	17.09	2.16	27.67	0.0402	0.0403	0.0403
5	18	17.09	2.30	27.50	0.0434	0.0430	0.0432
6	18	17.09	2.30	27.71	0.0435	0.0396	0.0416
7	18	17.04	2.24	27.94	0.0430	0.0352	0.0391
8	18	17.06	2.21	27.99	0.0419	0.0347	0.0383
9	18	17.06	2.30	27.78	0.0440	0.0381	0.0411
Average		17.07	2.29	27.72	0.0437	0.0392	0.0415
Standard Deviation		0.0170	0.0759	0.2052	0.0018	0.0033	0.0022
99% CI	Lower	17.05	2.21	27.50	0.0416	0.0356	0.0390
	Upper	17.09	2.37	27.93	0.0457	0.0429	0.0439

Table A2. Experimental results for the UP–WC test.

Exp. No.	$S_{p_nominal}$	S_p	h_B	h_F	μ from h_B	μ from h_F	Avg. μ
1	16	14.68	4.50	25.98	0.0375	0.0401	0.0388
2	16	14.70	4.36	26.17	0.0354	0.0368	0.0361
3	16	14.68	4.42	25.99	0.0364	0.0399	0.0382
4	16	14.60	4.45	25.75	0.0377	0.0424	0.0401
5	16	14.66	4.76	25.73	0.0409	0.0448	0.0429
6	16	14.67	4.31	25.81	0.0352	0.0435	0.0393
7	16	14.54	4.98	25.45	0.0450	0.0467	0.0459
8	16	14.60	4.95	25.50	0.0440	0.0477	0.0459
9	16	14.61	5.18	25.36	0.0467	0.0508	0.0488
Average		14.64	4.66	25.75	0.0399	0.0436	0.0418
Standard Deviation		0.0496	0.2993	0.2578	0.0042	0.0041	0.0040
99% CI	Lower	14.58	4.32	25.45	0.0352	0.0390	0.0372
	Upper	14.69	4.99	26.04	0.0445	0.0483	0.0463

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