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Abstract: Worldwide, at least twenty different tribological tests have been proposed for the empirical determination of friction coefficients in cold forging processes. Due to the varying test setups, means of measurement, and level of abstraction, the comparability of the outcomes is, however, disputable. Within this work, six established test principles are compared using identical tribological systems. Large differences between the empirically determined friction coefficients are observed but can be explained under consideration of the respective tribological loads. Additional investigations of an extrusion process reveal that friction models also have to take into account the varying local thickness of the lubricant film.

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Friction coefficients in cold forging: a global perspective

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Worldwide, at least twenty different tribological tests have been proposed for the empirical determination of friction coefficients in cold forging processes. Due to the varying test setups, means of measurement, and level of abstraction, the comparability of the outcomes is, however, disputable. Within this work, six established test principles are compared using identical tribological systems. Large differences between the empirically determined friction coefficients are observed but can be explained under consideration of the respective tribological loads. Additional investigations of an extrusion process reveal that friction models also have to take into account the varying local thickness of the lubricant film.

Friction, Cold Forming, Tribology

1. Introduction

Numerical simulations are essential for the efficient design of modern process chains. The quality of the herewith gained results depends heavily on the input parameters of the numerical model. These input parameters refer mainly to the description of material as well as frictional behaviour. Both groups of parameters are preferably determined empirically beforehand with the help of model tests. Whereas the process of gaining material modelling parameters has been widely standardized, the determination of frictional coefficients is still carried out heterogeneously. No universally agreed upon test procedure has been established to characterize friction in cold forging operations. Thus, at least 20 different tribotests for cold forging have been proposed. Actually, the development of new friction tests is an ongoing topic of research [1]. Due to the differing test setups, means of friction measurement, and level of abstraction, the comparability of the determined friction coefficients is highly disputable. Yet, no empirical comparison of the most commonly used tests is available to the knowledge of the authors. Benchmark studies of sheet metal forming friction tests have shown that large deviations between the determined friction coefficients exist for different friction tests [2]. With friction being a relevant system parameter that affects the result of the numerical simulations of cold forging operations substantially [3], a systematic comparison of friction tests and their outcomes is of high significance.

Within this paper, six established friction tests for cold forging operations are used to determine friction coefficients (Amontons-Coulomb) of one state of the art industrial tribosystem. The setups of the friction tests are designed according to a reference forming operation. Consistency in between the studied tests is ensured by reproducing the tribological loads of the reference forming operation within each of the frictional tests. The factored tribological loads comprise the contact normal stress, surface enlargement, relative sliding velocity, and temperature at the interface. However, due to the individual frictional test setups, not all of the mentioned tribological loads can be set independently [4]. Thus, accompanying numerical analyses of the frictional tests and the reference forming operation are used to gain additional insight into the impact of the tribological loads.

2. Experimental and numerical procedure

The reference forming operation is a single stage extrusion process [5] as depicted in Figure 1 (a and b). All specimens consist of a case hardening steel (16MnCrS5, DIN 1.7139, SAE 5115). The flow curves (see Figure 1 (c)) are determined empirically by cylinder upsetting with strain rates of $\dot{\varphi} = 0.1$ and $\dot{\varphi} = 1$ at temperatures of $T = 25°C$, 100°C, 200°C, 300°C, and 400°C according to the procedure described in [6]. Work pieces of the reference extrusion process and samples for the friction tests are all numerically modelled elastic-plastic with these flow curves. A *Young's* modulus of $E = 210\,GPa$ and a *Poisson's* ratio of $\nu = 0.3$ is assumed.

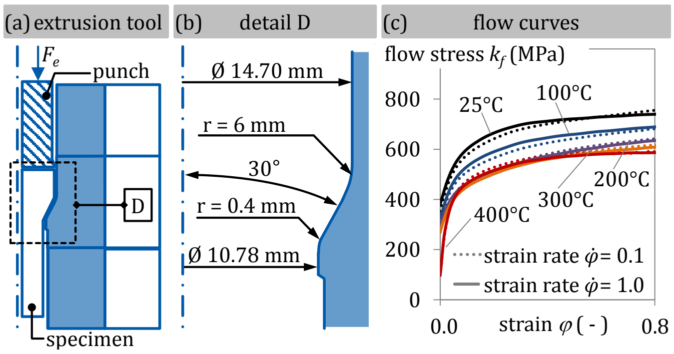

Figure 1. Tool geometry of reference forming process (F_e: extrusion force, a), detail of forming die (b), and experimentally determined flow curves.

All employed tools are made of M2 grade tool steel with a hardness of $H = 61\text{-}63\,HRC$. The tools surfaces are coated with an AlCrN based coating (*Balinit Alcrona Pro*) and polished to a roughness of $Ra < 0.2\,\mu m$. The specimens are sandblasted to a roughness of $Sq = 3.64 \pm 0.20\,\mu m$. Consecutively, the lubrication system, consisting of a zinc-phosphate conversion coating and reactive soap, is applied. As a result, the specimens feature a combined lubrication layer weight of $w = 22\,g/m^2$. The main steps of the lubrication process are displayed in Table 1.

Table 1. Process of application of the lubrication system.

Step	Description
1. cleaning	GardoClean 350, 50g/l at 90°C for 10 min.
2. pickling	hydrochloric acid (15%)
3. activation	Gardolene V 6522, 2g/l at 20°C
4. phosphating	Gardobond Z 3190 at 65°C for 6 min.
5. lubricant application	Gardolube L 6176 (soap), 4 %, at 85°C for 5 min.

In total, six tribological tests are investigated: two indirect tests, in which the friction coefficients are determined by measurement of geometric properties, and four direct tests, in which the friction coefficients are determined by force or torque measurements. All tests feature an open tribological system [6]. The empirically determined friction coefficients are based on at least three test runs. In Figure 2 (top), a schematic overview of the employed tests, along with the relevant quantities to be measured, is illustrated. The dimensions of the used samples are given in the respective publications.

The first representative of the indirect tests is the Ring Compression Test with Boss (RCT-B) [7]. Friction is determined by compression of the rings in between two platens. By geometrical measurement of the reduction of height $R_H = (H_0 - H_1)/H_0$ as well as the increase of the outer diameter $R_D = (D_1 - D_0)/D_0$, friction coefficients are determined by comparing these parameters to numerically generated calibration curves. These are generated with friction coefficients of $\Delta\mu = 0.01$ spacing. Within the present study, the specimens were upset to three different heights up to $R_H = 58.9\%$.

The second indirect test is the Combined Forward Rod Backward Can extrusion Test (CFRBCT) [8]. Here, a cylindrical specimen is extruded simultaneously in forward and backward direction. Analogous to the RCT-B, the friction coefficient is determined by measuring the forward extruded rod length B and can height H_b and comparing these parameters to numerically determined calibration curves. Within the present study, the friction coefficient is determined based on specimens with a stroke length of $S_p = 15\,mm$; $17\,mm$; $19\,mm$; $20\,mm$.

The Backward Can Extrusion Test (BCET) is the first representative of the investigated direct tests [9]. This test method is based on a backward can extrusion operation. By measuring the pullback force F of the punch after the backward extrusion, a friction coefficient can be determined by division of the pullback force F with the numerically determined radial force F_r acting during the pullback sequence. Friction coefficients are determined with cups extruded to a cup depth of $c = 51\,mm$.

The Backward Can Extrusion with simultaneous Rotation Test (BCERT) [10] is based on a backward can extrusion process with superimposed rotation of the die together with the work piece. Measurement of the torque during the extrusion combined with Finite Element Analysis (FEA) of the contact normal stress and relative sliding velocity allows subsequently to determine the local direction of friction and the coefficient of friction. Within the current study, friction is determined at a relative can height of $h_c/d_p = 0.6$.

The Upsetting Sliding Test (UST) [11] is designed to reproduce loads in wire drawing. A convex indentor is upset into the surface of a cylindrical work piece and subsequently moved in axial direction. The friction coefficient is determined by analysing the semi elliptic contact surface submitted to the measured axial force F_a and the radial force F_r. Within this study, a track depth of $t = 0.2\,mm$ is chosen which results in a plastic strain of $\varphi = 0.9$. A mean friction coefficient is determined for the entire sliding distance.

The Sliding Compression Test (SCT) [12] is a two stage process.

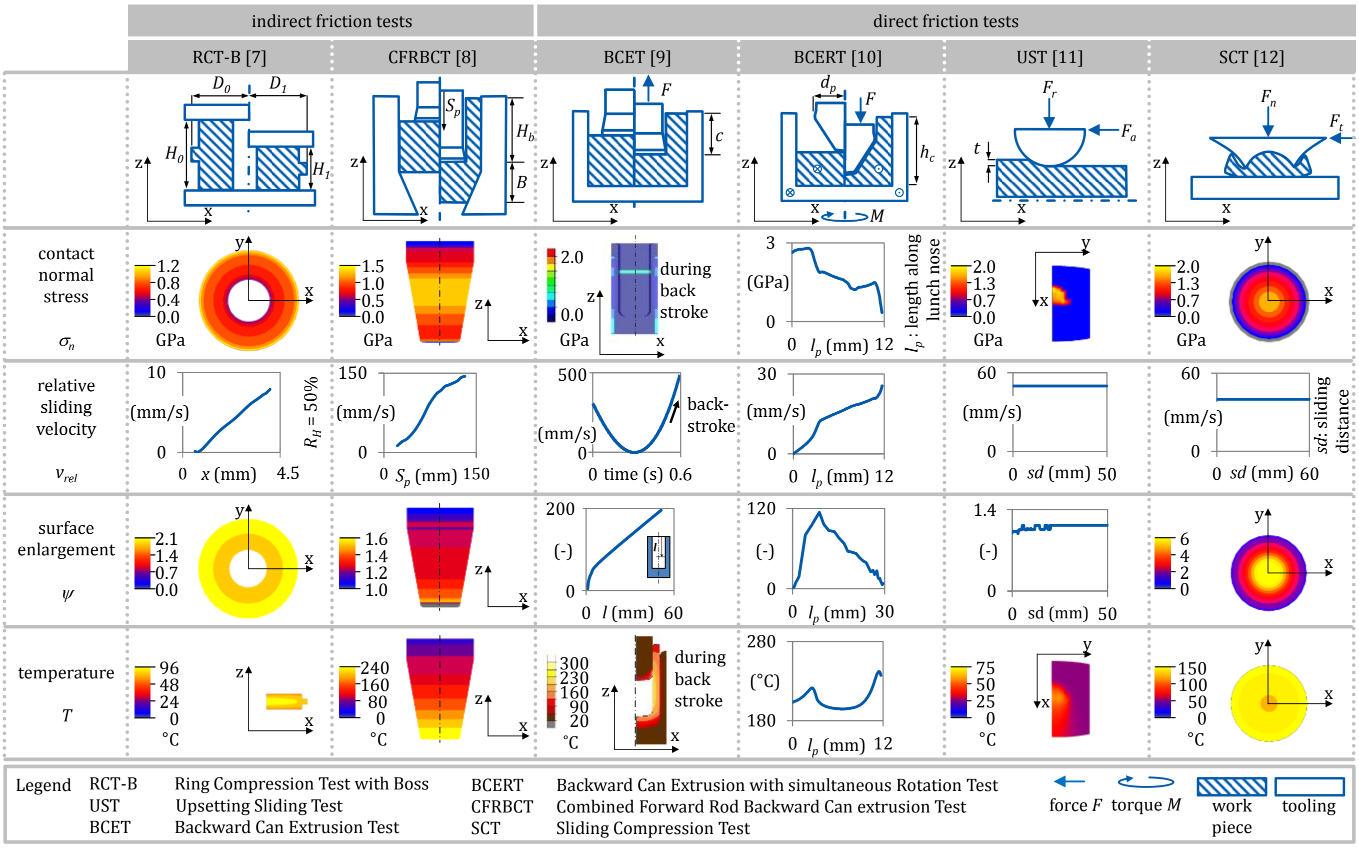

Figure 2. Comparison of tribological loads of the investigated friction tests.

After compression of the specimen, the tool is moved while the compression force F_n is upheld. Friction is determined by measurement of the compression force F_n and tangential force F_t. Within the current study, a compression force of $F_n = 350\,\text{kN}$ is chosen. A mean friction coefficient is determined for the entire sliding distance. To obtain the tribological loads, all described tests are modelled numerically with the help of FEA with a constant friction coefficient $\mu = 0.05$ and an element edge length of $k < 0.2\,\text{mm}$.

Next to giving a schematic overview of the utilized tests, Figure 2 also gives a detailed description of the tribological loads. While mean contact normal stresses range from $0.7\,\text{GPa} < \sigma_n < 1.5\,\text{GPa}$, the relative sliding velocities cover a larger interval, ranging from $4\,\text{mm/s} < v_{rel} < 250\,\text{mm/s}$. Large deviations of the surface enlargement are also observed. The BCET offers a surface enlargement that is more than one dimension greater than any of the other tests' surface enlargement. Mean temperatures only reference the contact zone. Due to the low degree of deformation as well as relative small contact zone in relation to the work piece volume, the contact temperature of the UST is relatively low with $T_C = 55°\text{C}$ (tool temperature $T_T = 100°\text{C}$). Similarly, the contact temperature $T_C = 65°\text{C}$ of the RCT-B is also lower than the temperature of the other tests because of a low upsetting velocity of $v = 5\,\text{mm/s}$ and a large contact area in relation to the work piece volume.

3. Results and discussion

The empirically determined friction coefficients are displayed in Figure 3. Additionally, a summary of the in Figure 2 described tribological loads of the friction tests as well as the reference process are depicted.

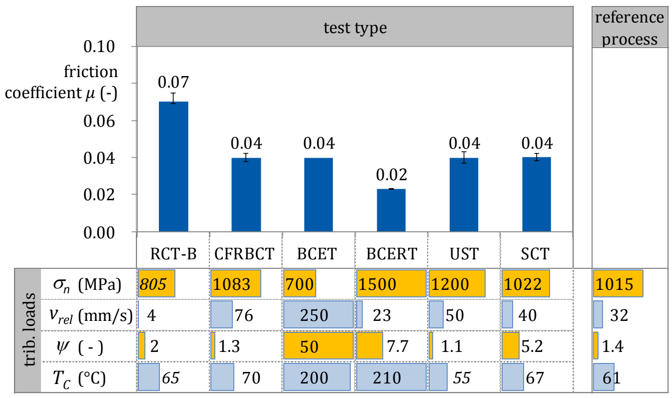

Figure 3. Friction coefficients and tribological loads of the friction tests and the reference process.

Large deviations in between the tests are observed, with friction coefficients ranging from $0.02 < \mu < 0.07$. The CFRBCT, BCET, UST, and SCT lead to a friction coefficient of $\mu = 0.04$. In general, the tribological loads of CFRBCT, UST, and SCT correspond well to each other as well as to the reference process, except for the surface enlargement.

In contrast to the aforementioned tests, the BCET features lower contact normal stresses ($\sigma_{n,BCET} = 700\,\text{MPa}$) and a significantly higher relative sliding velocity ($v_{rel,BCET} = 250\,\text{mm/s}$), yet also results in the same friction coefficient of $\mu_{BCET} = 0.04$. The RCT-B, on the other hand, exhibits lower contact normal stresses as well as a very low relative sliding velocity $v_{rel,RCT\text{-}B} = 4\,\text{mm/s}$, which leads to an increased friction coefficient of $\mu_{RCT\text{-}B} = 0.07$. The lowest friction coefficient of $\mu_{BCERT} = 0.02$ is measured with the BCERT. This test also features the highest mean contact normal stresses with $\sigma_{n,BCERT} = 1{,}500\,\text{MPa}$.

While four of the six investigated tests show the same friction coefficient of $\mu = 0.04$, two tests deviate significantly. Due to test-specific restrictions and boundary conditions, the tribological loads also deviate. It can thus be assumed that the deviation of the tribological loads leads to a deviation of the measured friction coefficients. No galling is observed in any test. The comparison of the results gained with the CFRBCT, the UST, and the SCT leads to the assumption that the surface enlargement is of insignificant importance under the investigated conditions.

Additional test series with the SCT were performed to prove these assumptions. In order to check the insignificance of the surface enlargement, two different die geometries (Die A and B) are used to create a comparable mean contact normal stress of $\sigma_{n,DieA} = 1{,}024\,\text{MPa}$ and $\sigma_{n,DieB} = 1{,}052\,\text{MPa}$ while achieving different surface enlargements of $\psi_{DieA} = 5.2$ and $\psi_{DieB} = 1.5$. The usage of these dies leads to a significantly different plastic deformation of the specimens. Therefore, the tools were heated to $T_T = 100°\text{C}$ in order to guarantee that the heat generated during the upsetting of the specimens does not influence the measurement of friction. The respective results are depicted in Figure 4 (a). Both configurations show a friction coefficient of $\mu = 0.03$ with overlapping error bars. It is thus assumed that surface enlargement plays a subordinate role in the investigated lubrication system.

In order to validate the assumption that the variation of tribological loads in between the friction tests is the source of the deviating friction coefficients, three additional test series are performed with the SCT. The contact normal stress is lowered to $\sigma_n = 743\,\text{MPa}$ while the relative sliding velocity is also lowered to $v_{rel} = 2\,\text{mm/s}$ in order to emulate the tribological loads of the RCT-B. This setup yields a friction coefficient of $\mu = 0.07$, see Figure 4 (b), which corresponds well with the friction coefficient obtained by the RCT-B. The error bars of the SCT are comparatively high for this specific combination of tribological loads. This is due to the rising friction coefficient over the course of the sliding distance, whereas the friction coefficient is mostly constant for the other combinations of tribological loads.

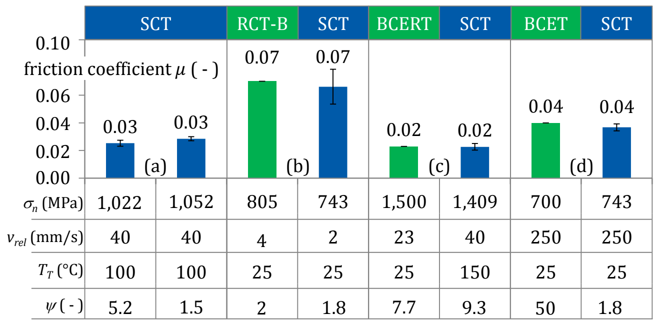

Figure 4. Friction coefficient as a function of the tribological loads, determined with the SCT, in comparison to the RCB-T, BCERT, and BCET.

To mimic the conditions of the BCERT, the contact normal stress is raised to $\sigma_n = 1{,}409\,\text{MPa}$ in the SCT, as well as the temperature increased to $T = 150°\text{C}$. With this setup, a friction coefficient of $\mu = 0.02$ is obtained, see Figure 4 (c), which coincides very well with the BCERT.

In order to better reflect the tribological loads of the BCET, the SCT is modified to obtain a contact normal stress of $\sigma_n = 743\,\text{MPa}$ and a relative sliding velocity of $v_{rel} = 250\,\text{mm/s}$. This yields a friction coefficient of $\mu = 0.04$ which corresponds well to the BCET, see Figure 4 (d).

A functional relationship is assumed between the friction coefficients of all investigated friction tests in the form of Equation 1 and 2:

$$\mu_i \cdot K_i = \mu_j \cdot K_j \tag{1}$$

$$K_{i/j} = ln(\sigma_{n,i/j})^m \cdot ln(v_{rel,i/j})^n \cdot ln(\psi_{i/j})^o \cdot ln(T_{i/j})^p \tag{2}$$

The subscripts i and j denote the frictional tests ($i/j = 1$: RCT-B; $i/j = 2$: CFRBCT; $i/j = 3$: BCET; $i/j = 4$: BCERT; $i/j = 5$: UST; $i/j = 6$: SCT). The relevant tribological loads are taken from Figure 3. Equation 1 is minimized according to Equation 3:

$$\sum_{i=1}^{n_1} \sum_{j=1}^{n_1} \left(\mu_i - \mu_j \cdot (K_j/K_i)\right)^2 \rightarrow min.\ with\ i \neq j \tag{3}$$

with n_1 representing the total number of investigated friction tests ($n_1 = 6$). Equation 3 is found to be minimized with exponents of $m = 7.731$, $n = 0.232$, $o = 0.050$, and $p = 1.286$.

Through transposing of Equation 1 and substituting the subscript i with subscript e (extrusion process), $\mu_{e(j)}$ can be derived, see Equation 4:

$$\mu_{e(j)} = \mu_j \cdot (K_j/K_e) \tag{4}$$

Using the determined exponents m, n, o, and p as well as the tribological loads of the six friction tests, a medium friction coefficient of $\mu_e = 1/n_1 \sum_{j=1}^{n_1} \mu_{e(j)} = 0.041$ is determined for the reference extrusion process.

Figure 5 (a) displays the comparison of the empirically measured extrusion forces $F_{e\text{-}emp}$ (three specimens) with numerically calculated extrusion forces. Numerically, force trends $F_{e\text{-}sim}$ were calculated for constant friction coefficients of $\mu = 0.00$ and 0.041 as well as one trend based on the friction model as described in Equation 4 (subscript $j = 1$ (RCT-B)). Upper and lower bounds for the friction model were set to $0.02 < \mu < 0.07$ according to the empirically determined friction coefficients, see Figure 3.

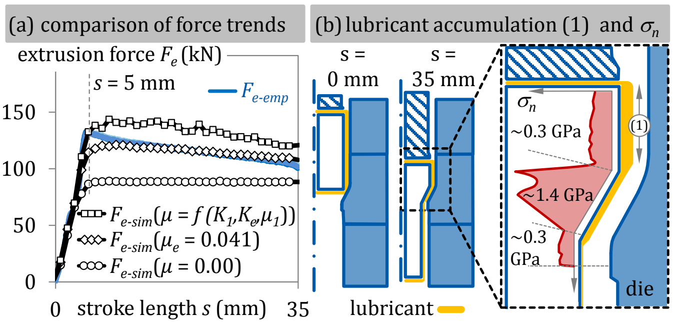

Figure 5. Comparison of extrusion forces F_e (left) and schematic depiction of lubricant accumulation and σ_n (right).

Modelling the extrusion process with the help of the friction model leads to a very good agreement with the empirical data up to a stroke length of $s = 5$ mm. Then, a distinct difference in between the two curves is observed. The authors assume that this deviation of force arises from the accumulation of lubricant within the forming zone. Equivalently, it is shown in [13], that an accumulation of lubricant on the tool surface can lead to a decrease of the friction coefficient. In contrast to the friction tests, the cylindrical area of the extrusion process is a closed tribological system. Due to the high contact normal stresses in the extrusion zone ($\sigma_n \sim 1.4$ GPa), lubricant does not escape during forming and thus accumulates in the contact zone, as is schematically depicted in Figure 5 (b). This accumulation of lubricant leads to a reduction of friction which is so far not considered in friction tests and models.

4. Summary

Six different friction tests for cold forging operations are contrasted within this paper. Although all tests were designed to reproduce the tribological loads of a reference extrusion process, numerical simulations of the friction tests reveal that none of the frictional tests is capable to fully reproduce all tribological loads. This is due to interdependencies of the tribological loads that are inherent in all friction tests. Empirical investigations reveal that the friction coefficients vary within a range of $0.02 < \mu < 0.07$. Additional friction tests with the Sliding Compression Test show that by selectively varying the tribological loads, high agreement of the friction coefficients can be achieved.

Using a semi-empirical friction model within the numerical simulation of the extrusion process reveals that a distinct deviation of the forces exists. This is attributed to the accumulation of lubricant over the course of the forming process within the contact zone. However, since none of the investigated friction tests can account for this phenomenon, the numerically determined force trends deviate from the experiments after the die has been fully filled with material ($s > 5$ mm) and lubricant has been encapsulated in between the die and work piece.

5. Acknowledgements

The authors would like to thank all members of the Subgroup “Lubrication” of the International Cold Forging Group (ICFG) for their continuous support. Special thanks go to Mr. Muralishankar of Super Auto Forge India Pvt. Ltd. for providing the tooling, Dr. Meidert of ThyssenKrupp Presta for providing the material for the specimens, and Dr. Hollmann of Chemetall GmbH for application of the lubrication system. Additional gratitude is expressed to Ms. Üstünyagiz and Mr. Sulaiman of the Technical University of Denmark for assistance in designing and carrying out the BCERT experiments. We also like to thank Dr. Zang of the Technische Universität Darmstadt for his work regarding the planning and execution of the experimental work.

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Request letter for cooperative work

To whom it may concern

We would like to submit the abstract with the title "Friction coefficients in cold forging: a global perspective", authored by Peter Groche (1), Philipp Kramer, Niels Bay (1), Peter Christiansen, Laurent Dubar, Kunio Hayakawa, Chengliang Hu, Kazuhiko Kitamura and Philippe Moreau as a cooperative work to the 2018 CIRP ANNALS – Vol 67/1. The background of the proposed paper is as follows:

The presented work was accomplished within the International Cold Forging Group's Subgroup "Lubrication" and was started in September 2015. While all of the authors have been involved in the characterization of friction and have published extensive research articles regarding the measurement of friction in cold forming processes, it is unclear until today if and to what extent the by different tribological tests obtained friction coefficients are comparable.

Within our to be submitted work, we use an extrusion process with a tribological system consisting of zinc-phosphate and soap as reference for the tribological tests. Based on the tribological loads determined within this reference process and the used tribological components, we used six different, well-established tribological tests to determine the friction coefficients. The results show a significant deviation of the friction coefficients. By thoroughly investigating the sources for the deviation we found the different tribological conditions as the main reason.

The presented work was designed and carried out by six different research laboratories in Japan, China, France, Denmark and Germany and was coordinated within biannual meetings, thus exhibiting a significant cooperative nature. We believe that our findings considerably contribute to the field of metal forming and hope to persuade the Editorial Committee to accept our publication as a cooperative work.

Yours sincerely

Prof. Dr.-Ing. Dipl.-Wirtsch.-Ing. P. Groche