# Development of a new wear test method for hot forming

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Abstract: Utilization of the lifetime extension possibilities of forming dies is one of the key questions in the economic production of forged products. Failure of the shaping surfaces is mostly due to wear processes (in about 70% of the cases) [1]. In order to calculate wear and to check experimentally the calculations the upsetting technology performed between parallel pressing plates has been chosen, which is used at the forging factory of Rába Axle Ltd. as a pre-upsetting (scale removing) step using robot technology. Local wear depth has been calculated by the Archard wear model. In order to apply the wear model one has to know the displacement field at the contact of the part and the pressing plate, the temporary pressure distribution and the wear coefficient characteristic of the die, depending on the working temperature of the die. In order to define the inter-dependent displacement fields the material flow has been mathematically modeled. Developing further the selected mathematical model, based on the largest diameter of the barreling part the approximate value of the friction coefficient has been determined, which is necessary to define the temporary displacement field and the temporary pressure values. We have also attempted to determine the wear coefficient experimentally. When evaluating the experiment special macro- and micro-geometrical tests were used. In order to solve the mathematical model numerically a program was written using the Mathcad software.

*Keywords: material flow, wear, upsetting, die surface, friction coefficient*

## 1. Cost analysis, actuality of the problem

Increase of the production costs of forged parts continued dramatically in the past few years (Fig. 1.). The reason of this change is partly the increase of the technical requirements and partly increasing material costs. A possible, obvious way of cost reduction is the increase of the lifetime of the dies by reducing the degree of wear being the main cause of die failures [2].

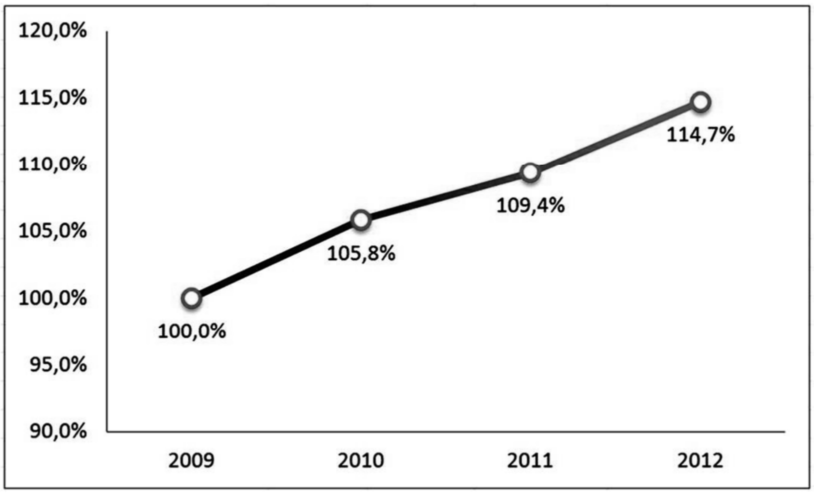


*Figure 1. Changes in the specific production cost of forged parts (cost/kg, percentage change)*

In multi-cavity impression-die forging it is an important requirement that the dies of the consecutive forming steps work in harmony and the die wear should be predictable. The study of die wear was therefore started at the upsetting dies characteristic of the first forming step of the process. In our earlier work a wear coefficient was determined for NK2 die steels by analyzing the abrasion marks of 26000 forged specimens prepared under the conditions used by Rába Axle Ltd. [3] [4].

Our aim in the present work is to investigate the possibility of joint determination of wear and friction coefficients and the more precise determination of the wear coefficient characterizing the die steels used.

## 2. Novelty of the method, used theoretical models

The practical modeling of the wear process of die surfaces with acceptable precision is made possible by the robot technology used in hot upsetting as it places the pieces to be formed always in identical position, according to the program (Fig. 2.).

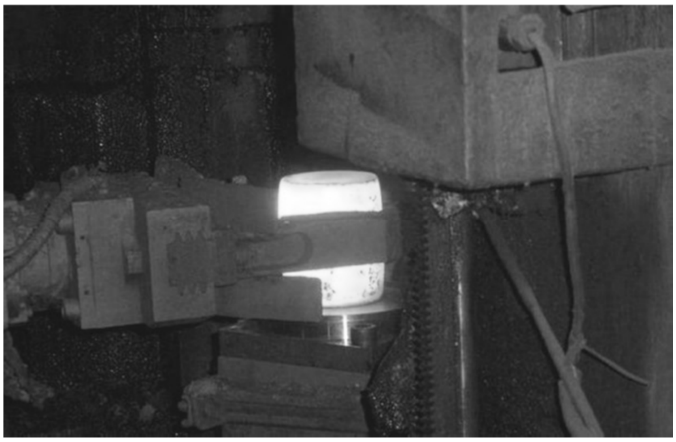

*Figure 2. Manipulation of the upset forged part by a robot*

In the modeling of the forging process the constant parameters are as follows: the surface roughness of the die, operation parameters of the die (scaled surface of the part, lubrication, kinematic and dynamic properties of the production machine etc.).

Based on all above the more precise determination of the wear coefficient is possible if the size of the experimental sample is reduced to the experimentally testable minimum. Under testable minimum that minimum is meant where the abrasion mark can be evaluated, the maximum wear site begins to be formed and the effect of adhesion wear is negligible. Then, at the beginning of the wear process the relative displacement of the contacting surfaces is influenced only by the friction coefficient, i.e. the abrasive wear produces the maximum mark.

Using this assumption the wear coefficient of the mark and the friction coefficient can be brought into direct relation with each other can be involved into an algorithm. This latter means that based on the average value of the maximum diameter of the upset forged part the friction coefficient and through this the wear coefficient of the die steel can be determined.

### 2.1. Mathematical modeling of material flow

Basic laws describing the motion of continua can be used for the mathematical modeling of material flow [5]. Such a basic law is conservation of mass:

$$\frac{\partial \varsigma}{\partial t} + \nabla \cdot (\varsigma \vec{v}) = 0 \tag{1}$$

where:

$\varsigma$ is the density of the material (kg/m$^3$),
t is the time (sec),
$\nabla$ is the Hamilton operator,
$\varsigma\vec{v}$ is the mass current (kg/m$^2$sec).

If considering plastic forming it is usually assumed that the density of the formed part does not change during upsetting, so form equation (1) the constancy of the volume described by equation (2) follows [5]:

$$div(\vec{v}) = 0\ . \tag{2}$$

When upsetting a primary part of cylindrical shape the deformation can be well approximated by axial symmetry, therefore the constancy of the volume may be expressed in the cylindrical coordinate system too, only / $w_z$ / and / $w_r$ / velocity components should be used:

$$div(\vec{w}) = \frac{\partial w_r}{\partial r} + \frac{w_r}{r} + \frac{\partial w_z}{\partial z} = 0 \tag{3}$$

where:

$\vec{w}$ is the velocity vector a given (r, z) point,
$w_z$ is the axial velocity component at a given point,
$w_r$ is the radial velocity component at a given point.

According to reference [6] the velocity field is kinematically allowed if within the body it satisfies everywhere the condition of incompressibility $\dot{\varepsilon}_{ii} = 0$ and the circumferential boundary conditions. The boundary conditions that can be defined for the points of the processed part based on the movement of the pressing plates – in comparison to the friction-free case [7] [8] – should be complemented with the assumption that / $w_z$ / the axial velocity component (4) has an inflection point at z=h/2, i.e. at this point the deformation rate / $\dot{\varepsilon}_z$ / exhibits an extremum. The model with the assumptions listed above results in a barreled part. The axial velocity component of the kinematically allowed velocity field can be described by a third order polynomial:

$$w_z(z) = az^3 + bz^2 + cz + d \ . \tag{4}$$

The velocity field of the points of the barreling part (Fig. 3.) are described by the following functions:

$$w_i(r,z) = \left[w_r(r,z); w_z(z)\right] \tag{5}$$

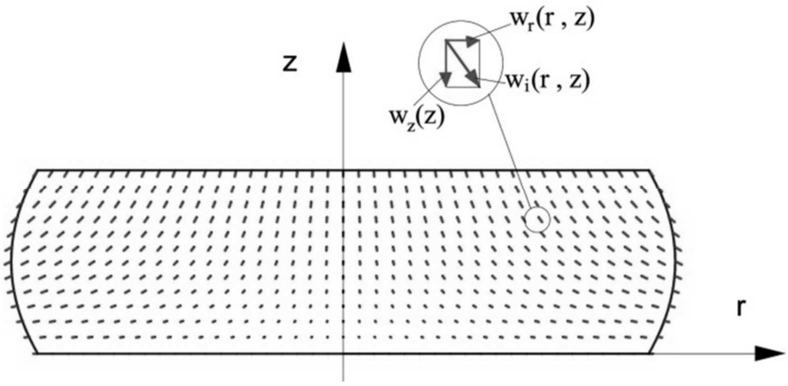


*Figure 3. Image of the velocity field resulting in bulging part*

Modifying parameters /c/ of equation (4) to a dimensionless parameter k=-ch/$v_0$ the velocity functions (taking into account the boundary conditions) can be reproduced as equations (6) and (7) [7] [8].

The axial velocity component is:

$$w_z(z) = \frac{z\left(-2z^2kv_0 + 2z^2v_0 + 3zkv_0h - 3zv_0h - kv_0h^2\right)}{h^3} . \tag{6}$$

The radial component of the velocity field / $w_r$ (r,z) / can be obtained by solving equation (3):

$$w_r(r,z) = -\frac{1}{2}\frac{r\left(-6z^2kv_0 + 6z^2v_0 + 6zkv_0h - 6zv_0h - kv_0h^2\right)}{h^3} . \tag{7}$$

The exact value of / k / can be determined by minimizing the power-demand of the forming process [7] [8]. If using the assumed velocity field this dimensionless parameter influences the degree of barreling which, in turn is related to the friction coefficient. Therefore the value of / k / is related to the Kudo friction coefficient [7]. The relation between the two factors can be most easily given by equation (8) [7] [8]:

$$m = 1 - k \ . \tag{8}$$

In the case of a pre-upsetting task the following parameters are given: the initial radius / $R_0$ /, the initial height / $H_0$ / and the height of the upset part / $h_n$ /. Using these data and equations (6) and (7) one can simulate the profile curves, the / $R_{min}$ / and / $R_{max}$ / values as a function of / m / (Fig. 4.).

At the applied simulation the value of initial height / $H_0$ / was cut down with $v_0dt$=0.1 mm, and the new position of the points was calculated by using the relations (6, 7). Then this new geometry was considered to be the initial one, and the previous steps were repeated by decreasing the heights again. This cycle was repeated until the height at an instant reached the specified value of / $h_n$ /. The stabilities of / k / and / m / are rightfully assumable in case of summing up elemental vertical shifts of upsetting. We have presumed the stability of these values throughout small pre-upsetting.

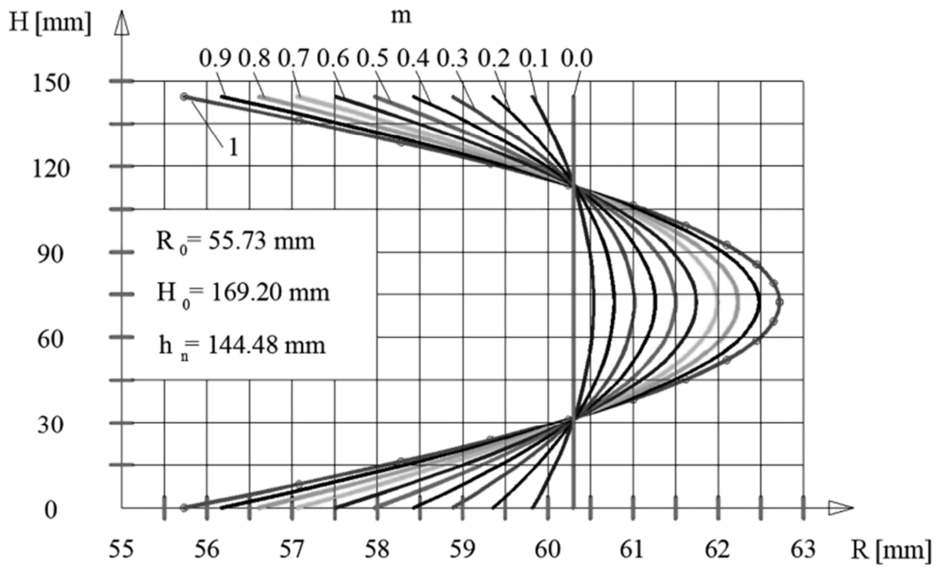


*Figure 4. Profile curves as a function of the Kudo friction coefficient, m*

Specific temperature conditions of the forming process should be taken into account in the evaluation process. During upsetting at the contact point of the formed part and the pressing plate the temperature of the part (with some simplification) is close to 1100 °C, that of the pressing plate to 300 °C.

The corrected radius of the part is calculated using linear thermal expansion:

$$R_{1100} = R_{20}\left(1 + \alpha_{pt}\Delta T\right) \tag{9}$$

where:

$\alpha_{pt}$ is the linear thermal expansion coefficient of the part: 12x10$^{-6}$ (°C$^{-1}$),
$R_{1100}$= 55.726 mm.

Of course other geometrical parameters of the part / $H_0$, $h_n$ / should also be modified. The simulated wear results thus can be related to compression plates of 300 °C temperature, so data for room temperature can be obtained by further calculations. The thermal expansion of the diameter of the die contacting the part is:

$$\Delta R_{300} = R_{20}\alpha_{sz}\Delta T \tag{10}$$

where:

$\alpha_{sz}$ is the linear thermal expansion coefficient of the die: $10.37 \times 10^{-6}$ ($°C^{-1}$),
$\Delta R_{300}$= 0.175 mm.

There is a functional relation between radii / $R_{min}$ / and / $R_{max}$ / on the one hand and the / m / coefficient on the other, and this relation can be fitted by a regression curve (a second order polynomial) for the given upsetting operation (Fig. 5.).

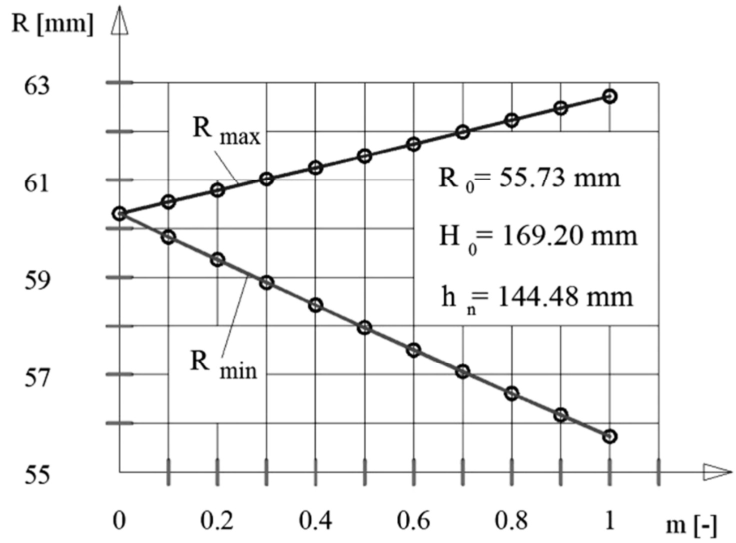


*Figure 5. $R_{min}$ and $R_{max}$ values plotted against the Kudo friction coefficient, m*

The inverse functions m=f($R_{min}$), or m=f($R_{max}$ ) can also be written, from which the friction coefficient can be determined for a given / $R_{max}$ / radius.

In order to use this method it had to be proven that the profile curves obtained by modeling the material flow correspond well to the possible profile curves of real parts[8] [9] [11]. When proving the relation first we demonstrated using literature data and own test results that – within the proposed geometrical constraints [12] - the profile curves can be well fitted by a second order polynomial [7] [8] [10].

The second order polynomial describing the profile curve of real parts (equation (11)) can be defined by the minimum and maximum radii / $R_{min}$ and $R_{max}$ / and the temporary height of the upset part / h /.

$$R(z) = -\frac{4(R_{max} - R_{min})}{h^2} z^2 + \frac{4(R_{max} - R_{min})}{h} z + R_{min} . \tag{11}$$

The determination of the minimum radius / $R_{min}$ / in practice is problematic, as the barreled outer surface (mantle) may join other surfaces not only along an edge, but also with a small radius. The reason of this that during the real shape evolution process sticking of various degree may also appear, resulting in a curvature of various radius (experimentally measured value is about 2.5 mm) between the barreled mantle and the flat face. Equation (11) is valid without taking into account the curvature mentioned above, i.e. the mathematical model does not take into account sticking the friction coefficient obtained should be regarded as an approximate, average value [8]. Based on the assumption of constant volume one can write:

$$R_0^2 \pi H_0 = \int_0^h R^2(z) \pi dz \cdot \tag{12}$$

After inserting equation (11) one can express / $R_{min}$ / from equation (12), i.e. the profile curve can be drawn if the initial radius / $R_0$ /, the initial height / $H_0$ /, the upset height / $h_n$ /, and the maximum radius of the upset part / $R_{max}$ / are known, a high degree of similarity (and inter-relation) between the profile curves obtained from the modeling of material flow and those possible geometrically can be proven.

The friction coefficient value can be determined from the given initial radius / $R_0$ /, initial height / $H_0$ /, upset height / $h_n$ / and from the maximum radius of the upset part / $R_{max}$ / [15].

### 2.2. Mathematical modeling of the wear process

One of the best known relations describing abrasive wear is the Archard wear model which (as a function of the wear coefficient characterizing the die) assumes proportionality between the contact pressure of the contacting surfaces, the displacement and the worn out material volume (13):

$$dV = K(T) \frac{dF_n}{H(T)} dL \tag{13}$$

where:

- dV is the volume of the worn out material ($mm^3$),
- K(T) is the wear coefficient characterizing the die, depending on the working temperature of the die (-),
- $dF_n$ is the normal force acting on the contacting surfaces (MN),
- H(T) is the Brinell surface hardness (HBS) of the die depending on the working temperature of the die (MPa),
- dL is the displacement of the contacting surface elements (mm).

Simplifying assumptions:

- the temperature of the upsetting die does not exceed 450°C during upsetting, i.e. its hardness can be regarded as constant [13],
- the part to be upset is characterized by a homogenous temperature field,
- the temperature and the working strength during upsetting are homogenous and constant,
- only the lower die is investigated because of the higher heat load,
- there is no sticking during the study,
- the friction coefficient values in the study on both die-halves are considered to be constant and identical in course of the upsetting process.

Taking into account the simplifying assumptions, based on the Archard wear model the wear depth for one upsetting cycle can be determined numerically [1]:

$$z = K \sum_{i=1}^{n} \frac{\sigma_n(r,t)_k \, v(r,t)_k}{H} \Delta t \tag{14}$$

where:

| | |
|---|---|
| z | is the wear depth (mm), |
| K | is the specific wear coefficient characterizing the die (-), |
| n | is the number of the discretized increments of the height reduction (pc), |
| $\sigma_{n(k)}$ | is the normal stress at the contacting surface elements in the /$k^{th}$/ increment (MPa), |
| $v_{(k)}$ | is the relative slipping velocity of the contacting surface elements in the /$k^{th}$/ increment ($mms^{-1}$), |
| H | is the Brinell surface hardness (HBS) of the die (MPa), |
| $\Delta t$ | is the contact time interval of the surface elements, the time increment during the displacement (sec). |

When modeling the upsetting cycle by numerical mathematical methods the forming process within the upsetting cycle should be divided into /n/ subsequent intervals (steps). Within one step the upper pressing plate moves $\Delta h = v_0 \Delta t = 0.1$ mm. At every new step a new height value / h / should be used. The actual height is obtained if the / $\Delta h$ / value is subtracted from the previous height value. The functional relation at the contact of the pressing plates and the upset part can be written as follows, using equation (8):

$$w_r(r,0) = w_r(r,h) = \frac{1}{2}\frac{r(1-m)v_0}{h}. \tag{15}$$

When studying wear a displacement-field can be defined instead of the velocity field. Equation (5) changes accordingly to:

$$u_r(r,0) = u_r(r,h) = \frac{1}{2}\frac{r(1-m)\Delta h}{h} \tag{16}$$

where:

$\Delta h$ is the displacement of the upper pressing plate during the / $\Delta t$ / interval $\Delta h=0.1$ (mm).

It can be well seen from equation (16) that the displacement value increases linearly along the radius, and its value is zero at r=0 (Fig. 6.). At r=0 there is no displacement, the die is not worn. This is, of course, only a theoretical statement, valid for point-like surface of infinitesimal size, but this train of thought should be considered when assessing the expected wear distribution.

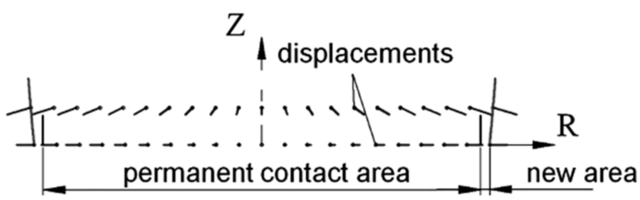


*Figure 6. Division of the contact area*

The friction coefficient value influences the radial displacement, thus the wear. If the upsetting process would be frictionless, m=0, the part would not barrel and the relative displacement would be at maximum. In case of complete sticking m=1 the surface of the part would not change when contacting the die. From the viewpoint of wear the displacements should be summed up step by step. During the summation process the initial area under the ingot should be treated separately from the contact surface parts formed in course of the upsetting process. In the Archard wear model (see equation (14)) the stepwise displacements and the actual pressure should be considered together. In the upsetting process the pressure arising at the contact of the parallel pressing plates and the formed part is not uniform. According to literature hints [14] when upsetting solid cylindrical parts by parallel pressing plates the surface pressure / p / can be calculated form the working strength / $k_f$ /, the Coulomb friction coefficient / $\mu$ /, and geometrical data / h, R, r / as follows:

$$p(r) = k_f e^{\frac{2\mu}{h}(R-r)} \tag{17}$$

The exponential equation (17) can be simplified by Taylor expansion and by neglecting higher order members. There might be a more accurate solution [16] but this approach is easy to use for MathCAD programs and gives correspondent and accurate results for practice. An approximate relation for the surface pressure taking into account the Kudo friction coefficient is as follows:

$$p(r) = k_f \left( 1 + \frac{2m}{\sqrt{3}h}(R-r) \right) \tag{18}$$

The simultaneous consideration of displacements and pressures is easier by the numerical method. Wear depth at r=0 is zero, as there is no displacement, but, at the same time the surface pressure is maximum at this point.

## 3. Industrial experiment

In order to prove the theory industrial experiments were performed. Before starting the experiment a silicone replica was taken of the active surface of the upsetting dies (Fig. 7.). The average surface roughness of the upsetting dies were machined to Ra=0.25 in order to shorten the adhesive wear process and thus to minimize the sample quantity. Useful orientation points were machined onto the surface to support the evaluation (see the red arrows).

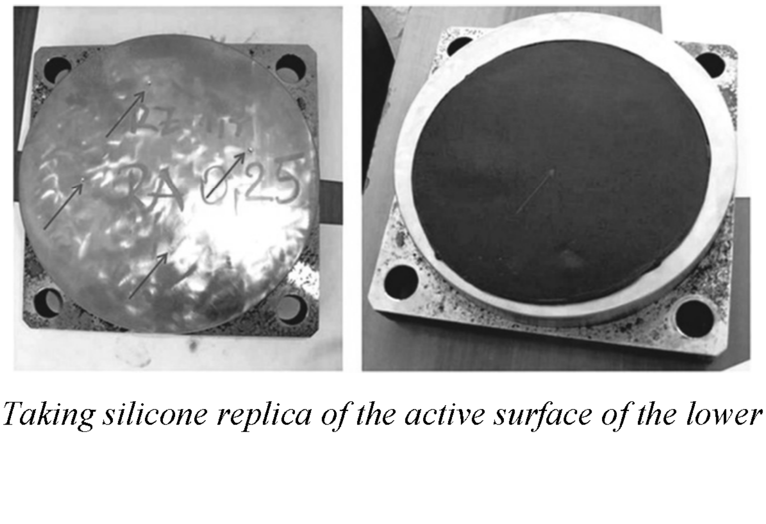

*Figure 7. Taking silicone replica of the active surface of the lower upsetting die*

The industrial experiments were made under constant production conditions in the forging factory of Rába Axle Ltd. A picture of some of the experimental specimens is shown in Fig. 8.

Some characteristic data are as follows:

- cut mass: m'=12.48 ±0.2 kg,
- cut length: $H_0$=167 mm,
- heating temperature: $T_{heat}$=1213-1226 °C → $T_{(average)}$ =1219 °C,
- initial diameter: $D_0$=110 ±0.2 mm,
- upset height: $h_{n(coolsize)}$=142.32-142.91 mm → $h_{n(average)}$ =142.6 mm,
- upset upper diameter: $D_{1(upper - coolsize)}$=114,2-114,5 mm → $D_{1(average)}$=114,3 mm,
- upset lower diameter: $D_{2(lower - coolsize)}$=112,4-113,7 mm → $D_{2(average)}$=113,0 mm,
- upset largest diameter: $D_{k(coolsize)}$=121,6-122,3 mm → $D_{k(average)}$=122,1 mm,
- lubrication: without lubrication,
- forming equipment:10 MN LASCO hydraulic press,
- hardness of die surface: 48 ±2 HRC,
- measured die surface temperature: $T_{lower-max.}$=302 °C; $T_{upper-max.}$=239 °C.

*Figure 8. Forged parts of the upsetting experiment*

The surface temperature of the dies did not reach the critical threshold value of 450 °C where softening starts [13].

## 4. Simulation of the wear process

Using the numerical relations introduced above an own Mathcad program was developed [3] to predict the integral displacements and the expected wear characteristics (the location and degree of the largest wear). Based on the functional relations established earlier for the surface hardness and wear coefficients of NK2 die steels used in Rába Axle Ltd. [3], the wear coefficient was chosen as K=5.49x10$^{-5}$. Using the program the initial value and location of the expected dry wear in one upsetting cycle and the expected size of the worn surface were determined. Input data were: $H_0$=169.20 mm; $R_0$=55.73 mm; $h_n$=144.48 mm; m=0.7; $k_f$ =100 MPa; K=5.49x10$^{-5}$.

When defining the friction coefficient, as an initial value, the average of the maximum upset diameters of the specimens measured in cool state were taken into account: $D_{k(average)}$=122.1 mm. Based on equation (9) the maximum upset radii expected at the forming temperature was also determined: $R_{max(1100)}$=61.86 mm. The Kudo friction coefficient can be directly read from Fig. 5, at the radius of $R_{max}$=62 it is m=0.7. The friction coefficient necessary for the simulation can also be obtained from Fig. 4. In this case the radius of R=62 identifies the profile curve belonging to the friction coefficient of m=0.7. Using equation (14) the program calculates with the input data the expected largest extension of the mark, its depth and location under the simplifying assumption borne out by the experiments that the surface hardness of the die can be regarded as constant during the upsetting process [3] [13]. The results of the run are shown in Fig. 9.

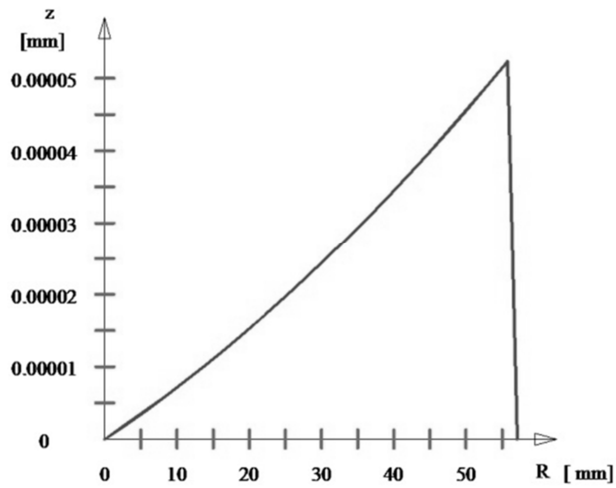


*Figure 9. Wear distribution for a single upsetting cycle*

Before the test the expected diameter of the abrasion mark was estimated based on the size changes due to the different thermal expansion coefficients of the contacting surfaces of the part and the die. The expected diameter of the mark at room temperature is estimated using equations (9) and (10): 114.49-0.35=114.14 mm. From Fig 9 the expected maximum wear depth is $5.2293 \times 10^{-5}$ mm for a single upsetting cycle, its expected distance from the center is 55.55 mm using equations (9) and (10). The expected maximum radius of the abrasion mark is: 57.06025 mm which corresponds to a diameter of 114.1205 mm. From the viewpoint of forging the decisive factor is the surface element exposed to maximum wear, which is sensitive to the material flow therefore it is a potentially dangerous site for surface folds on the formed part. The results obtained were also projected to 26000 workpieces [3]. The calculated maximum wear depth was: 1.3596 mm. In case of 26000 workpieces it is already necessary to take the diffusion heat transmission processes of tool surface into consideration. These processes cause the soft layer on the surface. Therefore the depth of real wear rate may be slightly higher.

The Mathcad program yields only approximate wear data but its great advantage is that it can be easily joint with CAD systems and the 3D geometry necessary for design can be parametrically defined. Afterwards the largest extension of the mark was investigated by various test techniques.

## 5. Comparison of the macro- and micro-geometries of the new and worn pressing plates

After upsetting the experimental parts (Fig. 9.) the lower pressing plate and the upset part No. 66 were investigated carefully in the laboratory of the Széchenyi István University. The goal of this study was mainly to determine the maximum extension of the abrasion mark and to detect the changes within the contact area between the part and the pressing plate.

### 5.1. Macro-geometry of the abrasion mark

Within the contact area the surface roughness of the pressing plate changes, sooner or later worn-out cavities are formed and, due to the heat effect, the pressing plate became discolored.

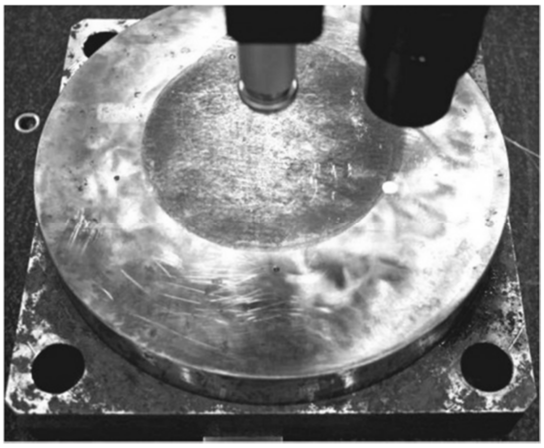

*Figure 10. Monitoring the borderline of the contact area with a MAHR PMC800 coordinate tester*

The theoretical division of the contact area was shown in Fig. 6. The permanent contact area and the newly formed area can be experimentally observed (Fig. 10.). The borderline between the permanent contact area and the newly formed area can be approximated by concentric circles (Fig. 10.). Deviations from the circular shape can be due to the anisotropic properties of the material, with the macro- and micro-geometry of the contacting surfaces of the pressing plate and the formed part before upsetting and with positioning uncertainties of the part.

The surface of the pressing plate was ground after fine milling. Milling resulted in an ordered pattern, grinding in a disordered one. Grinding was uneven sometimes it left in patches the original milling pattern on the surface (Fig. 11.).

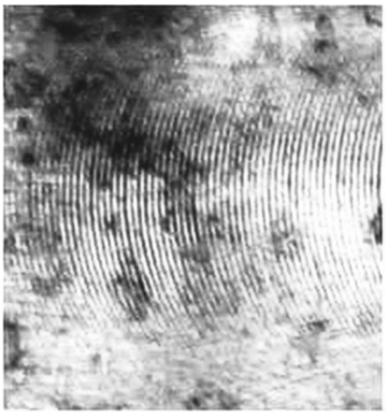

*Figure 11. Local residues of the milling pattern on the pressing plate*

The initial parts for upsetting were sawn from a rod. After upsetting the sawing pattern can be unambiguously observed on the pressing plate. The sawing pattern is mostly caused by discoloration due to thermal overload, it can be observed visually, but cannot be detected at a macro-geometrical level. This pattern partly survived on the face of the upset part (Fig. 12.).

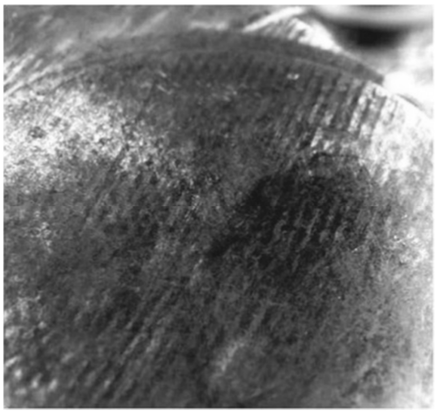

*Figure 12. Sawing pattern from the end-plate of the formed part on the pressing plate*

When studying abrasive cavitation the comparison of the micro- and macro-geometries of the pressing plate before and after use was performed on silicone replicas using a Taylor – Hobson Talysurf CLI 2000 roughness tester. Using the 4 orientation (reference) points it was possible to position the scanning of the worn surface within 0.1 mm to the original one. Scanning was done in two, mutually perpendicular directions.

It has been established that the silicone replicas could not be used for macro-geometrical comparison, as the contacting surfaces of the replicas were larger and less accurate than the stage of the roughness tester, so the flaw and error-free positioning of the silicone replicas was not possible.

Using a MAHR PMC800 coordinate tester the limiting points of the abrasion tracks were determined and the approximate diameter of the circle was determined (Fig. 13.).

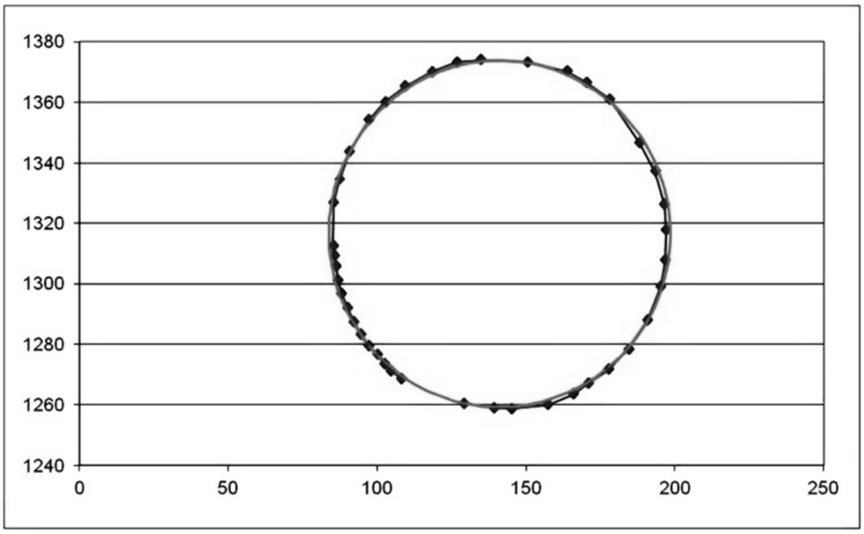


*Figure 13. Coordinates of the center of the $D_2$ circle approximating the data: y=1316.508, x=141.164*

The number and position of the points selected for fitting the approximating circle influence the end result, so the arithmetical mean of the diameters of two approximating circles with different numbers of points ( $D_1$ and $D_2$ ) were taken into account. The arithmetical mean was: $D_A=(D_1+D_2)/2=(113.489+114.670)/2=114.0795$ mm.

The diameter of the approximating circle can also be determined by optical analysis using the photograph of the mark. The advantage of this approach is that using a proper CAD system – in our case AutoCAD – the 3D position of the points is reduced to 2D and the points used for the evaluation can be positioned at freely selected magnification. Effective processing of the evaluation points can be done by proper program development, by a joint use of AutoCAD and Mathcad software. Using AutoCAD the diameter of the approximating circle is also: D=114.08 mm (Fig. 14.).

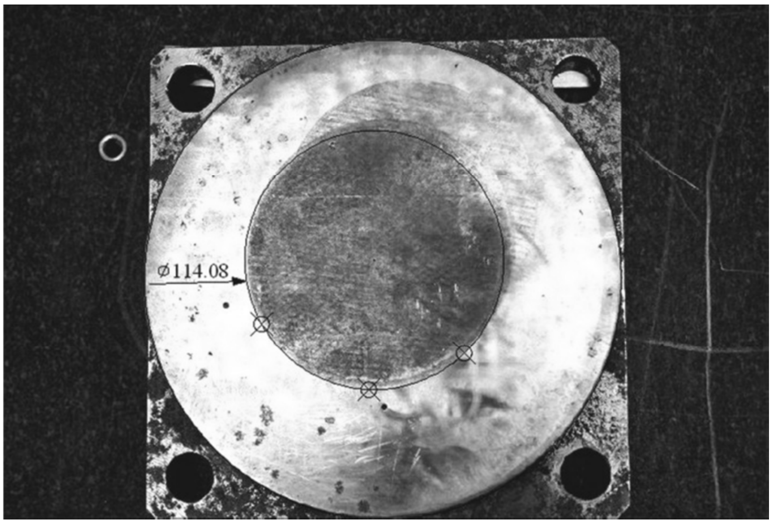


*Figure 14. Determination of the approximating circle by the AutoCAD program*

The remarkable resemblance of the experimentally determined parameters, in spite of the weak points of the different methods used can be regarded as an important finding. The test results can be well compared with the simulation results of our Mathcad program, the program can be corrected. The negligible amount of correction (0.14 mm, the rounding of $R_{max}$=61.86 to $R_{max}$=62) corroborates the applicability of Fig. 5 describing the relation between the friction coefficient and the abrasion mark.

### 5.2. Micro-geometry of the abrasion mark

In order to detect changes in the micro-geometry 2x9 sections of 5 mm length were scanned for surface roughness determination, as shown in Fig 15. Ra and Rz roughness values were evaluated after removing shape error and the waviness was removed by a 0.8 mm Gauss filter.

Sections 1 and 9 were on the reference are outside the abrasion mark, where the part did not contact the pressing plate.

Sections 2-8 were situated within the abrasive mark in equidistant positions, section 5 was at the center.

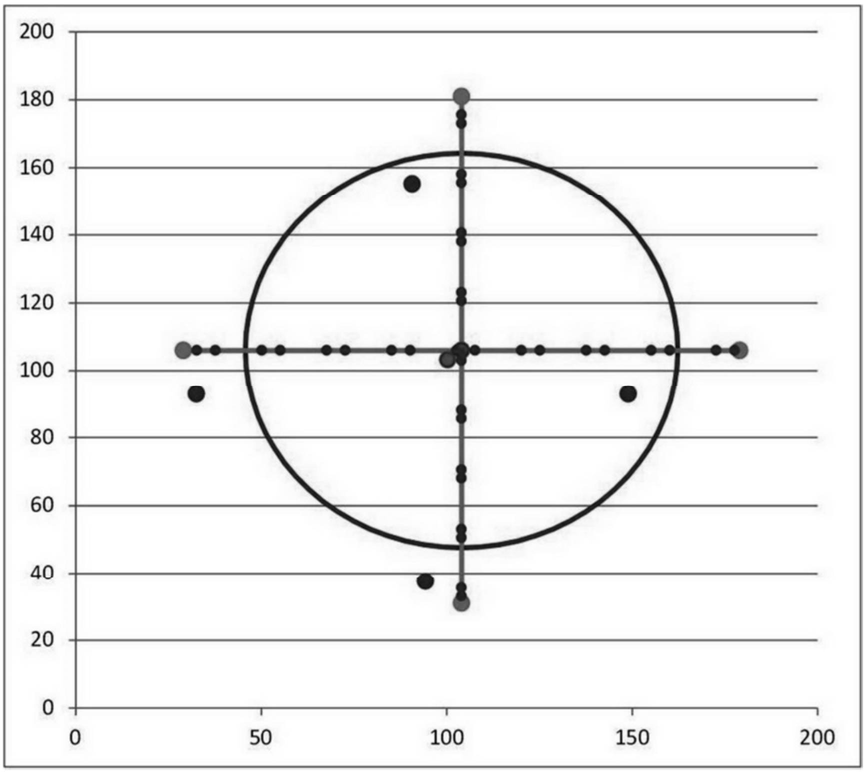


*Figure 15. Sections evaluated for surface roughness*

Table 1 shows the results of surface roughness evaluation for the various sections.

*Table 1*

| row | original (x) | | worn (x) | | original (y) | | worn (y) | |
|---|---|---|---|---|---|---|---|---|
| | Ra | Rz | Ra | Rz | Ra | Rz | Ra | Rz |
| 1 | 0.292 | 2.28 | 0.418 | 3.71 | 0.200 | 1.90 | 0.364 | 4.01 |
| 2 | 0.311 | 2.87 | 0.590 | 6.53 | 0.210 | 2.03 | 0.593 | 5.96 |
| 3 | 0.265 | 2.51 | 0.752 | 1.01 | 0.215 | 2.69 | 0.508 | 4.37 |
| 4 | 0.313 | 3.38 | 0.468 | 3.66 | 0.240 | 2.20 | 0.415 | 3.94 |
| 5 | 0.360 | 3.00 | 0.526 | 4.96 | 0.320 | 3.05 | 0.501 | 4.60 |
| 6 | 0.288 | 2.59 | 0.412 | 3.18 | 0.462 | 3.74 | 0.653 | 4.58 |
| 7 | 0.328 | 5.02 | 0.579 | 6.16 | 0.344 | 3.20 | 0.502 | 4.43 |
| 8 | 0.858 | 5.49 | 1.290 | 7.45 | 0.513 | 5.21 | 0.557 | 4.53 |
| 9 | 0.655 | 4.87 | 0.852 | 6.13 | 0.482 | 5.92 | 0.597 | 7.05 |

It can be concluded that the whole surface of the pressing plate became rougher and the roughness value at the edge of the abrasion marks– where the expected wear is maximal – is much larger than at other places (Fig. 16.).

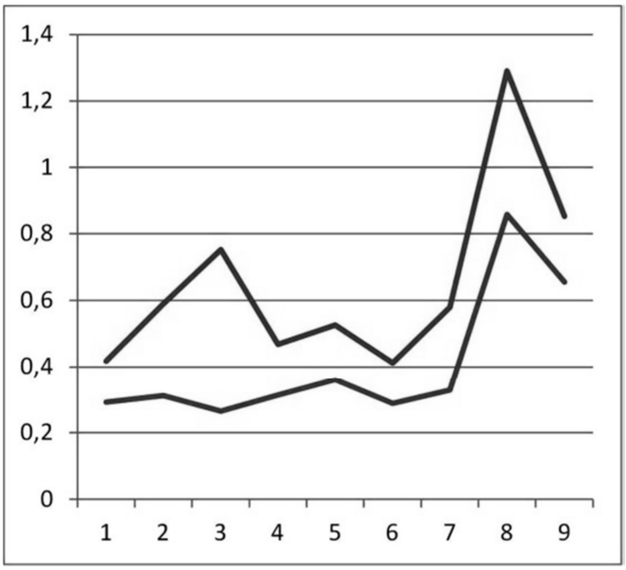


*Figure 16. Average roughness (Ra) at various sections in the x direction (lower curve - initial, upper curve – worn surface)*

Those sites can be unambiguously identified where polishing did not remove milling patterns (Figures 17. and 18.).

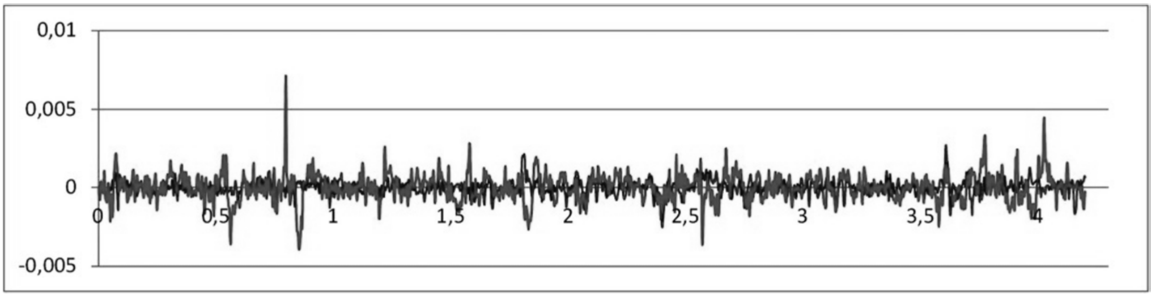


*Figure 17. Surface roughness profile at an area without residual milling pattern (lower amplitude) and in worn state (higher amplitude) (axes are given in mm units)*

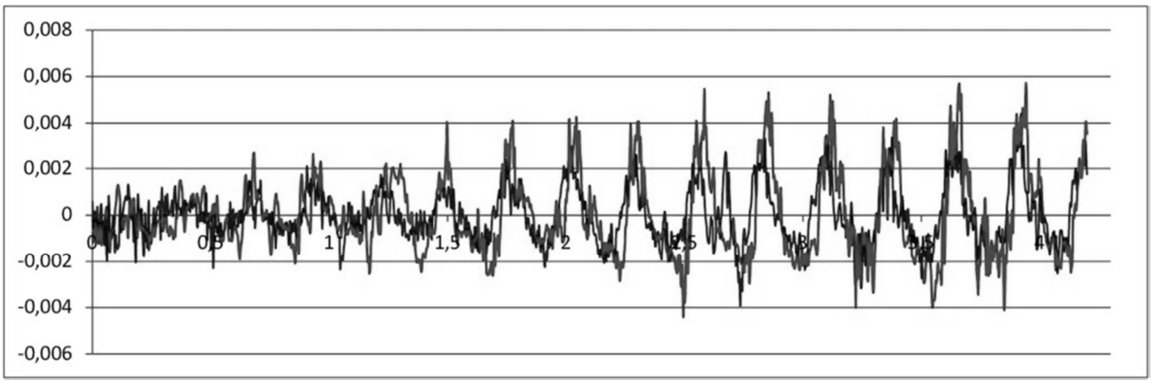


*Figure 18. Surface roughness profile at an area with residual milling pattern (lower amplitude) and in worn state (higher amplitude) (axes are given in mm units)*

During upsetting the pressing plate is loaded by an uneven compressional load. The stress distribution corresponds to the pressure distribution given by equations (17) and (18). Under the effect of the arising compressional stresses the flat surface of the pressing plate is deformed elastically. When upsetting the part – with some simplification – it is deformed only in a plastic manner and the face of the formed part inherits the elastic deformation of the pressing plate.

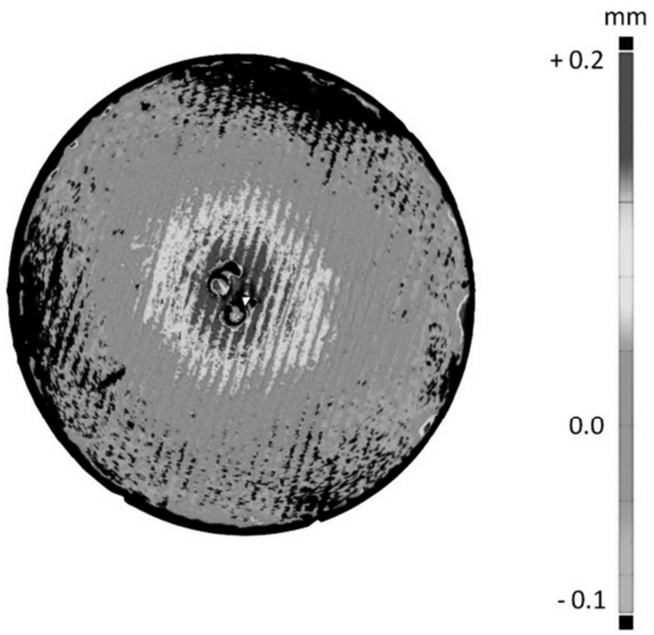


*Figure 19. A protrusion observed on the face of the part to be formed*

Deviations form planarity were studied separately using the MAHR PMC800 coordinate tester. Scanning the studied surface systematically 216000 data were collected. The test data were analyzed as a point cloud using the GOM evaluation software.

It was observed that the central part of the face plate protrudes and the sawing pattern can be partly observed (Fig. 19.). In our opinion the residual sawing pattern may be due to non-removed scale on the face of the formed part. The protruding part can be well approximated by a spherical surface with a radius of R=4606 mm. The protrusion is so small (0.35 mm) and structured (Fig. 19.), that in order to detect it one must use a coordinate tester with a precision of 0.001 mm.

## 6. Conclusions and Future Improvements

In our earlier work we suggested a method to determine approximately the friction coefficient. In our present work we proved that the proposed method can be used under real production conditions - practically without any additional cost – for pre-upsetting (scale removal). Pre-upsetting on robot assisted lines made possible the joint investigation of the friction coefficient and wear.

We have proved by a method, which is new in technology planning that the wear coefficient can be related to the friction coefficient and the method can be well algorithmized. A more precise determination of the wear coefficient requires further studies. We would like to investigate the possibility of taking into account the frictional work.

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