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The Coining Process: Analytical Simulations Evaluated

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Abstract:

For a model, containing the essentials of the coining process, two theoretical simulations are performed. The finite element method is applied to obtain detailed information with respect to stresses and strains. Disadvantage is the rather time consuming procedure. The upperbound theorem offers the possibility to determine more global results concerning the process and is suitable for parameter research. Both methods are extensively described and evaluated. A quantitative comparison with experiments is used for verification. An excellent agreement was found.

Keywords:

Forming, coining, finite element method, upperbound theorem.

1. Introduction

Throughout many ages coining has been considered as an important manufacturing technique. From an engineering point of view this metal forming process still deserves attention. The filling of the engravings can be accompanied by very high toolloads. For designers of coins and tools knowledge of the requested pressures and local strain distributions would be very helpful. In reality the material flow during coining is rather complex. Compression-, extrusion- and dead metal-zones can be distinguished in the workpiece, as illustrated for instance by F. Delamare and P. Montmitonnet [4] (see Fig. 1.1.).

Fig. 1.1. The material flow in a coin after F. Delamare and P. Montmitonnet [4]

An analytical simulation of the process with the finite element method or an upperbound approach necessitates a simplifying modelling of the geometry of the coin. In an earlier work [8] the design was reduced to a number of cone-shaped engravings in the punch (see Fig. 1.2.).

Fig. 1.2. Modelling of the coining process after [8]

It was shown that the dimensions of the "feeding area" around an engraving had a vital influence on the required pressure (see Fig. 1.3.).

Fig. 1.3. The feeding area around an engraving

According [8] the result, based on the upperbound theorem, for the mean coining pressure, in case of negligible friction, is:

$$p = \sigma_f \left\{ A + 2 \ln \left(\frac{D}{d_0} \right) \right\} \quad (1.1)$$

where σ_f equals the yield stress of the blank material (perfectly plastic) and with A a function of D/d_0 as shown in Fig. 1.4.

Fig. 1.4. The parameter A as a function of D/d_0

In this paper a more detailed research on the material flow etc. in the feeding area and the conical engraving is presented. Results from a finite element simulation, an upperbound approach and experiments are compared. A fairly well agreement is found.

2. The modelling of the process

The investigated process is illustrated in Fig. 2.1.

Fig. 2.1. The investigated forming process

A blank, with diameter D and original thickness s_0 , is pressed in a die. The punch is provided with a conical engraving, top angle α and bottom diameter d_0 . The material is forced to flow into the engraving, and the degree of filling is described by the parameter d_0/d . For the material behaviour, exponential strainhardening is assumed:

$$\sigma_f = C (\bar{\epsilon} + \epsilon_0)^n \quad (2.1)$$

With the prestrain ϵ_0 the initial value of the yield stress σ_f can be adjusted. According to the findings of [4] and [8] the friction in the contact areas between tool and workpiece is neglected.

3. Analysis with the finite element method

For the modelling, described in the preceding paragraph a numerical simulation will be carried out. The finite element method is suitable to obtain locally detailed information with respect to the state of deformation, the stress distribution and the external load. The general purpose computer program MARC [6], version K.2, based on the finite element method, offers the possibility to deal with extreme geometrical non-linearity (large displacements, rotations and strains) combined with physical non-linearity (elastoplastic material behaviour). It was decided to apply that program to perform the desired analysis.

The numerical values for the relevant quantities, composing the input data for the program, are:

$$\begin{aligned} C &= 34 \text{ N/mm}^2 & D &= 50 \text{ mm} & \alpha &= 60^\circ \\ n &= 0.185 & d_0 &= 3 \text{ mm} \\ \epsilon_0 &= 0.012 & s_0 &= 5 \text{ mm} \end{aligned}$$

At first the blank (circular disk) is subdivided into elements. Chosen is for a mesh existing of isoparametric quadrilateral ringelements with four nodal points [1]. Per element the axisymmetric fields for the radial and the axial displacement are bilinear in the isoparametric coordinates. Fig. 3.1. shows the element mesh for the undeformed geometry. It can be observed that a relatively fine grid is constructed at the location where large gradients of the interesting mechanical quantities can be expected.

Fig. 3.1. Mesh in undeformed geometry

For the nodes on AB, BC, CD and DE (see Fig. 3.1.) appropriate boundary conditions have to be specified. Along AB the axial displacement is suppressed whereas in radial direction the displacement is free (no friction is supposed). On BC the radial displacement is suppressed and the axial displacement is free. For the nodes on CD the axial displacement has a prescribed value according to the movement of the punch, the radial displacement is free. The boundary conditions in the vicinity of D require adaptation with increasing displacement of the punch; this will be explained later. Between the boundary DE of the blank and the conical hole in the punch so-called gap elements are situated in order to prevent the material of the blank from penetrating the punch. If contact occurs the no friction condition is assumed to be valid. Modelling of contact problems with gap elements is a usual way to handle the describing inequality conditions, however the procedure causes user inconveniences and sometimes numerical difficulties. New developments [7], implemented in the K.3 version of the MARC program offer a fully automatic contact (and friction) feature. These capabilities are especially designed to support the simulation of manufacturing processes such as forging, rolling, forming and extrusion. During the research presented here, this option was not yet available. The ultimate displacement of the punch is reached in a number of small steps. Such an incremental approach is necessary for a successful convergence of the iterative scheme for the solution of the non-linear equations. Besides, intermediate results are obtained and a possibility is created for interactive influence on the rather time consuming calculation. During the simulation process, with increasing displacement of the punch, rezoning of the element mesh [5] is needed for two distinct reasons. Firstly it is important to avoid that elements are unacceptably distorted because of the deformation, and secondly, as mentioned previously, special attention is required to model the contact between punch and blank (near D) in a realistic way. Both items are considered separately. Without intervention some originally well shaped elements will deform excessively with proceeding deformation of the blank. Within such elements the relation between material coordinates and spatial coordinates is not unique anymore, which is physically improper, while also numerical difficulties have to be expected (non positive definite stiffness matrix). Rezoning involves the creation of a new element mesh and the relevant quantities (stresses, history parameters) have to be determined in the new integration points, based upon the information belonging to the old mesh, using interpolation and extrapolation techniques. A smoothing of the distribution of these quantities is inevitable. In order to satisfy again the discretized equilibrium equations an extra calculation step can be performed. During the displacement of the punch in negative z-direction nodal points on CD (see Fig. 3.1.) in the vicinity of D, originally in contact with the flat part of the punch, move in negative r-direction, thus entering the conical cavity in the punch. The boundary condition, prescribing the displacement in z-direction, has to be changed for a proper simulation of reality. Fig. 3.2. shows clearly the undesired configuration if adaptation is omitted.

Fig. 3.2. Improper boundary conditions

Without correction the cavity in the punch is fictitiously shrinking. There are several methods to prevent the above mentioned problem, occurring if not material associated boundary conditions have to be taken into account. Chosen was a technique again using the rezoning option. Nodal points originally located on CD and entering the cavity are moved through the material of the blank and replaced back to the entrance of the cavity. Not every increment this procedure was carried out, a small deviation was allowed to appear, thus not increasing computer time too much.

During the complete simulation the rezoning process was executed a number of times. The results, belonging to the maximum punch displacement (0.0975 mm from initial contact) were reached in 73 increments. For every increment convergence of the iterative solution of the discretized equilibrium equations was obtained within 4 cycles, on average. The deformed geometry with the results for the effective stress (Von Mises) and equivalent plastic strain are presented for an intermediate state (increment 28, punch displacement 0.06 mm) as well as of course for the final state (increment 73). In Fig. 3.3. only the interesting part of the workpiece is shown. Above a certain radius an almost homogeneous situation occurs.

By comparing the results presented in Fig. 3.3. can be observed that material passing the entrance of the conical cavity is locally unloaded (decrease of the effective stress). Globally however the level of effective stress increases as can be expected. By definition the equivalent plastic strain increases monotonically. The analysis is performed using an isotropic hardening model (Nadai); from the figures it can be concluded that a choice for kinematic hardening will hardly influence the results, because no reversal of the loading occurs [3].

1 : 5.0 N/mm ²
2 : 10.0 N/mm ²
3 : 15.0 N/mm ²
4 : 20.0 N/mm ²
5 : 25.0 N/mm ²
6 : 30.0 N/mm ²
7 : 35.0 N/mm ²

1 : 0.1	7 : 0.7
2 : 0.2	8 : 0.8
3 : 0.3	9 : 0.9
4 : 0.4	10 : 1.0
5 : 0.5	11 : 1.1
6 : 0.6	12 : 1.2

Fig. 3.3. Intermediate and final state

For the final state (increment 73) the external load on the blank is presented in Fig. 3.4.

Fig. 3.4. External load in the final state

For values of the radius, a few times higher than the dimension of the cavity, the external load can be considered homogeneous, easy to explain because no friction was taken into account. Following the computational process the total load of the punch on the blank can be regarded as a function of the degree of penetration of the blank material into the cavity. The total load F is divided by $\pi/4 D^2 C$ (see paragraph 2) in order to obtain a dimensionless force quantity $F^* = p/C$. The degree of penetration is defined as $\ln(d_0/d)$. Fig. 3.5. shows the relation obtained by the finite element analysis (FEM) compared with the results of an alternative computation based on the upperbound theorem (UBT), see paragraph 4.

Fig. 3.5. Force versus penetration

For a discussion of the results the reader is referred to paragraph 5. It should be noted that the inequalities in the finite element result are caused by the previously mentioned rezoning procedure. With only a little extra effort the finite element method is capable of dealing with the unloading process. The first stage of unloading consists of a removal of the punch. The resulting stress and strain distribution can be found in Fig. 3.6.

Fig. 3.6. State after punch removal

Comparing Fig. 3.6. with Fig. 3.3. shows that a complete change of the stress distribution is remarkable whereas the equivalent plastic strain remains unchanged. This means that removal of the punch is not involved with extra plastic behaviour of the blank material. Only elastic unloading can be observed. The average external pressure on the boundary BC of the blank reduces from 215 N/mm², see Fig. 3.4., to 25 N/mm². Apparently contact between the blank and the die is not lost by removing the punch. An estimation could be made with respect to the force necessary to isolate the blank from the die. The second stage of unloading, a complete removal of the boundary conditions in order to obtain the isolated blank, was finally simulated. The computation yields the residual stresses in the product, see Fig. 3.7. It is clear that the effective stress only changes a little during this second unloading stage, thus remaining at considerable levels. The distribution of the equivalent plastic strain is not effected at all during this last simulation step. Therefore this distribution is not presented.

Fig. 3.7. Residual effective stress

4. Solution based on the upperbound theorem

In conformity with for example [2] the workpiece is divided in three regions, separated by Γ -surfaces (Fig. 4.1.).

Fig. 4.1. Regions of the workpiece

The deformation power dissipated in the regions equals:

$$P_D = \int_V \sigma_f \dot{\epsilon} dV \quad (4.1)$$

For the power related with the shear on the Γ -surfaces holds:

$$P_\Gamma = \int_\Gamma \frac{1}{\sqrt{2}} \alpha_f |\Delta u_t| d\Gamma \quad (4.2)$$

where with Δu_t the discontinuity in the tangential velocity is denoted. Because of concise formulation dimensionless power quantities are used:

$$P^* = \frac{P}{\pi/4 D^2 C |\dot{\epsilon}|} \quad (4.3)$$

Using the power balance the dimensionless mean external pressure p/C will be equal to the total dimensionless dissipated power:

$$\frac{p}{C} = \sum P^* \quad (4.4)$$

Strainhardening is taken into account in a simplified way. A uniform yield stress per region is assumed, which depends on the mean value $\bar{\epsilon}_m$ of the effective strain in every region:

$$\sigma_f = C (\bar{\epsilon}_m + \epsilon_0)^n \quad (4.5)$$

For subdomain 1 it is supposed:

$$v_1 = -\frac{1}{2} \frac{\dot{\epsilon}}{S} \left(r - \frac{1}{4} \frac{D^2}{r} \right); w_1 = \frac{\dot{\epsilon}}{S} z \quad (4.6)$$

$$\bar{\epsilon}_{m1} \approx 2 \ln \left(\frac{S_0}{S} \right) \quad (4.7)$$

and so:

$$P_{D1}^* = \frac{1}{\sqrt{2}} \left\{ 2 \ln \left(\frac{S_0}{S} \right) + \epsilon_0 \right\}^n \quad (4.8)$$

$$\left[2 - \sqrt{1+3 \left(\frac{D_0}{D} \right)^4} \right] - \ln \left\{ \sqrt{1+3 \left(\frac{D_0}{D} \right)^4} - \left(\frac{D_0}{D} \right)^2 \right\}$$

For subdomain 2:

$$v_2 = \frac{1}{2} \frac{\dot{\epsilon}}{S} \left\{ \left(\frac{D}{D_0} \right)^2 - 1 \right\} r; w_2 = -\frac{\dot{\epsilon}}{S} \left\{ \left(\frac{D}{D_0} \right)^2 - 1 \right\} z \quad (4.9)$$

$$\bar{\epsilon}_{m2} = \left\{ \left(\frac{D}{D_0} \right)^2 - 1 \right\} \ln \left(\frac{S_0}{S} \right) \quad (4.10)$$

and:

$$P_{D2}^* = \left[\left\{ \left(\frac{D}{D_0} \right)^2 - 1 \right\} \ln \left(\frac{S_0}{S} \right) + \epsilon_0 \right]^n \left\{ 1 - \left(\frac{D_0}{D} \right)^2 \right\} \quad (4.11)$$

For the filling of the conical engraving, subdomain 3, the dissipated power is calculated with:

$$P_{D3} = P_e \frac{\pi}{4} d_o^2 w_{I2} \quad (4.12)$$

The extrusion pressure p_e is determined with the help of the slab method [8]. The same result can be achieved by applying a spherical velocity field in this region:

$$P_e = \sigma_{f3} 2 \ln\left(\frac{d_o}{d}\right) \quad (4.13)$$

For the normal velocity on the I2-surface it holds:

$$w_{I2} = |w_2(z=s) - \dot{s}| = |\dot{s}| \left(\frac{D}{d_o}\right)^2 \quad (4.14)$$

Thus with:

$$\bar{\epsilon}_{m3} \approx 2 \ln\left(\frac{d_o}{d}\right) \quad (4.15)$$

it can be derived:

$$P_{D3}^* = \{2 \ln\left(\frac{d_o}{d}\right) + \epsilon_o\}^n 2 \ln\left(\frac{d_o}{d}\right) \quad (4.16)$$

The power due to the shear on the I1-surface is calculated with:

$$P_{I1} = \frac{1}{\sqrt{3}} \sigma_{f1} \pi d_o \int_0^s |w_1 - w_2| dz \quad (4.17)$$

So it holds:

$$P_{I1}^* = \frac{2}{\sqrt{3}} \{3 \ln\left(\frac{s_o}{s}\right) + \epsilon_o\}^n \frac{s}{d_o} \quad (4.18)$$

The shear on the I2-surface can be neglected. Requiring incompressibility yields an expression for s as a function of d :

$$\frac{s}{s_o} = 1 - \frac{1}{6 \tan(\alpha/2)} \frac{d_o}{s_o} \left(\frac{d_o}{D}\right)^2 \left(1 - \left(\frac{d}{d_o}\right)^3\right) \quad (4.19)$$

With the expressions (4.4), (4.8), (4.11), (4.16), (4.18) and (4.19) the coining pressure can be written as a function of the material parameters (n , ϵ_o), the geometry of the blank (s_o , D) and the geometry of the cone (d_o , d , α). Results of the calculations are given in Fig. 4.2., 4.3., 4.4. and 4.5.

In Fig. 4.2. the several contributions to the total dissipated power of the deformation in the three regions and of the shear on the I1-surface are shown.

The ratio of the different dissipated powers depends on the relative size of the feeding area (D/d_o) as illustrated by Fig. 4.3.

Fig. 4.2. The dimensionless coining pressure as a function of the degree of filling $\ln(d_o/d)$

Fig. 4.3. The coining pressure as a function of D/d_o

Fig. 4.4. The coining pressure as a function of the degree of filling with the strainhardening exponent n as parameter

In Fig. 4.4. and 4.5. the influence of the material properties n and ϵ_o is demonstrated. Especially the dependence on the prestrain ϵ_o is noteworthy. For a large feeding area, $D/d_o \gg 1$, the effective strains in the regions 1 and 2 are extreme small. In that case the prestrain dominates the value of the yield stress σ_f and therefore has a vital influence on the dissipated energy in this regions.

Fig. 4.5. The coining pressure as a function of ϵ_o

Fig. 4.6. Comparison of upperbound results and experiments on lead ($C=43 \text{ N/mm}^2$, $n = 0.18$ and $\epsilon_o = 0.001$)

Fig. 4.6. presents the comparison of the theory (upperbound) and experiments on lead. An excellent agreement is achieved.

5. Discussion and conclusions

It has been shown that for global quantities as the toolload, accurate predictions can be achieved with the application of the finite element method as well as with the theory based on the upperbound theorem. With respect to local quantities considerable deviations can be observed in the theoretical results for the equivalent strain. The values derived with the upperbound method seem to be unrealistic. Improvement could possibly be reached by alternative choices for the velocity fields. Futural research on this matter is necessary. Comparing the present upperbound results with earlier work, see [8] formula (C11), a difference in shear effects at the surface AB (Fig. 3.1) can be noticed. In the usual coining process a dead metal-zone beneath the engraving can be expected, see for example [2], which explains the above mentioned difference. The finite element method is suitable to obtain detailed information, especially as regards the distribution of stress and strain. However because of the high computational effort it is not recommendable for parameter research. For that purpose the upperbound method deserves preference. Nevertheless, as described before, finite element results can give a deeper understanding, that can be advantageous for the elaboration of less time-consuming methods.

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