Chapter 2

Micro Forming Processes

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2.1 Introduction to Micro Forming Processes

Bernd Kuhfuss

The projects of this chapter describe micro forming processes that are studied as single processes but can also be combined as process chains. Proven examples are material accumulation and succeeding rotary swaging, or rotary swaging and extrusion.

Micro forming differs from macro forming due to scaling effects, which can mean both challenges and benefits. Problems may arise from the handling of fragile parts or adhesive forces between the micro parts or friction effects.

Benefits from scaling effects are made use of in the project “Generation of functional parts of a component by laser-based free-form heading”. The aim is a material accumulation that is generated from short duration laser melting. This material accumulation gives the basis for succeeding cold forming operations. The first powerful application for the new technology was upsetting. In the macro range, upset ratios of about 2.3 are achievable, but this is reduced in the micro scale due to earlier buckling of the components. In the micro scale, where cohesive forces can exceed the gravitational force, the molten material forms a droplet that remains adhered to the rod. Thus, upset ratios of up to 500 were reached. The process development was accompanied by a mathematical model and allows for a deep insight into the thermodynamics of laser-induced material accumulation in the micro range.

The laser molten material accumulation could, for example, be further processed by micro rotary swaging. Though rotary swaging has been known in the macro range for a long time and is nowadays an intensively used process in the automotive industry to produce lightweight components from tubular blanks, there are only a few scientific works that have addressed material characteristics like the work hardening or residual stress that are linked to the process and machine parameters and the resulting material flow. Due to micro scale specifics that follow from the kinematics, i.e. relatively smaller stiffness against part buckling and wider tool gaps in the opened state, the feed rates achievable cannot compete with high throughput technologies that produce 500 parts per minute and more. One major aim of the project “Rotary swaging of micro parts” was to increase the productivity for the main process variants, namely infeed and plunge rotary swaging. This demanded also process modeling to understand how parameter variations like friction between tools and the work in the different zones of the swaging tools affect the process.

From the modeling and simulation, separate process variations were deduced and investigated. For infeed swaging, a special workpiece clamping allows for compensation of the pushback force, which results in a 10-fold increase of the maximum feed rate. For plunge rotary swaging, an approach was tested to close the tools gaps during opening using elastic intermediate elements that encapsule the workpiece against the forming dies and enable a 4-fold increase of the radial feed rate.

Micro rotary swaging could become a base technology for process chains. Besides the combination with material accumulation, where the final geometry is formed by swaging, swaging can also be a preliminary step for subsequent operations. This was in the focus of the project “Process combination micro rotary swaging/extrusion”. In general, blanks for extrusion processes are produced by wire drawing, which provides high velocities, good surfaces and a diameter with close tolerances. Substitution of wire drawing by rotary swaging gives promising results in some applications. Whereas rotary swaging cannot compete with respect to process velocities, it can offer advantages when certain material properties are needed. In addition, the swaging process can be designed in such a way that the work surface shows micro lubrication pockets, which is also favorable in later extrusion processes.

One effect of the downscaling of the forming of micro parts is the relatively closer tolerances for production. In deep drawing processes, the geometries of the punch, center deviation of the punch, drawing gap and blank position with respect to the drawing die all influence the robustness of the process. These interdependencies are studied in the project “Influence of tool geometry on process stability in micro metal forming”. In the micro range, friction between the work, punch and drawing radius plays an important role and leads to variations that limit the usable process windows. Numerical methods and experimental research allow the detection of the geometric influences and their individual contribution to the work quality and process stability.

2.2 Generation of Functional Parts of a Component by Laser-Based Free Form Heading

Christine Schattmann*, Mischa Jahn, Alfred Schmidt and Frank Vollertsen

Abstract To overcome the disadvantages like buckling in upsetting processes in micro range, an alternative two-stage shaping process has been developed. This two-stage process consists of a master forming and a subsequent forming step. During master forming, a laser heat source is applied to a workpiece melting the material. Because of size effects the melt pool, whose size depends on the radiation time and the radiation strategy, stays connected to the part. After switching off the laser, the solidifying melt preserves its shape to the so-called preform. Finally, a subsequent forming stage ensures gaining the desired geometry. Since the laser melting process is very stable, a constant part quality is reachable even in linked part manufacturing. Here, cycle rates up to 200 parts per minute enable an industrial application. The reached microstructure, which is defined by the solidification process, has good properties for the subsequent forming process. Thus, a high formability can be achieved.

Keywords Laser · Cold forming · Finite element method (FEM)

Miniaturization is becoming increasingly important for the generation of functional parts. Due to the decreasing the size and increasing the range of the functions of many components, new manufacturing processes are necessary to overcome difficulties that arise when processes such as bulk metal forming are transferred from the macro to micro range. Conventionally, a multi-stage upsetting process with an increased process complexity and number of cycles with high demands on handling is necessary to achieve large plastic forming. This results in a time consuming and, therefore, expensive process. Furthermore, the formability is reduced as the size of the specimen decreases [Eic10], which is also true for the maximum value of natural strain which leads to defects in grains [Tsc06]. While the advantages of the bulk metal forming process upsetting are high output rates with small deviations [Lan02] and low waste of material [Lan06], the maximum achievable upset ratio is only about 2.3 in the macro range. Unfortunately, if applied to workpieces in the micro range, the upset ratio reduces to less than 2 [Meß98]. This happens due to buckling effects that occur faster because it is based on the shape inaccuracy of the specimen and of the tool as well as their perpendicularity to each other [Vol13]. To overcome these issues, a new laser-based upsetting process has been developed [Vol08].

The new process consists of two stages: the master forming step, generating a material accumulation; and a subsequent micro cold forming step, in which the specimen reaches its final geometry. In the master forming process, a laser is used

to melt a metallic workpiece. Due to the shape balance effect [Vol13], the molten material tends to form a spherical melt pool and remains connected to the workpiece for as long as the surface tension exceeds the gravitational force. This sphere-like shape is conserved even after solidification and the generated material accumulation is called a preform. The process is limited by the maximum thermal upset ratio, which is achieved just before the molten material detaches from the workpiece. In the second process stage, rotary swaging or die forming are used to form the solidified preform into the desired geometry.

The laser-based free form heading process is applicable for two workpiece geometries: within or at the end of a thin rod, and on the rim of a thin sheet. The main investigation areas of the first process stage are the radiation strategy, the energy balance including the laser melting as well as the solidification of the fluid melt pool, and the reproducibility. Afterwards, during the second process stage, the formability, including the yield stress and the natural strain, are significant to reach the final part geometry. Given that handling of micro parts is challenging [Chu11], linked-part combinations are required to process the usually high number of micro parts to different manufacturing machines.

2.2.1 Laser Rod End Melting

The laser rod end melting process consists of two stages. The first step is the thermal upset process, which generates the material accumulation at the end of the rod. Here, the energy equilibrium and the solidification influence the reproducibility and the microstructure of the preform. In the second step, die forming or rotary swaging leads to the final geometry of the part. With regard to industrial applications, the manufacturing of linked part production is also implemented.

2.2.1.1 Thermal Upset Process

To avoid the disadvantages of conventional upsetting processes, a preform at a cylindrical, metallic rod with diameter $d_0 \leq 1.0$ mm can be generated by laser rod end melting. This alternative process, which is shown in Fig. 2.1, is based on applying a laser heat source to the end of the rod, thereby melting the material. The size of the melt pool depends on the amount of energy absorbed by the workpiece, and can be controlled by adjusting the laser beam power and pulse duration. Due to the surface tension, the melt forms spherically to minimize its surface area. By melting the material, the length of the stationary rod reduces and the spherical melt pool, which stays connected to the rod, moves upwards during the process. Thus, the thermal upset ratio given by $u = \frac{l_0}{d_0}$ increases, where l_0 denotes the molten length and d_0 the rod diameter. The thermal upset ratio is limited by the volume of the melt by which the gravity force exceeds the surface tension causing the melt to drip off

Fig. 2.1 Upsetting **a** multi-stage process conventional upsetting, **b** alternative process laser-based free form heading with only two process stages [Brü16b]

the rod. With decreasing d_0 it is possible to achieve high upset ratios $u \gg 100$; for example, $u \approx 500$ for $d_0 = 0.2$ mm [Ste11]. Thus, the mechanical upset ratio of less than 2 in micro range can be significantly exceeded by the thermal upset ratio of the new process.

To avoid oxidation during the process, a shielding gas atmosphere with Argon is created. After switching off the laser heat source, the melt solidifies rapidly. Therein, the cooling rate has a significant effect on the microstructure and, thus, on the formability of the generated preforms. The final geometry of the part is reached during the subsequent forming process, such as rotary swaging or die forming. In this regards, good reproducibility, high geometrical accuracies, and small reject rates show the advantages for industrial applications in the micro range.

2.2.1.2 Process Stages and Radiation Strategy

The master forming process can be subdivided into three process stages: the radiation stage, an intermediate or dead phase, and the cooling stage. To melt the material during the radiation stage, two strategies are possible for the generation of preforms at a rod: coaxial rod end melting and lateral rod end melting. The idea of the coaxial approach is to orientate the rod and the laser heat source coaxially to each other and keep both stationary [Vol08]. In the initial setup of this process design, the rod is positioned in the focus plane of the laser, which is pointing to its head surface, see Fig. 2.2a. In contrast, the lateral process design is based on positioning the laser laterally to the rod and deflecting it along the material using a laser scanner [Brü15a], cf. Fig. 2.2b. Both strategies have advantages, as will be described in the following sections. After switching off the laser, the melting process continues due to the surplus energy that is already added to the rod. This process stage is called the intermediate or dead phase and its duration depends on the amount of energy overheating the melt. When all of the remaining energy is

Fig. 2.2 Radiation strategy: **a** coaxial orientated laser beam, **b** lateral orientated laser beam [Brü16b]

consumed, the solidification process starts, which essentially determines the formability of the produced specimen.

2.2.1.3 Modeling and Simulation of the Master Process

Due to its importance for the subsequent forming step and the final result, a detailed analysis of the master forming process and the quantification of the impact of the process parameters on the generated preform is required. Basically, the master forming process and occurring effects can be described by analytical models and result from the similarity theory [Brü12a]. However, a full continuum mechanical model and a accurate corresponding finite element method (FEM) is needed to perform the required throughout process analysis. The modeling and simulation framework can then also be used to find suitable parameter ranges.

From a mathematical point of view, the process can be modeled by coupling the two-phase Stefan problem [Eil81] and the incompressible Navier-Stokes equations with a free capillary surface [Bän01]. Within the thermal problem, laser heating, thermal conduction in the melt and the bulk, cooling described by the Stefan-Boltzmann law, and also the forced convection caused by shielding gas have to be considered to capture all of the relevant effects. The dynamics in the melt are dominated by the movement of the solid-liquid interface and the thermal advection. The resulting partial differential equation (PDE) system is coupled and non-linear [Bän13].

Several finite element approaches have been developed to numerically simulate the process, which are suited to take into account the different radiation strategies in a computationally efficient way. A 2D rotational symmetric approach is used to simulate the process based on a coaxial process design [Jah12a]. Therein, an

arbitrary Lagrangian-Eulerian method (ALE) [Bän13] is coupled with an approach based on considering the enthalpy in the workpiece. The throughout analysis of the numerical method is given in [Lut18]. Other than the coaxial process, the lateral process design requires a full 3D simulation [Jah12a]. As a result of the findings in [Lut18], an approach merely based on an enthalpy method was implemented [Jah14b]. To analyze the cooling process and take into account the interaction of melt and shielding gas, the extended finite element method (XFEM) can be used. This method belongs to the class of unfitted approaches and it allows us to decouple the physical domain from the discretization by extending the approximation space. This has the advantage that it allows for a more precise simulation because not only the workpiece but also its surroundings can be considered. In contrast, conventional finite element approaches are based on representing the physical domain by the computational mesh and, therefore, can only consider the impact of the workpiece surrounding via boundary conditions.

After having been validated by comparing the numerical results to the experimental data, such as in regards to the shape of the preform [Jah12], the solidification process [Jah14b], and the energy balance with respect to the self-alignment capability of the process [Jah12a], the finite element simulations are used for parameter identification and for further process analysis.

2.2.1.4 Energy Impact and Heat Dissipation Mechanisms

As previously mentioned, the results of the master forming step are primarily dependent on the amount of energy available for the melting process [Brü12b]. This energy depends not only on the total amount of energy $E = P_L t_L$ applied to the workpiece but also on the composition of the energy, meaning the choice of the laser power P_L , and the radiation time t_L to generate a certain energy E . This happens because the laser power has a significant influence on the absorption rate and the heat dissipation mechanisms. In addition, the divergence of the laser beam has to be taken into account. All these aspects are highly dependent on the radiation strategy and the chosen process parameters.

For laser-based processes, the absorption rate is typically given by the Fresnel equations. For the process at hand, the absorption rate is then approximately 0.38 [Wal09]. However, by significantly increasing the laser beam power P_L , and thus its intensity, a vapor capillary can be created in the melt pool. In this capillary, which is called keyhole, laser rays are reflected multiple times, which leads to abnormal absorption and results in an increasing absorption rate of up to 0.80 (see e.g. [Hüg09]).

A first approximation of the amount of energy E_S needed to generate a preform of volume V_S can be derived using an adiabatic model consisting of a linear relation of absorbed energy and preform volume [Vol08]. However, since this model neglects the fact that energy is permanently dissipated by radiation and convection and, moreover, transported into the non-melting part of the rod by conduction, it only provides a rough estimate. Finite element approaches are used for a precise

Table 2.1 Process parameters for preform generation

Rod diameter	0.40 mm
Laser power	130 W
Pulse duration	100 ms
Absorption coefficient	0.38
Shielding gas	Argon
Deflection velocity	30 mm/s

analysis of the process and the prediction of the impact of different radiation strategies or process parameters on the results.

The heat fluxes for the coaxial and lateral radiation strategy are compared by using the process parameters specified in Table 2.1. The corresponding heat fluxes, which are the absorbed laser energy and cooling caused by the Stefan-Boltzmann law and convection, are and visualized in Fig. 2.3. Therein, one can see that the energy available for the process in both situations differs significantly and, therefore, causes different preform volumes. The main reason for this is that in the coaxial design, the laser heat source always affects the molten part of the rod so that the energy has to be transported through it to melt more material. Thus, the overall temperature of the melt rises permanently, thereby causing more heat dissipation so that the efficiency of the process with respect to the applied energy decreases. Moreover, the divergence of the laser beam has a high impact on the result when using the coaxial radiation strategy. While the rod is initially positioned in the focal plane of a defocused laser beam that is pointing to its head surface, the melting process shortens the rod. Consequently, the melt leaves the laser's focus plane and the defocusing of the laser beam causes a significant drop of the absorption rate. In contrast, the deflection of the laser beam by a scanner in the lateral process design can guarantee that the laser energy is always applied to the rod near the solid-liquid interface. Furthermore, defocusing is no longer an issue since the lateral scanner position keeps the focus on the rod surface. For the lateral process design, optimal deflection velocities can be determined which depend on the rod diameter [Brü14b]. The results, visualized in Fig. 2.4, show that this velocity is proportional to the laser power and anti-proportional to the square of the rod diameter.

Besides the radiation strategy and the laser beam intensity, a closer look at the process analysis reveals that small changes of the terms of the energy losses can already lead to different results. This happens because of the heat dissipation mechanisms, which depend, partly in a highly non-linear way, on the difference of ambient temperature and the temperature of the preform. In [Jah14], a throughout study quantifying heat dissipation mechanisms for laser based rod end melting is given. Therein, it has been shown that energy is primary dissipated during the radiation stage by the cooling due to the Stefan-Boltzmann law while heat conduction and convection have only a small impact. Because the cooling due to the Stefan-Boltzmann law involves the difference $T^4 - T_a^4$, with T denoting the temperature at the surface and T_a is the ambient temperature, using a high laser power usually results in higher temperatures, which causes more dissipation of energy.

Fig. 2.3 Comparison of energy fluxes for the coaxial and lateral radiation strategy

Fig. 2.4 Optimal deflection velocity for different rod diameters [Brü16b]

2.2.1.5 Solidification and Microstructure

After switching off the laser, the cooling process starts. This process stage, consisting of the dead phase after switching off the laser and the subsequent solidification process, is crucial for the industrial application of this process. One reason for this is that its duration is the dominating factor of the total process time [Brü13c]. Hence, decreasing the cooling time is mandatory to upscale the process and rapidly generate a high number of preforms. Furthermore, this process stage also defines the microstructure of the preform and, therefore, its formability.

Unfortunately, the duration of the cooling process cannot be considered independently from the radiation stage because it highly depends on the temperature distribution in the melt. In particular, the duration intermediate stage after switching off the laser can be controlled by choosing adapted process parameters. Suitable process values can be determined by combining the finite element simulation with the concept optimization via simulation [Wan07].

In contrast to the melting process, where most energy is dissipated by radiation, the cooling process is governed by convection. Consequently, it can be primarily

controlled by changing the shielding gas atmosphere. The duration of the cooling and solidification process has been quantified in [Brü13a], where it has been shown that the cooling time is highly dependent on the diameter of the preform.

The cooling process affects the generated microstructure of the preform, which essentially depends on the temperature gradient at the solid-liquid interface and its velocity [Kur98]. In general, a high interface velocity and a rather small gradient is desirable because it leads to a globular microstructure. A rough estimate of the microstructure can be obtained by an analytical model [Brü16b]: assuming a constant heat dissipation due to radiation and convection, the dependency of the secondary dendrite arm spacing S_D on rod diameter d_0 and the preform diameter d_{PF} is given by Eq. (2.1)

$$S_D = 5.5 \cdot B^{\frac{1}{3}} \cdot \left[\frac{\frac{1}{\pi} \rho V_{PF} H_M}{\dot{q}_{HC} \frac{d_0^2}{4} + \varepsilon \sigma_S \left(d_{PF}^2 - \frac{d_0^2}{4} \right) (T_m^4 - T_\infty^4) + \alpha \left(d_{PF}^2 - \frac{d_0^2}{4} \right) (T_m - T_\infty)} \right]^{\frac{1}{3}} \quad (2.1)$$

with the solidification and material specific constant B and the solidification time, described by the density of the material ρ , the volume of the preform V_{PF} and the melting enthalpy H_M in the numerator and the different heat flows in the denominator. Here, the heat conduction $\dot{q}_{HC}$, the emissivity ε , the Stefan-Boltzmann constant σ_S , the melting temperature T_m , and the ambient temperature T_∞ are substituted in the calculation. The model for the expected solidification interval and the secondary dendrite arm spacing shows good agreement with the experiments for smaller solidification intervals, as shown in Fig. 2.5 [Brü16b].

Fig. 2.5 Comparison of solidification interval between model and experiment for different rod diameters [Brü16b]

2.2.1.6 Reproducibility

The laser rod end melting process is very stable with respect to small deviations in the process design and choice of process parameters. For example, the coaxial process has a self-aligning capability which means that the generated preform volume is independent of deviations in rod diameter as long as the pulse energy and the radiation time are scaled accordingly to results from the theory of similarity [Brü12a]. In principle, the coaxial design is prone for a decreasing energy efficiency due to defocussing because of the static position of the focus plane in this design. Therefore, less melting volume can be gained for increasing thermal upset ratios, because the rod shortening increases the distance between focus plane and melt surface, see [Brü15a]. However, this issue can be resolved using a feeding system for the rod. Then, the rod is continuously kept within the focus plane of the laser and, as a result, the thermal upset ratio can be increased to a maximum of $u \approx 500$ [Ste11b]. This means that the energy efficiency is doubled [Ste11a]. Despite this improvement, there are still deviations in the thermal upset ratio compared to the lateral radiation strategy. While the fluctuations of specific melting volume are lower for coaxial radiation, the absolute volume in the lateral process design is still generally higher, making this radiation strategy more energy efficient [Brü15a]. The reasons for this are the different effects of the heat dissipation mechanisms, which were previously described.

With regard to the positioning of the laser beam onto the rod in the lateral process design, Brüning showed in [Brü13e] that positioning deviations up to 100 μm in vertical direction do not essentially affect the resulting preform diameter. Moreover, if the total axial and radial positioning is considered, then the deviations need to be significant less than 100 μm , thus a good initial situation for the subsequent forming process is reached.

As described, the absolute preform volume is proportional to the applied pulse energy [Brü12b]. Differences between the theoretical melting volume and the final volume of the preform can be explained by material losses during the laser process. Especially during the abnormal absorption, vaporization caused by keyhole formation can take place introducing high dynamics into the process. This dynamic can result in spatter formation and, hence, in material losses. Using the lateral radiation strategy, relative mass losses between 0.5 and 2.6% related to the thermal upset ratio and the rod diameter can be measured [Brü16b]. The volume of the preform correlates with the diameter of the solidified preform. Using the coaxial radiation strategy, an increase of preform diameter leads to decreasing eccentricity with a decreasing standard deviation. If the lateral radiation strategy is considered, then the eccentricity, see Fig. 2.6, is lower in a range of 40–60 μm but with a slight fluctuation and a standard deviation of about 10 μm as stated in [Brü16a].

Fig. 2.6 Comparison of simulation results for the laser rod end melting process with coaxial and lateral radiation strategy

2.2.1.7 Formability

The preforms achieve their final geometry during the subsequent forming process. Therefore, the yield stress, the natural strain, and the form filling behavior are of interest. In [Brü13c], it is shown that for preforms with diameters between 1.1 and 2.1 mm, it is possible to reach a maximum average logarithmic natural strain up to 1.7 without damaging the forming tool. Further investigations based on simulations of the forming process show that the influence of the friction coefficient on average natural strain can be neglected [Brü14c]. Here, the distribution of natural strain is inhomogeneous, primarily because the center of the preform interacts with the tool and, thus, increases the centered natural strain. Experiments show that a preform with an initial diameter of 420 m having a dendritic grain structure results in a convex formed specimen with a height of 27 m, even though the surfaces of the open die tool have been planar, as shown in Fig. 2.7. This investigation leads to a maximum averaged natural strain of 2.75 without any occurring defects. Further forming resulted in a plastic deformation of the tools due to strain hardening of the specimen.

The yield stress level increases linearly with the increasing absolute value of average natural strain. Furthermore, a higher secondary dendrite arm spacing leads

Fig. 2.7 Part after open die forming (simulation of averaged logarithmic natural strain: blue 0, red -2.5; the model consists of rigid surfaces)

Fig. 2.8 Upsetting of cylinders in micro range with different microstructure depending on average natural strain ϕ , [Brü16b]

to a decreasing yield stress level, see [Brü14a]. Further yield curves arose using an universal testing machine [Vol13]. Here, the yield strength of the preforms that is investigated by upsetting experiments of cylinders is comparable to that of the same material and size with different microstructure [Brü13d]. However, the forming behavior changed with the different microstructure, such that the final geometry after open die forming is different. Within the experiments using globular microstructure inside the cylinder, only barreling occurs. Using the dendritic microstructure of the preform, barreling occurs also, but the size of the front surface is increasingly in homogeneously, as shown in Fig. 2.8 [Brü16b].

During master forming, the eccentricity of the preform with respect to the rod is defined. During the experiments, deviations up to 100 μm depending on the rod diameter can arise. However, the subsequent forming stage also includes a natural calibration step, which reduces the eccentricity to less than 30 μm and which is assumed to be related to the deviation of the coaxial alignment between the upper and the lower die. Furthermore, if an oversized volume of preform is used, then the forming process can be successful without any burr formation. This is possible because the material flows not only inside the cavity but also in direction of the rod. Thus, preform volumes of up to 125% of the die volume can fill the cavity completely without any burr, see [Brü15b].

Finally, it is possible to mold fine surface structures after the forming stage, such as the surface roughness of the die, see [Brü14a], down to feature sizes of 500 nm which is investigated in [Brü15c]. In Fig. 2.9 the different geometries of truncated cone formed parts are shown. Here, the average of natural strain of the manufactured parts is well recognizable.

2.2.1.8 Linked Part Production

Given that the handling of the micro parts is always challenging, different linked part systems are investigated to improve the possibilities for mass production and

Fig. 2.9 Part after closed die forming using different averages of natural strain [Brü16b]

Fig. 2.10 Linked-part production: **a** line-linked, **b** comb-linked, and **c** plate-linked

upscaled output rates during manufacturing process. This includes line-linked parts and comb-linked parts.

For line-linked parts, the thermal upsetting process is performed using a continuous rod, see Fig. 2.10a. The stationary laser process takes place in the middle of a rod. Further rod material is fed into the process zone, so that a material accumulation and thereby the preform arises. A variation of feeding times and laser power result in different preform geometries, which can be formed from round to flat [Sch17b]. Investigations show that the energy efficiency and the cycle time can be improved so that more than 222 parts can be generated per minute [Sch17d].

Because the geometry of the preform is changed slightly due to the surface tension, the comb-linked strategy can be an alternative. In Fig. 2.10b it is shown that the workpiece rod is connected to a conveyor rod. After cutting the workpiece rod, the preform can be generated as usual [Brü13b]. This comb-linked strategy was tested for wire diameters from 0.2 up to 0.5 mm. Depending on the preform diameter, the solidification time varies. Thus, the dead time between laser process and feeding process should be adjusted, see [Jah13].

This linked strategy can be extended by placing the workpiece rods on a carrier plate [Sch17a], as shown in Fig. 2.10c. This opens new possibilities of applications,

thus a metallic detachable connection can be introduced. Here, a shear stress of 69 N/cm^2 can be transmitted by a metallic hook-and-loop fastener [Brü14c].

2.2.2 Laser Rim Melting

The laser melting process can also be applied to the rim of metal sheets. Using the lateral process design, a laser beam is deflected with a defined machining depth along the rim of a thin metal sheet [Woi15], as shown in Fig. 2.11. The material starts melting and a molten drop arises which stays connected to the sheet because of surface tension. By deflecting the laser beam along the rim, more material is molten and the melt pool moves along the rim. Due to the physical dimensions of the workpiece, the rear part of the melt solidifies rapidly so that the melt pool always has a specific length which depends on the process parameters and the thickness of the sheet. In general, the process can be used to generate continuous cylindrical preforms and, thus, preforms with irregularities at sheet rims (see Fig. 2.12b) are avoided (see Fig. 2.12a).

In [Brü17], the influence of the melt pool dimensions on the continuous generation of cylindrical preforms was investigated. Here, a length-to-height ratio of 3.0 ± 0.4 of the melt pool was achieved for a blank thickness of 0.2 mm . Furthermore, an analytical model taking into account the surface area of the melt pool not only supports this result but is also able to predict the maximum allowable melt pool length to gain a continuous cylindrical melt pool. Otherwise, instabilities occur, which are comparable to the humping effect during laser welding. In Fig. 2.13, it is apparent that the local frequency of periodical maxima in a continuous preform is influenced linearly by the laser power and by the deflection velocity for sheet thicknesses of 25 up to $100 \text{ }\mu\text{m}$. Here, a high accordance to the results of humping during laser welding by Neumann [Neu12] is viewable. This happens despite the fact that Neumann conducted experiments with a laser power in

Fig. 2.11 Process laser rim melting including simulated heat distribution

Fig. 2.12 Continuous preform on a metal sheet with a thickness of 70 μm : **a** cylindrical, **b** irregular [Sch17c]

Fig. 2.13 Comparison between humping during laser welding [Neu12] and irregularities during laser rim end melting

the range of some kW and much higher travel speed than used in the current rim melting experiments.

The process can also be applied to closed rims, such as voids in metal sheets. Using the results obtained for the generation of continuous cylindrical preforms, it is for example possible to generate a preform with a height of 0.4 mm at a void of a metal sheet with a thickness of 200 μm , cf. Fig. 2.14a for the simulation result. Thus, it is possible to cut a thread of M2 inside [Sch17c]. Figure 2.14b shows the corresponding scanning electron microscope (SEM) image of one thread with a thickness of 400 μm .

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Fig. 2.14 Laser rim melting at void: **a** Simulation, **b** a M2 thread is cut into a continuous cylindrical preform [Sch17c]

The wide range of possibilities for the laser-based free form heading process is appreciated, which is expanded from the rod to the metal sheet, so that more new applications can be exploited and industrial integration can be expected.

2.3 Rotary Swaging of Micro Parts

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Abstract The incremental forming process of micro rotary swaging can be divided into two process variations. During infeed rotary swaging the diameter of a workpiece is reduced over the entire feed length of the workpiece. However, during plunge rotary swaging the diameter is reduced within a limited section by radially feeding the dies. Both variations feature limited productivity due to their incremental nature. An approach to increase the productivity is by increasing the feed rate. However, due to the fixed kinematics, a higher feed rate results in a larger volume of deformed material per stroke, which could lead to failures particularly due to inappropriate material flow. In infeed rotary swaging at a high feed rate, the flow of the workpiece material against the feed direction can result in bending or breaking of the workpiece. In both infeed and plunge rotary swaging, the workpiece material can flow radially into the gap between the dies and provoke the formation of flashes on the workpiece. For plunge rotary swaging the radial flow is also a motion against the feed direction. Measures to control both the radial and the axial material flow to enable high productivity micro rotary swaging are presented. An adjusted clamping device enables an increase of the productivity by a factor of four due to a reduction of the axial forces generated by the undesired axial material flow in infeed rotary swaging. The radial material flow during plunge rotary swaging can be controlled by intermediate elements. Thus, an increase of productivity by a factor of three is possible. Furthermore, the geometry after plunge rotary swaging is strongly influenced by the workpiece clamping and fixation, and the material flow can be controlled by applying low axial forces to the workpiece on one or both sides of the forming zone.

Keywords Forming · Productivity · Quality

2.3.1 Introduction

Micro rotary swaging is an incremental open die forming process to reduce the cross-section of axisymmetric workpieces. Concentrically arranged dies are oscillating with a given stroke frequency f_{st} and a stroke height h_T . The dies strike simultaneously on the workpiece to deform it gradually from the initial diameter d_0 to the final diameter d_1 [Kuh13]. The main components as well as the kinematics (white arrows) are presented in a front view of a rotary swaging machine in Fig. 2.15.

The nominal diameter d_{nom} is defined as the inside diameter of the forming zone of a closed die set. The two main variants of the process are infeed rotary swaging and plunge rotary swaging. During infeed rotary swaging the workpiece is axially

Fig. 2.15 Front view of a rotary swaging machine without end cover

Fig. 2.16 Rotary swaging variants: **a** infeed; **b** plunge [Mou18b]

fed with the feed rate v_f into the swaging unit and is reduced over a length l_0 (see Fig. 2.16a). However, during plunge rotary swaging the workpiece is processed locally as the dies are fed radially with a feed rate v_r while oscillating (see Fig. 2.16b).

Besides the presented developments in micro rotary swaging, investigations on rotary swaging by other authors were made in the macro range and focus on the workpiece properties after forming and on new applications for the process. For example, Alkhazraji et al. found that both tensile and fatigue strength are enhanced in workpieces with ultra-fine grains made by rotary swaging [Alk15]. The process was also analyzed for joining tubes [Zha14] or for plating axisymmetric metallic parts [Sch16].

With regard to high productivity in the micro range, infeed rotary swaging can already facilitate several hundred parts per minute by a continuous process, for example as a preliminary stage for extrusion [Ish18]. But there is still potential for further productivity increases. Plunge rotary swaging enables the manufacturing of variable types of geometries. However, the productivity is considerably low [Kuh09a], due to the low feed rates v_r used to prevent an undesired flow of the workpiece material into the gaps between the dies. Still, for both process variants, high productivity can be achieved by controlling the material flow and the positioning of the workpiece.

The degree of incremental deformation φ_{st} is a common value in rotary swaging to describe and quantify the deformation. It is defined by the initial diameter d_0 and the effective feed per stroke of the die h_{st} (Eq. 2.2). For infeed rotary swaging h_{st} equals the axial workpiece feed per stroke l_{st} , which itself depends on the die angle α and on the ratio of the workpiece feed rate v_f , over the stroke frequency f_{st} (Eq. 2.3). For plunge rotary swaging, h_{st} depends on the ratio of the feed rate of the dies v_r over the stroke frequency f_{st} Eq. 2.4.

$$\varphi_{st} = \ln \left(\frac{d_0 - 2h_{st}}{d_0} \right)^2 \quad (2.2)$$

$$h_{st_infeed} = l_{st} \tan \alpha = \frac{v_f}{f_{st}} \tan \alpha \quad (2.3)$$

$$h_{st_plunge} = \frac{v_r}{f_{st}} \quad (2.4)$$

The previous equations show that the degree of incremental deformation φ_{st} does not consider the final diameter d_1 . However, this parameter is involved in the volume per stroke V_{st} , which represents the material volume formed by the dies at each stroke. For an idealized forming, V_{st} for infeed rotary swaging can be determined with Eq. 2.5 and for plunge rotary swaging with Eq. 2.6. For plunge rotary swaging the deformed volume of the workpiece is assumed to have a cylindrical shape. In Eq. 2.6 k stands for k -th incremental step.

$$V_{st_infeed} = \pi l_{st} \left(\frac{d_0^2}{4} - \frac{d_1^2}{4} \right) \quad (2.5)$$

$$V_{st_plunge}(k) = l_{form} \frac{\pi}{4} \left[(d_0 - kh_{st})^2 - (d_0 - (k+1)h_{st})^2 \right] \quad (2.6)$$

In order to improve the micro rotary swaging process, it is necessary to understand and control the material flow. Three methods to improve the rotary swaging in the micro range based on the material flow control are presented. In infeed swaging a higher axial feed rate is enabled by using an adjusted workpiece

clamping [Mou18a]. In plunge rotary swaging the application of an elastic tube prevents flash formation [Mou15b, Mou15b] and with the application of external axial forces the part geometry can be controlled [Mou18b].

2.3.2 Process Limitations and Measures for Their Extension

During micro rotary swaging different limitations to the process occur [Kuh08a]. Furthermore, process parameters like the workpiece material, the die geometry, and the tribological conditions influence these limitations.

Typical limitations of the process can be divided into two categories (see Fig. 2.17). The limitations in the first category lead to a termination of the process. These are bending, torsion or breaking of the workpiece. The limitations in the second category are those that affect the workpiece quality. Examples are flashes, high surface roughness [Wil13] and shape deviation [Kuh09b]. A guiding element with a hole slightly larger than d_1 for the workpiece at the inlet in the swaging head is used to prevent premature limitations due, for example, to vibrations occurring in the process [Wil15]. Furthermore, the use of a guiding element, which is necessary and specific for the micro range, reduces the risk of the workpiece getting into the tool gap during the process and thus enables the application of higher strokes.

During rotary swaging material flow takes place predominantly in the axial and radial directions. The axial material flow occurs in both the positive and negative directions. Higher material flow against the axial feed direction induces an axial load that bends the workpiece when the critical bending load $F_{b,crit.}$ is reached. This

Fig. 2.17 Limitations of micro rotary swaging

Fig. 2.18 Range of feed rates for forming different materials with different dies without failure. $d_0 = 1.0$ mm
Lubricant: Condocut KNR 22

is a specific problem in micro rotary swaging due to the low stiffness of the slim workpieces. The application of a guiding channel for the workpiece minimizes the risk of bending.

Depending on the workpiece material and the diameter reduction, different maximal feed rates can be applied. Infeed rotary swaging experiments with three materials (AISI 304 steel, Cu-ETP, Al99.5) were carried out and the maximum feed rate with which 5 samples could be processed without process failure was determined. Workpieces with an initial diameter $d_0 = 1.0$ mm and a length of 110 mm were reduced by different tool sets with nominal diameters of 0.3, 0.4 and 0.5 [Kuh08b].

Forming was possible within a larger window of feed rate and thus higher V_{st} for materials with higher Young's modulus (see Fig. 2.18). Forming with feed rates above the boundary (lines) for the specific material will lead to failure on the workpiece. From the failures in Fig. 2.17, predominantly bending and torsion occur in infeed rotary swaging.

The tribological conditions and the workpiece material influence the material flow. Especially in micro rotary swaging, due to the slim workpieces, the surface-to-volume ratio is very high, whereby the tribological conditions have a strong influence. During infeed rotary swaging of workpieces from alloy 304 and from Al99.5 with and without lubrication of the workpiece, opposite behaviors of the achievable feed rate are observed [Kuh12b]. While under the dry condition (without lubrication) lower axial feed rates than with lubricant can be achieved for alloy 304, the forming of Al99.5 at higher true strain becomes possible without lubricant. In some cases, Al99.5 rods can be formed only without lubricant, i.e. a reduction from $d_0 = 1.5$ mm to $d_1 = 0.5$ mm and even to 0.4 mm.

However, during forming of aluminum without lubricant, adhesive wear can occur on the tools due to the affinity between the workpiece and tool material. By using tools with amorphous diamond-like carbon coatings, adhesive wear can be avoided and the tool life considerably increased. The tools were coated using

magnetron sputtering. Pin-on-disk tests have shown that such coating results in a considerable reduction of friction and wear against aluminum.

For high volume per stroke V_{st} , inadequate material flow in the radial direction can lead to flashes on the workpiece. Because the material cannot flow completely in the axial direction, the generated surplus flows against the direction of the closing motion of the dies, which means into the gaps between the dies. This can be observed both in infeed rotary swaging and in plunge rotary swaging. Furthermore, the inadequate material flow can cause inaccurate geometries (incomplete filling of cavities or asymmetry) or destroy the workpiece irreversibly.

2.3.3 Material Flow Control

2.3.3.1 High Productivity in Infeed Swaging

In order to control the material flow against the axial feed direction and thus prevent process limitations like bending or even breaking, a spring-loaded clamping device for the workpiece was developed [Mou18a]. The clamping device allows an axial evasion of the workpiece. With a rotation lock, free rotation of the workpiece can also be prevented. Thus, the device enables the axial feed rate in infeed micro rotary swaging to be increased by more than four times compared to a fixed clamping. Feed rates higher than $v_f = 100$ mm/s were achieved. Particularly, incremental feed length l_{st} higher than the final diameter d_1 could be fed into the swaging unit, which is only realizable in the micro dimensions. However, the increased deformation per stroke due to higher feed rates leads to a higher forming, and so to high loads in the swaging head. The load leads to an elastic deformation of the outer ring and therefore causes a diameter profile.

Fig. 2.19 Diameter evolution at different feed rates for AISI 304 steel
 $d_0 = 1.0$ mm
 $d_{nom} = 0.75$ mm
 $h_T = 0.2$ mm. Lubricant: Condocut KNR 22

Figure 2.19 shows the final diameters after a reduction of rods from AISI 304 steel with an initial diameter of $d_0 = 1.0$ mm using a die set with a nominal diameter of $d_{\text{nom}} = 0.75$ mm and a calibration zone of $l_{\text{cal}} = 20$ mm. For low feed rates ($v_f < 4$ mm/s), d_1 is constant over the workpiece length and close to d_{nom} [Mou18a]. At high feed rates, however, the diameter evolves over the workpiece axis z' . The final diameter d_1 increases with the feed rate and becomes inhomogeneous. An increase of d_1 from the tip towards d_0 can be noticed. The region with the inhomogeneity is many times larger than the calibration zone of the dies. Similar findings are also found for infeed rotary swaging of bars in the macro range, but the inhomogeneous part is much shorter.

Haug explained the conical geometry of the tip of the workpiece with the stiffness behavior of the swaging head and especially for high feed rates with a material flow in the die gaps (flashes) [Hau96]. However, in the presented experiments in the micro range no flashes occurred. Haug reduced the length of the cone by increasing the preloading of the dies [Hau96].

In micro rotary swaging the evolution of the final diameter for high feed rates can be influenced by an in-process adjustment of the radial position of the dies (see Fig. 2.20). A combined axial workpiece feed and a radial die feed with constant feed rates of $v_r = 0.008$ mm/s and $v_f = 20$ mm/s resulted in a more homogeneous diameter evolution.

The stiffness of the swaging head plays a key role in the diameter evolution in the micro range and it is therefore necessary to analyze it during the process. This can be done by monitoring either the elastic deformation of the outer ring of the machine with strain gages [Kuh11] or by monitoring the die closing with a high-speed camera. With the strain gauge measurements, an increase of the elastic deformation of the swaging head with the feed rate of $v_f \geq 2$ mm/s is observed. For lower feed rates, the deformation remains constant as in the idle state before forming. An explanation is the increase of the deformed volume per stroke V_{st} with the feed rate. As a consequence, the outer ring of the swaging head expands more and the dies do not close completely, as can be seen in Fig. 2.21b compared with

Fig. 2.20 Uncompensated diameter evolution and compensated with $v_f = 20$ mm/s, $v_r = 0.008$ mm/s Material: AISI 304 steel $d_0 = 1$ mm $d_{\text{nom}} = 0.75$ mm

Fig. 2.21 High-speed camera images of the dies: **a** in the idle state; **b** during forming of an AISI 304 steel with $v_f = 20$ mm/s

the idle state in Fig. 2.21a. Due to the incomplete closing of the dies, the final diameter of the workpiece increases.

A rough approximation of the gap between the dies at $v_f = 20$ mm/s leads to a gap width of $55\text{ }\mu\text{m}$ and about $94\text{ }\mu\text{m}$ at 50 mm/s . The two gaps are larger than the corresponding maximal difference between the final diameter d_l and the die diameter d_{nom} ($50\text{ }\mu\text{m}$ at $v_f = 20\text{ mm/s}$ and $75\text{ }\mu\text{m}$ at $v_f = 50\text{ mm/s}$) (see Fig. 2.19). Although a discrepancy exists between the final diameter and the approximated gap width, which can be attributed to difficult accessibility and the measurement conditions, the determined values point nevertheless to a correlation between both.

At high velocities the incremental volume increases. This has an impact not only on the diameter evolution of the workpiece but also on the surface quality and the roundness. However, for velocities up to 50 mm/s these workpiece properties remain almost constant, as can be seen in Fig. 2.22 for the surface roughness. The mean value of the roughness in that velocity range is $2 \pm 0.6\text{ }\mu\text{m}$. The roughness of the small parts is determined from a cross-section of the workpiece. Using the Total

Fig. 2.22 Influence of feed rate on the surface roughness. Infeed rotary swaging $d_0 = 1.0\text{ mm}$ $d_{\text{nom}} = 0.75\text{ mm}$ $h_T = 0.2\text{ mm}$ Lubricant: Conducut KNR 22

Least Square Fits method, the roughness can be calculated from the extracted contour data.

2.3.3.2 High Productivity in Plunge Rotary Swaging

The main process limitation during plunge rotary swaging is the formation of flashes. Thus, to enable high radial feed rates, the radial material flow needs to be controlled. A new concept was developed to prevent the material from flowing radially into the gaps between the dies by the use of intermediate elements (IE). The IEs could be made of super-elastic metallic or polymeric substances (see Fig. 2.23). However, the elastic behavior of the currently available super-elastic metallic materials is still too low to allow high deformation as in rotary swaging. The use of elastomers in metal forming was summarized in [Thi93], and in [Chu14] a comparable approach for micro patterning was investigated.

Copper rods from Cu-ETP with $d_0 = 1.5$ mm were reduced using dies with $d_{\text{nom}} = 0.98$ mm. The stroke was $h_T = 0.3$ mm and different radial feed rates v_r were used. Furthermore, different IEs made of thermoplastic polyurethane with three different hardness grades were tested (low hardness = ILH; middle hardness = IMH and high hardness = IHH) [Mou18a]. For the reduction without IE, a maximum radial feed rate of $v_r = 0.34$ mm/s could be applied before flashes or breaks occurred. The IEs with high hardness enabled an increase of the feed rate up to about 300% (see Fig. 2.24a). The different IEs resulted in different final diameters: for harder IE the diameter increased due to the higher extensions of the outer ring of the swaging head. The extension was provoked by the IE that flowed into the gaps and blocked the closing of the dies. This resulted in an increase of the workpiece's final diameter. The tested IEs were not reusable (see Fig. 2.24b).

Fig. 2.23 Formation of flashes during rotary swaging: **a** workpiece with flashes; **b** inserted elastic intermediate elements

Fig. 2.24 Plunge rotary swaging with elastic intermediate elements: **a** maximal radial feed rate for different intermediate elements; **b** workpiece with intermediate elements after forming

Fig. 2.25 Influence of feed rate and intermediate element on the surface roughness. Plunge rotary swaging $d_0 = 1.0\text{ mm}$ $d_{nom} = 0.98\text{ mm}$ $h_T = 0.3\text{ mm}$ Lubricant: Conducut KNR 22

The surface roughness Sa and the corresponding standard deviation of the produced parts are higher than in the initial rods (Fig. 2.25). However, the increase of the productivity through the IEs does not affect Sa negatively.

2.3.3.3 Application of External Axial Forces in Plunge Rotary Swaging

Controlling the axial workpiece displacement in plunge rotary swaging influences the formation of the final geometry and the axial elongation of the workpiece. A practical way to control the material flow is by applying external axial forces F_a on the workpiece during the forming. These forces are in the order of a few Newton in contrast to the radial forming forces, which are in the range of kN.

Fig. 2.26 Setup for micro rotary swaging with external axial forces

Fig. 2.27 Workpiece geometry after plunge rotary swaging without and with external forces F_a : **a** no F_a ; **b** compression force on one side; **c** tension force on one side; **d** tension forces on both sides [Mou18b]

Forming experiments were conducted with alloy 304 with $d_0 = 1.5$ mm and a reduction by $\Delta d = 0.3$ mm by otherwise constant swaging parameters. Four different configurations without (A) and with external forces (B, C and D) were used with the setup illustrated in Fig. 2.26. In configuration B a compressive force was applied in the front, in configuration C a tensile force was applied in the front and in configuration D tensile forces were applied equally in the front and at the back of the rotary swaging machine [Mou18b]. The arrows in Fig. 2.26 represent the tensile forces in configuration D.

The geometry of the parts was investigated after forming. Symmetrical geometries were generated only when the same axial tensile forces were applied on both ends of the workpiece (see Fig. 2.27). Due to the freely moving workpiece in

the axial direction for configuration A, the geometry varied strongly. But with external forces only on one end of the workpiece, the geometry shifted in one direction. The material shift can be guided by the external axial forces and, thereby, the produced geometry can be controlled without changing the dies.

2.3.4 Characterization of the Material Flow with FEM

Using the finite element method (FEM), the material flow during the process is investigated. Two-dimensional axisymmetric simulations are commonly used to simulate rotary swaging [Gha08]. The model for infeed rotary swaging consists of two parts: the workpiece (deformable) and the die (rigid) [Mou14a].

One approach to characterize the material flow in rotary swaging is by analyzing the behavior of the neutral zone (NZ). The NZ represents an area in the region of the workpiece being deformed in which the workpiece material features no flow in the axial direction. A position closer to the initial diameter means a higher material flow in the direction of the feed motion. The location of NZ can vary due to the tribology. With a higher friction coefficient, the position of the NZ is shifting in the direction against the feed motion ($-z$) [Ron06]. Furthermore, the NZ is influenced by v_f as well other process settings, like the final diameter. It was found that the NZ changes its position as well as its shape during a single stroke (see Fig. 2.28). The gray area of the workpiece represents the material flowing against the feed direction, the bright area the material flowing in the feed direction and the boundary between both is the NZ. By an increase of the coefficient of friction value μ , the material flow in the feed direction within one stroke seems to increase. In order to analyze the NZ, a stroke was divided into many small and constant time steps. The number of detected NZ was found not to be equal to the number of time steps but to vary with the friction, the strain and the feed rate. The number of NZ increased with μ and furthermore a shift of the NZ in the negative z -direction was noticed (see Fig. 2.28a and b). The increased number of NZ is a sign of more internal deformation. As the NZ shape can change from concave to convex, the internal deformations are cyclic. Less material flow against the feed direction was observed in experiments without lubricant and thus with higher friction coefficients as in the FEM (see Sect. 2.3.2). For higher axial feed rates a similar trend to that for higher friction was observed, which can be explained by the higher volume per stroke which is deformed (see Fig. 2.28b and c) [Mou14a]. From these results, the improvement of the feed rate and strain range in the forming of the Al99.5 without lubricant can be attributed to the material flow, and more precisely the behavior of the neutral zone.

A further method to characterize the material flow dependent on the process parameters is the history of the plastic strain development. The plastic strain at the outer region of the workpiece is more sensitive to the friction coefficient value (see Fig. 2.29). This can also be seen at the NZ, since the shape is more curved at the surface for higher friction coefficients. But compared to rotary swaged rods in the

Fig. 2.28 Shape and position of the neutral zone in one single stroke of the forming of Al99.5 rods: **a** $v_f = 1$ mm/s and $\mu = 0.1$; **b** $v_f = 1$ mm/s and $\mu = 0.5$; **c** $v_f = 5$ mm/s and $\mu = 0.5$

Fig. 2.29 Plastic strain PEEQ over the workpiece radius after deformation for $\mu = 0.1$ and $\mu = 0.2$

macro range, at which only the outer 20% of the diameter is sensitive to friction [Liu17], in the micro range the PEEQ (plastic strain) is influenced over almost the complete radius. This can be explained by the small cross-section of the workpieces in which the surface to volume ratio is very high, thus the tribological conditions have a stronger influence.

2.3.5 Material Modifications

The interaction between the dies and the workpiece during forming leads to material modifications, such as in the mechanical properties and the microstructure. Figure 2.30 presents the Martens hardness in five different regions along a deformed workpiece of alloy 304. The scatter of the Martens hardness after forming (regions I to IV and beginning of V) reaches up to 250 N/mm² compared to about 160 N/mm² before forming (end of region V). The high scatter occurred because of the inhomogeneity of the microstructure (soft austenite and hard strain-induced martensite) and also the distribution of the hardness across the diameter, as can be

Fig. 2.30 Martens hardness of the workpiece measured after forming in five different regions, detailed sketch of the location and number of indentations, and three hardness profiles over the diameter in region II with hardness values between 3800 and 4400 N/mm²

Fig. 2.31 Workpiece properties after forming: yield strength (tensile test) and martensite content (magnetic induction) of AISI 304 steel

seen in detail in Fig. 2.30, where three hardness profiles in region II across the diameter $[-r, +r]$ are presented. The observed distributions reflect inhomogeneous work hardening through the diameter.

Workpieces of AISI 304 steel after rotary swaging present a strong hardening, as the yield strength increases significantly (Fig. 2.31). This increase is generally attributed to work hardening during cold forming. However, further specific phenomena like phase transition as well as dynamic effects influence the development of the material properties.

As is known for AISI 304 steel, martensite usually develops during cold forming. Using a Fischerscope MMS PC that works according to the magnetic induction method, the martensite content can be estimated (Fig. 2.31). The development of martensite with regard to the feed rate in the presented case can be approximated by Eq. 2.7. The equation is derived from the profile in Fig. 2.31.

$$\alpha_M = C * v_f^{-n} \quad (2.7)$$

α_M is high for low v_f and decreases when v_f increases. As the workpieces have the same initial diameter and nominal true strain, this behavior can be attributed to the number of strokes experienced by the workpiece and to an adiabatic heating during forming. At each stroke, new volume fractions of austenite are transformed into martensite, hence more martensite is present at low feed rates. When the feed rate increases, the deformed volume at each stroke also increases, which means the deformation work is higher. As a result, the workpiece temperature increases and the martensitic transformation is reduced. The yield strength, determined by tensile testing, is also high for workpieces manufactured at low feed rates and decreases almost linearly for the feed rate range between 2 and 8 mm/s. While the amount of

martensite is reduced from 60 to 10%, the yield strength is reduced only by about 30% within the investigated feed rate range. Therefore, the martensite-induced transformation in micro rotary swaging is sensitive to feed rate changes.

2.3.6 Applications and Remarks

Micro rotary swaging is an eligible convenient process when parts with high strength and high surface quality are needed [Mou14b]. Due to its incremental characteristic, it allows larger cross-section reductions compared to other forming processes. Moreover, not only rotationally symmetrical cross-sections are possible but also polygons. The smaller dimensions of the workpiece compared to the dimensions of available machines open new possibilities for adjusting rotary swaging for micro rotary swaging. Besides the manufacturing in linked parts (Sect. 2.3.2) several workpieces can be formed at the same time (multi-forming) with adequate die geometries. Figure 2.32 presents examples of micro parts generated by micro rotary swaging.

Important factors for generating good parts in micro rotary swaging are a deep understanding of the flow behavior of dissimilar (a) or similar (b) materials; the control of the relative motion between the workpiece and the dies (b), (c); the control of the positioning in process and the tool design (e). The part (a) in Fig. 2.32 shows a composite component made of two different materials (copper in the core, and AISI 304 steel as shell) by infeed rotary swaging [Kuh10]. The softer material in the core guarantees a tight connection of the two components [Kuh12a].

Fig. 2.32 Parts made by micro rotary swaging: **a** composite component consisting of a core from Cu-ETP and a shell from AISI 304 steel; **b**, **c** triangular cross-sections from AISI 304 and Cu-ETP; **d** axially joined parts by forming, **e** multi forming of notches

Part (d) was made by joining two rods [Mou14c] in plunge rotary swaging, using a tube as the connecting component. In both cases (a) and (d), the connection between the joining partners can be a form and a force fit. The correct relative rotational motion between the workpiece and the dies not only allows further material modifications (Sect. 2.4) but also leads to more possible geometry variations. For (b) and (c) this relative rotation was kept to zero and the calibration zones of the dies were flat. Using a mandrel with tubes, polygons like (c) can be manufactured. In (e) nine notches (depth 0.25 mm) manufactured in three steps are presented. Because the notches are small the feed rates remain the same for one or more elements when manufactured at the same time, but the productivity is increased with regard to the number of geometries designed in the die.

By the control of the material flow in micro rotary swaging with different measures, the process can be improved in the sense of productivity, the accuracy of the final product or even material modifications. Using a spring-loaded clamping device, the axial material flow in infeed rotary swaging can be controlled and thus enables about four times higher axial feed rates. With intermediate elements around the workpiece in plunge rotary swaging, an undesired radial material flow into the gaps between the dies can be prevented and, as a result, the radial feed rate can be significantly increased. Furthermore, external forces applied on one or both ends of the workpiece allow the material shift to be guided, thus the final geometry of the product can be influenced.

In infeed rotary swaging, the final diameter can be constant or have a profile over the workpiece length, depending on the feed rate used. For low feed rates, the generated diameter is closer to the nominal diameter of the dies d_{nom} and is constant along the workpiece length, besides the tip. At high feed rates, an inhomogeneity of the diameter over the total part length is produced. The diameter increases with the feed rate and from the tip to the end of the deformed part. The diameter increase can be explained by the machine resilience, which leads to an incomplete closing of the dies during the forming with high feed rates as in the idle state. However, the rising value of the final diameter over the length of the workpiece can be overcome by using another machine with less resilience or by controlling the position of the dies by adding a radial feed during the infeed process.

In plunge micro rotary swaging, intermediate elements around the workpiece enable up to three times higher radial feed rates v_r compared to forming without these elements. This is because flashes on the workpiece can be prevented. However, the final diameter increases slightly, which depends on the stiffness of the elastic intermediate element, with bigger diameters for harder elements. This fact has to be considered during the die design.

A further approach to control the material flow during plunge rotary swaging involves the external forces at the workpiece ends. The material flow reacts very sensitively to these. Low forces at one side can direct the material flow so that it occurs in a preferred direction. A symmetrical material flow is possible if equal external axial forces are applied on both sides of the forming zone. Thus, the final shape of the workpiece can be controlled.

For the process and especially for the material flow in micro rotary swaging, specific micro effects have to be considered. The first particularity are the slim workpieces, which tend to bend, break or even get into the die gaps, so a guiding element needs to be applied during forming. Furthermore, the surface-to-volume ratio is much higher and for that reason the friction condition has a greater effect on the process. The friction influences, for example, the plastic strain in a thicker area from the surface of the workpiece. In addition, the feasible axial feed rates reach such high values that the feed length during one stroke exceeds the workpiece's initial diameter. For this reason, the radial forces are high and the outer ring of the swaging head expands, which leads to an inhomogeneous diameter evolution. The previous results present the potential of rotary swaging for micro manufacturing.

Latin

Variable	Unit	Explanation
C	(-)	Material constant
d_0	mm	Initial diameter
d_1	mm	Final diameter
d_{nom}	mm	Diameter of the forming zone of a closed die set
f_{st}	s^{-1}	Stroke frequency
F_a	N	External axial forces
$F_{\text{b,crit}}$	N	Critical bending load
h_T	mm	Stroke height
h_{st}	mm	Radial die feed per stroke
l_0	mm	Initial length of the deformed workpiece part
L_0	mm	Initial length of the workpiece
l_1	mm	Final length of the deformed section
L_1	mm	Final length of the workpiece
l_{cal}	mm	Length of the calibration zone of the die
l_{form}	mm	Length of the forming zone of the die for plunge swaging
l_{st}	mm	Axial workpiece feed per stroke
n	(-)	Hardening behavior
v_f	mm/s	Workpiece feed rate
v_r	mm/s	Die feed rate
V_{st}	mm^{-3}	Volume displaced by the dies per stroke

Greek

α	$^{\circ}$	Tool angle
α_M	%	Volume fraction of martensite
Δd	mm	Diameter difference
φ_{st}	(-)	Incremental deformation degree
μ	(-)	Friction coefficient

2.4 Conditioning of Part Properties

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Abstract Micro rotary swaging is an incremental cold forming process that changes the geometry and the microstructure of the swaged parts. Due to the opportunity to produce parts with an accurate diameter and to influence the surface and the mechanical characteristics by infeed rotary swaging, it is possible to prepare micro parts for further forming processes such as extrusion. This targeted conditioning was further enhanced by modifications of rotary swaging tools and kinematics. The formability of the modified workpieces was reflected by the required forming force of the subsequent extrusion process, where not only a force increase but also a force reduction was observed with modifications in rotary swaging. In this section a process chain “rotary swaging – extrusion” for austenitic stainless steel AISI304 is shown.

Keywords Incremental forming · Microstructure · Process modifications

2.4.1 Introduction

Cold forming of complex workpieces is usually accompanied by work hardening and is usually realized by multi-stage forming processes. In addition, difficult to form materials have to be annealed between the forming steps due to the strain hardening. This heat treatment contributes to a substantial rise of costs for equipment and energy supply. One possibility to avoid this intermediate heat treatment and to improve the formability of the workpieces is a targeted conditioning of the workpieces for further forming. This conditioning includes adjusting the forming characteristics of the workpiece, the geometry (diameter) and the surface (roughness, lubrication pockets). Very good formability with such characteristics as good strength in combination with a sufficient ductility provide ultrafine-grained (UFG) materials. Refining of materials can be generated by applying severe plastic shear deformation, e.g. by pressing the workpiece into a die with an angular channel [Val00]. Further improvement of the forming process can be achieved by reducing the friction between the workpiece and the forming die. This can be realized, for example, by modifying the profile and the surface topology of the blanks [Ish17a].

Micro infeed rotary swaging not only changes the geometry and the surface of the swaged parts, but also influences the microstructure and, thus, the mechanical properties of swaged workpieces. Due to the special adjustments of the machine, it was possible to vary the produced diameter [Kuh13] and the roughness of the swaged parts [Ish15c]. Mouri et al. investigated the change of the microstructure after rotary swaging depending on the feed velocity as well as the increase or decrease of the martensite content with the variation of the forming temperature [Mou15a]. They deduced that rotary swaging could provide the opportunity to adjust the forming characteristics of the workpieces for further forming steps [Ish15a]. However, the process was to be enhanced in terms of tool geometry and process kinematics.

2.4.2 Process Chain “Rotary Swaging—Extrusion”

2.4.2.1 Modifications of the Die Geometry

The usage of curve-shaped dies (CSD) is typical of rotary swaging of circular tubes and rods (Fig. 2.33a). These dies feature a curved surface in the reduction zone, as well as in the calibration zone, where the diameter of the part is defined. However, the simulation analysis with FEM software showed that the shear strain distribution depended on the geometry of the dies [Ish15a] (see Fig. 2.34). This planar model represented the reduction of the workpiece cross-section at the forging zone (the part of the reduction zone where forming occurred) after the first revolution of the dies and at the end of the reduction.

An increase of the shear strain (PE12) was observed in the simulations with flat shaped dies (FSD) compared to the curve-shaped dies. These dies featured a curved surface in the reduction zone and a flat surface in the calibration zone (Fig. 2.33b). Further modifications of the die surface in the calibration zone were introduced with double-flat-shaped dies (DFSD), which featured a flat surface in both the reduction and the calibration zone (Fig. 2.33c).

Fig. 2.33 Evolution of the swaging dies from curve-shaped dies (CSD) to double-flat-shaped dies (DFSD) to increase the shear strain in the formed workpieces

Fig. 2.34 Distribution of the shear strain PE12 at the end of the reduction zone

Fig. 2.35 Eccentric rotary swaging: **a** principal shift of center line; **b** eccentric dies; closed double-flat-shaped dies for **c** centric rotary swaging; **d** eccentric rotary swaging

2.4.2.2 Modifications of Process Kinematics

Eccentric Rotary Swaging

Further increase of the shear strain in the workpiece was realized by eccentric rotary swaging. For this aim, new swaging dies were designed (see Fig. 2.35). The aim of these dies was to shift the center line with every stroke of the tools during their rotation (Fig. 2.35a) [Toe18].

Control of the Stroke-Following Angle

Angular driving of the part allowed control of the stroke-following angle $\Delta\phi$, which was defined as the difference between the rotation angle of the workpiece and the rotation angle of the dies during two consecutive strokes. When the angular velocity

Fig. 2.36 Variation of the stroke-following angle $\Delta\phi$: **a** without; **b** positive; **c** and **d** negative

of the tools was kept constant at $\omega_{Die} = 89.23 \text{ rad/s}$ and the part was not rotating ($\omega_{Wp} = 0 \text{ rad/s}$), the stroke-following angle $\Delta\phi$ between two successive strokes (stroke frequency $f_{st} = 102 \text{ s}^{-1}$) resulted in $\Delta\phi = 51^\circ$ (see Eq. 2.8):

$$\Delta\phi = \frac{360^\circ * (\omega_{Die} - \omega_{Wp})}{2\pi * f_{st}} \quad (2.8)$$

In the case of the same rotation speed of the workpiece and the flat-shaped dies, the stroke-following angle $\Delta\phi$ equals zero and the dies impact the workpiece at all times at the same circumferential line (Fig. 2.36a). The result of this forming process is a triangle. In other cases, the workpiece can rotate either in the same direction or opposite to the dies rotation. When they rotate in the same direction, a positive stroke-following angle $\Delta\phi$ (Fig. 2.36b) is achieved when the workpiece rotates faster than the dies. If the workpiece rotates more slowly, the stroke-following angle $\Delta\phi$ is negative (Fig. 2.36c). If the workpiece rotates in the other direction (Fig. 2.36d), the stroke-following angle $\Delta\phi$ remains negative as well.

2.4.2.3 Extrusion

Ten samples were examined in each series of experiments. For a comparison of the forming characteristics, the samples “NS” (not swaged) in the initial stage were turned from non-swaged parts. The diameter of the extrusion bore was $d_{1m} = 1.30 \text{ mm}$ (Fig. 2.41a). The diameter of the lower bore was $d_{2m} = 1.00 \text{ mm}$. All parts were radially preloaded in a metal sleeve. Forming was preceded with a manual pre-lubrication of the extrusion die. The press force was obtained by a piezo-based force sensor to characterize the formability of the swaged parts (Fig. 2.37b).

In order to regard the polygonal geometry and small variations in the diameter of the samples d_1 , the forming force F was related to the cross-sectional area A of the respective swaged workpieces. All curves had a similar gradient angle in the elastic range (at the beginning of the process) and differed during the plastic deformation.

2.4.2.4 Experimental Design

The forming parameters for all experiment series are summarized in Table 2.2.

Fig. 2.37 Forward extrusion process: **a** schema; **b** characterization curve

Table 2.2 Forming parameters: rotary swaging

Test number	Die	Feed velocity v_f , mm/s	Stroke- following angle $\Delta\phi$	Shift of the center line S_d , mm
I	CSD	1.0	51°	0
I-a		2.0		
II	FSD	1.0		
II-a		2.0		
III	DFSD	1.0		
III-a		0.5	60°	
III-b			51°	
III-c			40°	
III-d			30°	
IV	EFSD	1.0	51°	0.3
IV-a		2.0		

For the extrusion, all the swaged parts with the diameter $d_1 = 1.28 \pm 0.01$ mm were cut into small parts with a length of $l_1 = 3.0$ mm. The samples were pressed with a constant pressing velocity of $v = 0.1$ mm/s.

2.4.3 Results and Discussion

The analysis of the characterization curves reflected the possibility of conditioning of part properties by means of modifications in rotary swaging. The influence of the modifications in the swaging die design on the extrusion force is shown in Fig. 2.38. The stroke s of the pressing punch varied with the actual initial length of the samples. Curve NS represents the unswaged material during extrusion. The analysis revealed that rotary swaging with curve shaped dies increased the required extrusion force (curve I above curve NS). In contrast, using flat-shaped and double-flat-shaped dies decreased the required forces significantly (curves II and III

Fig. 2.38 Characterization curves based on tool design. I: curve-shaped dies (CSD), II: flat-shaped dies (FSD), III: double-flat-shaped dies (DFSD), NS: not swaged

are below curve NS). This difference can be explained by various aspects, for example a change of the microstructure, a reduction of the grain size or martensite and carbide formation [Ish17b].

The microstructures are shown in Fig. 2.39. Independent of the die design, a clear grain refinement in the radial direction resulted from the swaging process. However,, the grain boundaries after swaging with curve-shaped dies (Fig. 2.39b) were more easily detectable than after swaging with flat- or double-flat-shaped dies (see Fig. 2.39c and d).

Another aspect is the roughness and the topology of the surface. The surface roughness of the initial stage samples revealed a value of $S_a = 0.92 \pm 0.16 \mu\text{m}$. The geometry (cross-section) of swaged stainless steel (alloy AISI304) specimens using the curve-shaped dies as well as the flat- or double-flat-shaped dies featured a circular geometry. However, the value of surface roughness S_a of the latter two grew to $S_a = 1.50 \pm 0.35 \mu\text{m}$ and $S_a = 1.11 \pm 0.61 \mu\text{m}$ compared to the conventional swaged workpieces with $S_a = 0.41 \pm 0.05 \mu\text{m}$ (settings II, III and I). Hence, the workpieces swaged with curve-shaped dies tended to provide smoother surfaces, but the flat- or double-flat-shaped dies promoted pocket formation on the sample surface. Consequently, different die designs, curved or flat, led to significantly different forming properties [Ish18].

Although flat-shaped dies allow a significant reduction of the yield stress [Ish17b, Ish15a] and work hardening, an increase of feed velocity from $v_f = 1 \text{ mm/s}$ (settings I and II) to $v_f = 2 \text{ mm/s}$ (settings I-a and II-a) decreased this improvement. The required extrusion force increased when forming with flat-shaped dies (curve II-a higher than curve II) (Fig. 2.40), while the feed velocity did not affect the extrusion force when using curve-shaped dies (curve I equals curve I-a).

The roughness of the workpieces swaged with flat-shaped dies was influenced by the feed velocity v_f as well (decreased to $S_a = 0.94 \pm 0.23 \mu\text{m}$), while swaging

Fig. 2.39 Microstructure (cross-section) of the workpieces **a** before forming; and after rotary swaging at the setting parameters **b** I, **c** II and **d** III

Fig. 2.40 Characterization curves based on feed velocity v_f (I and I-a: curve-shaped dies (CSD), II and II-a: flat-shaped dies (FSD))

with curve-shaped dies at a higher feed velocity $v_f = 2 \text{ mm/s}$ changed this insignificantly ($S_a = 0.58 \pm 0.18 \text{ } \mu\text{m}$) (Fig. 2.41).

These results correspond to the martensite content, which was more influenced by the feed velocity if flat-shaped dies were applied [Ish17b]. Additionally, the number of strokes was higher with lower feed velocity. The number of strokes influenced the cross-sectional shape more when using flat-shaped dies, due to the pockets on the surface (see Fig. 2.45). The microstructure was refined more by the higher degree of shear strain [Ish15a].

Another major influence on the extrusion characteristics was the modification of the process kinematics. The use of the eccentric flat-shaped dies changed the process kinematics by means of radial displacement of the center-line during the process and thus resulted in a higher shear strain. Due to a significant reduction of the grain size and the martensite content compared to the rotary swaging using

Fig. 2.41 Characterization curves based on process kinematics: eccentric flat-shaped dies (EFSD, setting IV) compared with double-flat-shaped dies (DFSD, setting III)

double-flat-shaped dies [Ish17b], a reduction of the required press force during extrusion could be observed (Fig. 2.41, curve IV below curve III).

Eccentric rotary swaging influenced also the development of the microstructure. As a result, the transformed martensite showed a strong reduction compared to the amount of martensite after conventional rotary swaging. Thus, pre-forming of the workpieces in this special way affected work softening, reduction of yield stress, and increase of the plastic strain [Ish17b]. The grain boundaries were detectable in the core area of the processed workpieces while they were not visible any more in the outer regions (Fig. 2.42b). Furthermore, the specimens exhibited typical eddy patterns and a spiral grain orientation, which were more pronounced with a feed velocity of $v_f = 2$ mm/s (setting IV-a) (see Fig. 2.42c). Moreover, the shape of these workpieces was a polygon with eight corners. Using this method, the surface roughness was also influenced by the feed velocity. The workpieces swaged at a feed velocity of $v_f = 1$ mm/s (setting IV) featured a roughness of

Fig. 2.42 Microstructure (cross-section) of the workpieces **a** before forming; and after eccentric rotary swaging at feed velocity of **b** $v_f = 1$ mm/s (setting IV); **c** $v_f = 2$ mm/s (setting IV)

Fig. 2.43 Characterization curves based on process kinematics: stroke-following angle $\Delta\phi$ using double-flat-shaped dies (settings III-a to III-d)

$S_a = 1.09 \pm 0.26 \mu\text{m}$, which rose at the increased feed velocity of $v_f = 2 \text{ mm/s}$ (setting IV-a) to a value of $S_a = 1.36 \pm 0.48 \mu\text{m}$.

A variation of the stroke-following angle $\Delta\phi$ allowed the production of parts with polygonal geometries. The shape of the conventional swaged workpieces using curve-shaped dies was actually circular. The usage of flat-shaped dies or double-flat-shaped dies in combination with the targeted adjustment of the stroke-following angle $\Delta\phi$ delivered polygonally shaped parts with several facets. The shaping correlates to Eq. 2.9, where n is the number of corners:

$$n = 360^\circ / \Delta\phi \quad (2.9)$$

With the increasing number of facets the characterization curves became flatter (Fig. 2.43).

In dependence on the stroke-following angle $\Delta\phi$, not only the microstructure but also the surface roughness was influenced. A stroke-following angle $\Delta\phi = 60^\circ$ (setting III-a) delivered a hexagon (see Fig. 2.44a), with a surface roughness of $S_a = 0.45 \pm 0.16 \mu\text{m}$. The round shaped workpieces formed by $\Delta\phi = 51^\circ$ (setting III-b) featured the highest surface roughness value for these experiments with $S_a = 0.72 \pm 0.26 \mu\text{m}$. Workpieces swaged by $\Delta\phi = 40^\circ$ (setting III-c) developed the shape of a nonagon (see Fig. 2.44c), and a surface roughness of $S_a = 0.66 \pm 0.36 \mu\text{m}$. All dodecagons were swaged by $\Delta\phi = 30^\circ$ (setting III-d) (see Fig. 2.44d), and had a roughness of $S_a = 0.53 \pm 0.17 \mu\text{m}$.

The workpieces swaged with curve-shaped dies revealed a circular cross-section and a smooth surface (Fig. 2.45a). Swaging with the stroke-following angle of $\Delta\phi = 51^\circ$ (non-rotating workpiece, setting III-b) using double-flat-shaped dies led also to a round geometry (Fig. 2.44b), but many small bevels (see Fig. 2.45b),

Fig. 2.44 The influence of the stroke-following angle on the geometry and the microstructure (cross-section) of the swaged workpieces using double-flat-shaped dies at feed velocity of $v_f = 0.5 \text{ mm/s}$ (settings III-a to III-d)

Fig. 2.45 Influence of the swaging dies on the creation of lubrication pockets by non- rotating workpiece $\Delta\phi = 51^\circ$

which are dependent on the feed velocity v_f . The swaging dies did not strike along the same line of the workpiece any more and the resulting shape formed was not a polygon. The surface roughness increased to the value of $S_a = 0.72 \pm 0.26 \text{ } \mu\text{m}$.

2.5 Influence of Tool Geometry on Process Stability in Micro Metal Forming

Lewin Rathmann*, Lukas Heinrich and Frank Vollertsen

Abstract Deep drawing is a well-suited technology for the production of hollow metallic micro parts due to its excellent qualities for mass production. However, the downscaling of the forming process leads to new challenges in tooling and process design, such as high relative deviation of the tool geometry and increased friction. In order to overcome these challenges and use micro deep drawing processes in an efficient production system, a deeper understanding of the micro deep drawing process is necessary. In this investigation, an overview of the substantial influences on process stability in micro deep drawing and a method for description, called “tolerance engineering”, are presented.

Keywords Micro forming · Deep drawing · Tool geometry

2.5.1 Introduction

Deep drawing is a well-suited technology to produce such parts due to its excellent qualities for mass production. However, the downscaling of the forming process leads to new challenges in tooling and process design. Cheng et al. investigated how the interaction of free surface roughening and the size effect affect the micro-scale forming limit curve [Che17]. They modified the original Marciniak-Kuczynski model by introducing free surface roughening to describe the decrease of the micro-scale forming limit curve as a result of the geometry and grain size effects.

Saotome et al. showed that the process window decreases with increasing ratio between the punch diameter and sheet thickness when thin foils are used [Sao90]. In preparation for the deep drawing process, Gau et al. expanded the process window for forming micro cups with cup height to outer diameter ratios by annealing stainless steel 304 sheets at temperatures not less than 900 °C for more than 3 min [Gau13]. In 2014, a novel technique in micro sheet metal forming was presented by Irthiea et al. [Irt14]. They used a flexible die to produce micro metal cups from stainless steel 304 foils. The die consisted of a cylindrical tank filled with a rubber pad. Forming parameters like anisotropy of SS 304 material and friction conditions at various contact interfaces were investigated experimentally as well as in simulation with this setup.

Maessner et al. demonstrated that increasing punch forces can be expected in the micro range due to increasing friction [Mes94]. Furthermore, Michel et al. showed that the yield point and stress decrease with decreasing foil thickness [Mic03]. Besides material and friction, the geometry of the forming tools has a significant impact on the process forces, stress states and the process window of the deep drawing process.

Fig. 2.46 Relative deviations in manufacturing of a forming tool due to identical absolute deviations in macro and micro range

Aminzahed et al. present in [Ami17] a piezoelectric actuator as a new approach in a deep drawing operation to draw rectangular foils. They also studied blank holder effects on thickness distributions, punch force and spring-back and used experiments and simulations to obtain results.

Wagner et al. demonstrated that the punch radius has a significant influence on the process force and that higher punch radii are beneficial for the process [Wag05]. Thus, the quality of the produced parts depends on the choice of suitable geometric parameters of the forming tools. In conventional deep drawing processes, manufacturing deviation in the sub-millimetre range in tooling do not affect the deep drawing process since these deviations are neglectable in size compared to the dimensions of the tools. Moreover, small deviations are compensated by the formability of the workpiece material. Due to size effects, the tribology changes in the micro range, which leads to smaller process windows in micro deep drawing [Vol09]. If scaled down, the deep drawing process becomes more sensitive to process deviations. This is due to the relative deviations of the tool geometry, caused in tool manufacture. These increase with decreasing size in the micro range because the accuracy of manufacturing reaches its limits [Hu09]. Figure 2.46 illustrates the effect of larger relative manufacturing deviations in the micro range. Due to these effects, micro forming processes cannot be simply transferred to the micro range and further investigation is needed. Therefore, the aim of this study is to determine the influence of tool geometry on micro forming processes to allow a specific process design and improved process stability as well as a quantitative assessment of the effect of wear- and production-related deviations of tool geometry.

2.5.2 Experimental Setup

For the deep drawing experiments described in this investigation, blanks were cut out using a picosecond pulsed laser with a wavelength of 1030 nm in order to

prevent burr formation at the edge of the blank. The diameters were checked with a Keyence VHX 1000 digital microscope. For the tools, ledeburitic powder-metallurgical steel 1.2379 (X153CrMoV12) was used. The relevant geometric parameters of the tools used in the experiment were measured using a laser scanning microscope Keyence VK 9700. The drawing process was carried out on a single-axis micro forming press with a maximum punch force of 500 N and a constant punch velocity of 10 mm/s using HBO 947/11 as lubricant. The punch force was measured with a Kistler 9217A piezo load cell with an accuracy of 0.01 N. The punch stroke was measured with a Heidenhain LS477 linear scale with an accuracy of 1 μm . The press was driven by a servo motor controlled by a NI 9514 servo drive interface. The blank holder acts passively. It uses its own weight and is supported by two springs. The blank holder pressure can be adjusted by changing the spring tension. The blank positioning was realized by an automated positioning system. The blank was positioned with a pneumatic gripper that is driven by a linear direct drive cross table. The position of the blank was then measured using an Allied Vision G 917 B monochrome CCD camera with a resolution of 9 MP equipped with a telecentric lens with built-in coaxial illumination and a magnification of 0.75. With this setup the blanks can be positioned within a radius of 10 μm from the center of the drawing die.

2.5.3 Numerical Models

The Finite Element Method (FEM) is a proven tool for routine calculation tasks and is used in plant and mechanical engineering as well as in vehicle construction. This method enables largely realistic statements in the stages of concept finding and the development of components and structures using computer-assisted simulations of physical properties. This results in a significant reduction of product development time [Kle15]. For FEM analysis, the software Abaqus 6.14 was used. A 3-dimensional model for micro deep drawing was used. All tools are defined as analytical rigid shell objects and the blank was defined as a deformable body using the 8-node linear brick 3D-stress element C3D8R for the mesh. Within the sheet thickness four elements were used. In order to save computational time, only one half of the blank was considered. For the elastic plastic material model of the foil, tensile tests were performed to determine the required flow stress curves.

2.5.4 Circular Deep Drawing

The punch force presents an important parameter to assess the process stability of deep drawing processes. In [Beh13] the influence of variation of the tool geometry on the punch force is given. By using experimental data and FEM simulation, each geometry variation is classified regarding its impact on the punch force and therefore on the process stability, as shown in Fig. 2.47.

Fig. 2.47 Influence of tool geometry variation on the maximum punch force in circular micro deep drawing

Figure 2.47 shows that the change of punch diameter and drawing gap resulted in the greatest impact on the punch force and therefore should be carefully controlled during tool manufacturing. Changes in the punch radius, drawing die radius and edge shape, on the other hand, proved to be of minor importance.

The limiting drawing ratio LDR describes the maximum value of the drawing ratio that can be achieved under the given process conditions. A larger achievable LDR acts as an indicator of a more stable deep drawing process and therefore makes it attractive for industrial use. In [Beh16] the influence of the blank material and tool geometry variation on the LDR is described. It is shown that, regardless of the tool geometry, the austenitic steel 1.4301 enables the largest process window compared to the foil materials Al99.5 and E-Cu58, and therefore proves to be most stable in micro deep drawing processes, as shown in Fig. 2.48.

For all the materials used, the die clearance and die radius have the biggest influence on the LDR. While the die radius should be increased, a decrease of the drawing clearance below 1.25 times the foil thickness is beneficial to the drawing process.

The investigation of the influence of a tool geometry deviation on the part geometry is mentioned in [Hei17]. In this case, the influence of an uneven drawing radius deviation was investigated for different drawing ratios using experimental data and FEM simulation. The deviation of the die radius is described by the ratio of curvature (ROC), which is defined as the ratio between the maximum and minimum curvature of the die edge. This sort of variation has a significant influence on the part shape, resulting in uneven cup height after the drawing process due to stronger stretching of the cup wall at positions with a smaller die radius. This effect increases with higher drawing ratios. Furthermore, it is shown that by using FEM simulation an initial blank displacement sufficient to result in an even cup height can be found.

Fig. 2.48 Process window for different materials in micro deep drawing

However, the effect of an uneven drawing radius cannot be compensated entirely. While an appropriate blank displacement results in an even cup height when an uneven drawing radius is used, a significant difference in strain remains in the part, as shown in Fig. 2.49. This is explained by the different mechanisms that result in an uneven cup geometry. While blank displacement leads to material surplus on one side of the cup, an unsymmetrical radius deviation on the other hand generates uneven stretching of the cup wall. It can be concluded that the effect of an uneven radius geometry proves to be of major importance to produce accurately shaped micro cups.

The investigation of the interactions between individual sub-processes in multi-staged micro deep drawing and their impact on the overall process is time-consuming and cost-intensive when done only experimentally. Thus, a simulation model of a two-stage micro deep drawing process is developed in [Tet15] and showed that this model can display the first and second stroke in one forming simulation in accordance with the experiments conducted. Figure 2.50 shows the overview of the two-stage micro deep drawing process simulation model. It consists of two pairs of dies and punches with different diameters and a blank holder impinging the blank with pressure. After the first stroke, the second punch clamps the cup on the radius of the second die while the first punch and die move backwards. Then, the second punch with a smaller diameter starts moving, which draws the first cup into the second die, which results in a second cup with a smaller diameter and higher cup walls.

This model is the basis for further investigations with two-stage simulation models with varied tool geometries. This enables an identification of the influence

Fig. 2.50 Simulation model of a two-stage micro deep drawing process

The first characteristic is the curvature γ along the cup wall. The curvature is a result that can be found in both the simulation and experiment and results from an oversized drawing gap [Klo06]. It describes lip forming and bulge at the rib of a cup, which are defects along the cup wall and thus criteria for whether a cup is usable or not. This curvature is the maximum distance between a tangent and a point A. Both are located at the outer cup wall. The tangent goes through two points that mark the two local maximum peaks given by the lip forming and the rib of the cup wall. The point A is between these two peaks and marks the local minimum of the cup's wall topography. Figure 2.51 gives a pictorial representation of the curvature.

The influence of the adjustment of the first and second die diameters on the curvature γ is shown in Fig. 2.52. The abscissa labels the die diameter deviation from the ideal geometry for a minimum (–) and maximum value (+), while the ordinate displays the curvature in μm . Furthermore, the main influence of the adjustment of D_2 on γ is represented by a black dotted line. The main influence of D_2 means that the influence of the adjustment of D_1 on γ is not considered. Two courses happen and show that a large die diameter in the first stroke and a large one in the second stroke reduce the curvature. For an explanation, it is important to differentiate between the outer and inner cup wall. A close look at these two sides reveals that the second stroke relocates the curvature from the outer to the inner wall. Responsible for this is the second die radius, which shifts the curvature to the inner wall by straightening the outer. The greater the curvature is after the first stroke, the better this mechanism works. If the curvature is low after the first stroke,

Fig. 2.51 Pictorial representation of the curvature γ at the cup wall due to a large drawing gap

this mechanism does not occur and the measured curvature after the second stroke gets worse.

Another deep drawing quality feature is a homogeneous wall thickness over the entire cup wall. As a measure for this, the mean wall thickness alteration Δ is used. The development is shown in dependency on the adjustment of die diameter D_1 in Fig. 2.53. As in Fig. 2.52, the abscissa labels the die diameter deviation from the ideal geometry for a minimum (–) and maximum value (+), while the ordinate displays the mean wall thickness alteration in μm . Furthermore, the main influence of the adjustment of D_2 on Δ is represented by a black dotted line. It can be seen that the mean wall thickness alteration is held almost constant by choosing a small diameter D_1 in the first deep drawing step and a small D_2 in the second one. Choosing a small die diameter leads to small drawing gaps in both deep drawing steps. The small drawing gap effects a levelling on deviations of the large wall thickness. Especially in the area of the punch radius at the cup wall, a thinner wall can be found. Otherwise, the wall thickness increases at the end of the rib due to the missing influence of the blank holder when the blank emerges under it. Between those zones, the cup wall thickness is in the range of the initial blank thickness. If this maximum deviation is reduced by applying a large die diameter in the second stroke, the smaller drawing gap reduces the mean wall thickness alteration and

Fig. 2.52 Influence of the adjustment of die diameter D_2 in second stroke on curvature γ with varied adjustments of die diameter D_1 in first stroke

improves the homogeneity of the wall thickness distribution. Nevertheless, a small thickness deviation remains due to the drawing gap being larger than the blank thickness, leaving enough space for minimum alterations to occur.

The punch force is one of the most important considerations in metal forming. This force correlates with the tool wear, surface quality and the parameters of the conferred forces [Beh13]. Therefore, the influence of the adjustment of die diameter D_2 on the maximum punch force $F_{p,max}$ is shown in Fig. 2.54. As in Fig. 2.52 the abscissa labels the die diameter deviation from the ideal geometry for a minimum (-) and maximum value (+), while the ordinate displays the maximum punch force in N. Furthermore, the main influence of the adjustment of D_2 on $F_{p,max}$ is represented by a black dotted line. The diagram shows that a large diameter D_1 in the first stroke as well as a large diameter D_2 in the second stroke lead to stronger decreasing maximum punch forces than choosing a small D_1 and a large D_2 . The decrease in the force is due to the reduced contact between the tools and workpiece. This contact does not only appear among the radii of the die and punch and the blank, but it also counts for the inner walls of the die and the blank. Contact connected with relative movement between a contact pair is always accompanied by friction. In the micro range, the punch force is affected by friction between the tool and workpiece [Beh13]. Therefore, a large diameter D_2 decreases the punch force, because it leaves enough space for the blank to avoid contact with the inner die walls. Additionally, a larger diameter D_2 reduces the contact pressure between the blank and die radius. This pressure arises from the contact conditions between the die radius and blank, ending in greater friction. And finally, a lower drawing ratio decreases the punch force. The maximum punch forces for the second stroke in dependency on D_1 differ strongly, because of the remaining stresses in the cup

Fig. 2.53 Influence of the adjustment of die diameter D_2 in second stroke on mean wall thickness alteration Δ with varied adjustments of die diameter D_1 in first stroke

Fig. 2.54 Influence of the adjustment of die diameter D_2 in second stroke on maximum punch force $F_{p,max}$ with varied adjustments of die diameter D_1 in first stroke

wall. A smaller D_1 causes greater stresses than a larger D_1 . The punch must additionally overcome more stress during the second stroke. This increases the maximum punch force.

2.5.5 Deep Drawing of Rectangular Parts

Compared to deep drawing of circular parts, there is a significant increase in complexity when rectangular parts are formed. As additional steps such as handling, or trimming become increasingly difficult when scaling processes to the micro range, it was the aim to produce accurately shaped parts only through deep drawing. A commonly used method to achieve near net shaped parts in deep drawing is blank shape optimization. However, due to size effects this method cannot be transferred unadjusted for the optimization of micro deep drawing processes. For example, there is a significant increase in friction with decreasing process dimensions. This effect has a fundamental influence on the shape of the final deep drawn part. In [Hu11] it is shown that, if the material model and friction coefficient are adjusted to the actual condition in the micro range, it is possible to determine the optimal blank shape in a size-dependent FEM simulation and accordingly produce near net shaped parts in rectangular micro deep drawing, as shown in Fig. 2.55.

Furthermore, the work carried out showed that material characteristics are even more important than friction and that the results of the FEM simulation are in good

Fig. 2.55 Optimization of blank shape in FEM (a) and (b) and experiment (c) for deep drawing of rectangular blanks

agreement with experimental findings. Using Al99.5, rectangular parts with no remaining flange can be formed. In [Hu11] these findings can be transferred to blanks made of E-Cu58 and 1.4301. The influence of tool geometry deviation on the punch force in rectangular micro deep drawing has been investigated in Behrens et al. [Beh15]. It is demonstrated that increasing the corner radius or decreasing the die radius increases the resulting punch force and therefore has a negative effect on the drawing process. Additionally, the results show that the influence of a die radius variation on the punch force is far more prominent in rectangular deep drawing compared to the forming of circular parts, which can be explained by the different composition of the punch force.

2.5.6 Forming Limit

In order to enable the possibility of predicting component failure in deep drawing like bottom fracture using FEM simulation in addition to experimental investigations, it is necessary to integrate the material failure as an input variable into the simulation. Conventional methods like hydraulic or pneumatic bulge tests are available to determine forming limit curves even for thin foil materials. With these methods, only positive minor strains are measurable. Therefore, a scaled-down Nakajima test was developed and described in Veenaas et al. [Vee15]. Using an evaluation method adapted to the micro range, complete forming limit curves (FLC) can be determined. Especially for deep drawing processes, negative minor strains and the left side of a forming limit diagram are more important [Vee15]. It was possible to determine forming limit strain curves for the micro range for the foil materials Al 99.5, E-Cu58, 1.4301 and for PVD-sputtered Al-Zr, both for the positive and for the negative side of minor strain, as shown in Fig. 2.56.

2.5.7 Change of Scatter

Tolerance engineering is an approach for assessing influences in multi-stage processes but is also applicable in a single-stage process. The aim is the possibility of extrapolating the permissible tool geometry deviations in every process step from geometrical requirements on the final product geometry prescribed by operators. This becomes necessary because each process step increases or decreases the actual deviation of the workpiece. In order to describe this change of deviation, so-called “tolerance functions” are formulated. These functions answer the question, what is the necessary tolerance or admissible wear to get sound parts. For a process chain, the outcome can be calculated by a series of tolerance functions.

An example of data useful for tolerance engineering is given in Brüning [Brü15c] and is described by using upsetting of preforms with cone shaped cavities. In this example, four different dies are compared regarding their forming results.

Fig. 2.56 Forming limit diagram and the lower forming limit curves for different foil materials

These results depend on the radii of the dies and preforms after upsetting as well as on the relative preform volumes. It is shown that the preforms generated by the laser rod end melting process can be calibrated well within a single-stage cold forming operation. Furthermore, for average natural strains $\phi^* \leq 0.7$, the tolerable eccentricity of the preform is determined to $25 \mu\text{m}$ and increases with decreasing relative volume of the preform. This volume can scatter within 3% without a negative influence on the filling of the cavity. The influence of the calibration step on the eccentricity of the preform after the laser rod end melting process for one die is shown in Fig. 2.57.

The figure shows that the scattering field of the deviations regarding the position in the z - and r -direction is subjected to a shift. This shift is in the direction of the origin. Additionally, the scattering field of deviations after the calibration step is much smaller than the one after the laser rod end melting process. As Fig. 2.57 shows, it is possible to improve the scattering field and the position of its centroid by decreasing the field's width and length and relocating the center point in the direction of the origin.

According to tolerance engineering, it is now important to know the allowed deviation of the laser rod end melting process so that the subsequent process step is able to calibrate those deviations and create a sound part. Therefore, matrices are used to transform the deviations from the cold forging process into the allowed deviations of the previous step by multiplying them with damping factors and subtracting the shift of the mean deviations of both processes. An exemplary equation is shown in the following Fig. 2.58.

Fig. 2.57 Influence of a calibration step on the eccentricity of the preform after the laser rod end melting process

inverse calculation for i+1th step:

$$\left[\begin{pmatrix} r_i \\ z_i \end{pmatrix} + \begin{pmatrix} \pm \Delta r_i / 2 \\ \pm \Delta z_i / 2 \end{pmatrix} \right]^T = \left[\begin{pmatrix} r_{i+1} \\ z_{i+1} \end{pmatrix} + \begin{pmatrix} \pm \Delta r_{i+1} / 2 \\ \pm \Delta z_{i+1} / 2 \end{pmatrix} \right]^T \begin{pmatrix} b_{11}^{i+1} & 0 \\ 0 & b_{22}^{i+1} \end{pmatrix} - \left[\begin{pmatrix} r_i \\ z_i \end{pmatrix} - \begin{pmatrix} r_{i+1} \\ z_{i+1} \end{pmatrix} \right]^T$$

maximum allowed deviation of the preforming process

deviation after forming process

damping factors

shifting of the centroid of the scattering field

$b_{11}^{i+1}, b_{22}^{i+1}$: damping factors

with
 $b > 1$: decreasing
 $b < 1$: increasing
the scatter due to process i+1

process step	parameter	b_{11}	b_{22}
cold forging	eccentricity	2.63	2.75
thermal upsetting (single factor)	diameter	3.07	3.07

Fig. 2.58 Exemplary equation for tolerance engineering

On the left side of the equation, the mean deviation (r_i z_i) and the scatter of the laser rod end melting process ($\pm \Delta r_i/2$ $\pm \Delta z_i/2$) are mentioned. The mean deviation results from the movement velocity of the laser in the lateral direction to the rod, while the scatter develops, for example, from the flow of the inert gas during preforming. On the right side of the equation, the mean deviation of the cold forging (r_{i+1} z_{i+1}) and the scatter of this process ($\pm \Delta r_{i+1}/2$ $\pm \Delta z_{i+1}/2$) is mentioned. In the case of the mean deviation, it results from positioning deviations of the upper

and lower die in relation to each other, and the scatter develops from differences in the form filling of the die by the preform. The factors b_{11}^{i+1} and b_{22}^{i+1} are damping factors with values greater than one if the $(i+1)$ th process step decreases the scatter, or with values greater than zero and less than one if the $(i+1)$ th process step increases the scatter. These values result from the reverse calculation, which means the calculation of the scatter of the $(i-1)$ th process step from the deviations of the prior step. The factors have to be evaluated prior to the calibration process by using simulations or experiments. In Fig. 2.58 two examples of the parameter b_{11} and b_{22} are given. In the first row the parameters for the cold forging process after the laser rod end melting process are mentioned. In this case, the parameters show that the scattering field is decreased. The second row includes the parameter for the scattering field of the preform diameter after a thermal upsetting. As can be seen, the values for the parameters are also greater than one and therefore describe a decreasing of the scatter. Finally, the shifting of the centroid of the scattering field from the cold forging to the laser rod end melting has to be considered. This shifting is expressed by the difference between the two centroids.

If a process comprises more than two process steps, a series of tolerance functions is used to calculate the necessary tolerance to get sound parts. An example of such a series of functions is given by the following Fig. 2.59.

As can be seen, the left side of the equation in Fig. 2.59 is the same as in Fig. 2.58 and so is the part of the equation in the tall square brackets, but with different exponents and indices due to the three-stage process chain. This last part still describes the transformation of the $(i+2)$ th scattering field of the final product into the $(i+1)$ th scatter of the previous step. Now, the process chain consists of three process steps and the transformation of the scatter from the $(i+1)$ th step into the i th step has to be considered. The part between the tall square brackets can be understood as the $(i+1)$ th centroid and its scattering field, and is now multiplied with the damping factors for this $(i+1)$ th step and relocated by the last

inverse calculation for a process chain with three steps:

$$\left[\begin{pmatrix} r_i \\ z_i \end{pmatrix} + \begin{pmatrix} \pm \Delta r_i / 2 \\ \pm \Delta z_i / 2 \end{pmatrix} \right]^T = \left[\left[\begin{pmatrix} r_{i+2} \\ z_{i+1} \end{pmatrix} + \begin{pmatrix} \pm \Delta r_{i+2} / 2 \\ \pm \Delta z_{i+2} / 2 \end{pmatrix} \right]^T \begin{pmatrix} b_{11}^{i+2} & 0 \\ 0 & b_{22}^{i+2} \end{pmatrix} - \left[\begin{pmatrix} r_{i+1} \\ z_{i+1} \end{pmatrix} - \begin{pmatrix} r_{i+2} \\ z_{i+2} \end{pmatrix} \right]^T \right] \begin{pmatrix} b_{11}^{i+1} & 0 \\ 0 & b_{22}^{i+1} \end{pmatrix} - \left[\begin{pmatrix} r_i \\ z_i \end{pmatrix} - \begin{pmatrix} r_{i+1} \\ z_{i+1} \end{pmatrix} \right]^T$$

b_{11} , b_{22} : damping factors

with

$b > 1$: decreasing

$b < 1$: increasing

the scatter due to process $i+2$ and $i+1$

Fig. 2.59 Inverse calculation for a three-staged process chain

mathematical term. As before, the damping factors have to be evaluated in simulation or experimentally in the first place.

To receive a tolerance function for an arbitrary n -stage process chain, the following steps have to be considered. Firstly, the starting point is always the mathematical description of the center point and the scatter of the n th process step. Secondly, the first of the overall $n - 1$ shifts takes place. Therefore, the center point and the scatter are multiplied with the damping factors of the $(n - 1)$ th process step. Then, the difference between the center points of the n th and $(n - 1)$ th steps is subtracted. The result is the center point and the scatter of the $(n - 1)$ th step. Thirdly, the $(n - 2)$ th shift has to be done, and so on. This procedure is repeated until the $(n - (n - 1))$ th shift is done. Finally, a series of tolerance functions is received that describes the development of the allowed deviations or the tolerable tool wear through the overall process chain.

References

- [Alk15] Alkhezraji, H., El-Danaf, E., Wollmann, M., Wagner, L.: Enhanced fatigue strength of commercially pure Ti processed by rotary swaging. *Adv. Mat. Sci. Eng.* (2015). ID 301837
- [Ami17] Aminzadeh, I., Mashhadi, M.M., Sereshk, M.R.V.: Investigation of holder pressure and size effects in micro deep drawing of rectangular work pieces driven by piezoelectric actuator. *Mater. Sci. Eng.* **C71**, 685–689 (2017)
- [Bän01] Bänsch, E.: Finite element discretization of the Navier-Stokes equations with a free capillary surface. *Numer. Math.* **88**(2), 203–235 (2001)
- [Bän13] Bänsch, E., Paul, J., Schmidt, A.: An ALE finite element method for a coupled Stefan problem and Navier–Stokes equations with free capillary surface. *Int. J. Numer. Methods. Fluids* **71**(10), 1282–1296 (2013)
- [Beh12] Behrens, G., Kovac, J., Köhler, B., Vollertsen, F., Stock, H.: Drawability of thin magnetron sputtered aluminium zirconium foils in micro deep drawing. In: ICNFT 2012, Harbin China, pp. 30–35 (2012)
- [Beh13] Behrens, G., Vollertsen, F.: Influence of tool geometry variation on the punch force in micro deep drawing. In: *Proceedings of the ESAFORM 2013 Key Engineering Materials, The Current State-of-the-Art on Material Forming*, vols. 554–557, pp. 1306–1311. Trans Tech Publications, Zurich-Durnten (2013)
- [Beh15] Behrens, G., Ruhe, M., Tetzl, H., Vollertsen, F.: Effect of tool geometry variations on the punch force in micro deep drawing of rectangular components. *J. Prod. Eng.* **9**(2), 195–201 (2015)
- [Beh16] Behrens, G., Trier, F., Tetzl, H., Vollertsen, F.: Influence of tool geometry variations on the limiting drawing ratio in micro deep drawing. *Int. J. Mater. Form.* **9**(2), 253–258 (2016)
- [Brü12a] Brüning, H., Vollertsen, F.: Self-alignment capability of laser based free form heading process. In: Cosic, I. (ed.) *Proceedings of 11th International Scientific Conference*, pp. 427–430, Serbia (2012a)

- [Brü12b] Brüning, H., von Bargaen, R., Vollertsen, F., Zoch, H.-W.: Wärmebehandlung laser-generierter Vorformelemente: Analyse zur Robustheit der Fertigung von Vorformelementen sowie Einfluss einer Wärmebehandlung. *wt Werkstatttechnik online* (102), 702–708 (2012b)
- [Brü13a] Brüning, H., Jahn, M., Schmidt, A.: Process window for forming of micro preforms at different temperatures. In: Merklein, M., Franke, J., Hagenah, H. (eds.) *Proceedings of the WGP Congress 2013*, pp. 173–180, Germany (2013a)
- [Brü13b] Brüning, H., Vollertsen, F.: First approach to generate comb linked parts by laser melting. *J. Technol. Plasticity* **38**(1), 1–12 (2013b)
- [Brü13c] Brüning, H., Vollertsen, F.: Formability of micro material pre-forms generated by laser melting. In: Hinduja, S., Li, L. (eds.) *Proceedings of the 37th International MATADOR Conference*, pp. 373–376. Springer, London (2013c)
- [Brü13d] Brüning, H., Vollertsen, F.: Investigation on flow stress level of spherical preforms generated by laser melting. *Mater. Manuf. Process.* **28**(5), 519–523 (2013d)
- [Brü13e] Brüning, H.: Robustness of the laser melting process. In: *Proceedings of 32th International Congress on Applications of Lasers and Electro-Optics (ICALEO13)*, pp. 852–857 (2013e)
- [Brü14a] Brüning, H., Teepe, M., Vollertsen, F.: Surface roughness and size effect in dendrite arm spacing at preforms of AISI 304 (1.4301) generated by laser rod end melting. *Procedia Eng.* **81**, 1589–1594 (2014a)
- [Brü14b] Brüning, H., Veenaas, S., Vollertsen, F.: Determination of axial velocity of material accumulation in laser rod end melting process. In: Fang, F., Brinksmeier, E., Riemer, O. (eds.) *Proceedings of the 4th International Conference on Nanomanufacturing (nanoMan 2014)*, pp. F1–3 (2014b)
- [Brü14c] Brüning, H., Vollertsen, F.: Mit dem Laser zum Klettverschluss. *Laser* **3**, 26–28 (2014c)
- [Brü14d] Brüning, H., Vollertsen, F.: Numerical approach to determine natural strain of spherical preforms in open die upsetting. *Adv. Mater. Res.* **1018**, 325–332 (2014d)
- [Brü15a] Brüning, H., Vollertsen, F.: Energy efficiency in laser rod end melting. In: Graf, T., Emmelmann, C., Overmeyer, L., Vollertsen, F. (eds.) *Lasers in Manufacturing 2015 Proceedings*, p. 138 (2015a)
- [Brü15b] Brüning, H., Vollertsen, F.: Form filling behaviour of preforms generated by laser rod end melting. *CIRP Ann. Manuf. Technol.* **64**, 293–296 (2015b)
- [Brü15c] Brüning, H., Vollertsen, F.: Surface accuracy achieved by upsetting of preforms generated by laser rod end melting. In: Qin, T., Dean, T.A., Lin, J., Yuan, S.J., Vollertsen, F. (eds.) *4th International Conference on New Forming Technology*, vol. 21, pp. 1–7. EDP Sciences (2015c)
- [Brü15d] Brüning, H.: Form filling behaviour of preforms generated by laser rod end melting. *CIRP Ann. Manuf. Technol.* **64**, 293–296 (2015d)
- [Brü16a] Brüning, H., Jahn, M., Vollertsen, F., Schmidt, A.: Influence of laser beam absorption mechanism on eccentricity of preforms in laser rod end melting. In: *11th International Conference on Micro Manufacturing*, p. 77 (2016a)
- [Brü16b] Brüning, H.: Prozesscharakteristiken des thermischen Stoffanhäufens in der Mikrofertigung. *Strahltechnik*. BIAS Verlag (2016b)
- [Brü17] Brüning, H., Schattmann, C., Krüger, M.: Melt pool geometry in laser blank rim melting to generate continuous cylindrical preforms. *Prod. Eng. Res. Develop.* **4–5**(11), 425–433 (2017)
- [Che17] Cheng, C., Wan, W., Meng, B.: Size effect on the forming limit of sheet metal in micro-scaled plastic deformation considering free surface roughening. In: *ICTP 2017*, 17–22 September 2017, Cambridge, United Kingdom

- [Chu11] Chu, H.: An Automated Micromanipulation System for 3D Parallel Microassembly. Dissertation edition (2011)
- [Chu14] Jin, C.K., Jeong, M.G., Kang, C.G.: Effect of rubber forming process parameters on micro-patterning of thin metallic plates. *Procedia Eng.* **81**, 1439–1444 (2014)
- [Eic10] Eichenhüller, B., Engel, U., Geiger, M.: Microforming and investigation of parameter interactions. *Prod. Eng.* **4**(2–3):135–140 (2010)
- [Ell81] Elliot, C.M.: On the finite element approximation of an elliptic variational Inequality arising from an implicit time discretization of the Stefan problem. *IMA J. Numer. Anal.* **1**, 115–125 (1981)
- [Gau13] Gau, J.-T., Teegala, S., Huang, K.-M., Hsiao, T.-J., Lin, B.-T.: Using micro deep drawing with ironing stages to form stainless steel 304 micro cups. *J. Manuf. Process.* **15**, 298–305 (2013)
- [Gha08] Ghaei, A., Movahhedy, M.R., Karimi Taheri, A.: Finite element modelling simulation of radial forging of tubes without mandrel. *Mater. Des.* **29**, 867–872 (2008)
- [Hau96] Haug, R.: Infeed rotary swaging - Ph.D. Thesis. University of Stuttgart (1996)
- [Hei17] Heinrich, L., Kobayashi, H., Shimizu, T., Yang, M., Vollertsen, F.: Influence of asymmetrical drawing radius deviation in micro deep drawing. In: *Proceedings of the 36th IDDRG Conference—Materials Modelling and Testing for Sheet Metal Forming. Journal of Physics*, vol. 896, p. 012060 (2017) (online)
- [Hof17] Hof, L., Heinrich, L., Wüthrich, R.: Towards high precision manufacturing of glass tools by spark assisted chemical engraving (SACE) for micro forming techniques. In: *World Congress on Micro and Nano Manufacturing (WCMNM 2017)*, Kaohsiung, Taiwan (2017). USB-Stick
- [Hu09] Hu, Z., Walter, R., Vollertsen, F.: Forming tools for micro deep drawing—influence of geometrical tolerance of forming tools on the punch force in micro deep drawing. *wt Werkstattstechnik online*, H **11**(12), 814–819 (2009)
- [Hu11] Hu, Z., Vollertsen, F.: Investigation on the optimisation of the blank shape for micro deep drawing of rectangular parts. In: *Steel Research International, Special Edition: 10th International Conference on Technology of Plasticity (ICTP 2011)*, VCH Weinheim, pp. 974–978 (2011)
- [Hüg09] Hügel, H., Graf, T.: *Laser in der Fertigung: Strahlquellen, Systeme, Fertigungsverfahren*. Studium. Vieweg + Teubner, Wiesbaden, 2., neu bearb. Aufl. edn (2009)
- [Irt14] Irthia, I., Green, G., Hashim, S., Kriama, A.: Experimental and numerical investigation on micro deep drawing process of stainless steel 304 foils using flexible tools. *Int. J. Mach. Tools Manuf.* **76**, 21–33 (2014)
- [Ish15a] Ishkina, S., Kuhfuss, B., Schenck, C.: Grain size modification by micro rotary swaging. *Key Eng. Mater.* 651–653, 627–632 (2015a)
- [Ish15b] Ishkina, S., Schenck, C., Kuhfuss, B.: Beeinflussung der Umformbarkeit von Halbzeugen durch Modifikationen beim Mikrorundkneten. Tagungsband. In: Hopmann, Ch., Brecher, Ch., Dietzel, A., Drummer, D., Hanemann, T., Manske, E., Schomburg, W.K., Schulze, V., Ullrich, H., Vollertsen, F., Wulfsberg, J.-P. (eds.) *Kolloquium Mikroproduktion in Aachen*, pp. 64–71, 16–17 November 2015, IKV Aachen (2015b). ISBN: 978-3-00-050-755-7
- [Ish15c] Ishkina, S., Kuhfuss, B., Schenck, C.: Influence of the relative rotational speed on component features in micro rotary swaging. In: Qin, Y., Dean, T.A., Lin, J., Yuan, S.J., Vollertsen, F. (eds.) *Proceedings of the 4th International Conference on New Forming Technology (ICNFT 2015)* 6–9 August 2015, Glasgow, Scotland (UK). Published in: *Materials Science, Engineering and Chemistry* vol. 21, p. 09012 (2015c). 1–7 ISBN: 978-2-7598-1823-5
- [Ish17a] Ishkina, S., Schenck, C., Herrmann, M., Kuhfuß, B.: Konditionierung von Halbzeugen durch Mikrorundkneten. Fachbeiträge. In: Vollertsen, F., Hopmann, C., Schulze, V., Wulfsberg, J. (eds.) *Kolloquium Mikroproduktion*, Bremen, 27–28 November 2017, pp. 99–106. BIAS Verlag (2017a). (online) ISBN: 978-3-933762-56-6

- [Ish17b] Ishkina, S., Schenck, C., Kuhfuss, B., Mouri, E., Tobeck, K.: Eccentric rotary swaging. *Int. J. Precis. Eng. Manuf.* **18**(7), 1035–1041 (2017b)
- [Ish18] Ishkina, S., Schenck, C., Kuhfuss, B.: Conditioning of material properties by micro rotary swaging. In: AIP Conference Proceedings 1960, 160013 (2018). <https://doi.org/10.1063/1.5035039>
- [Jah12a] Jahn, M., Schmidt, A.: Finite element simulation of a material accumulation process including phase transitions and a capillary surface. Technical Report 12-03, ZeTeM, Bremen
- [Jah12] Jahn, M., Luttmann, A., Schmidt, A.: A FEM simulation for solid-liquid-solid phase transitions during the production of micro-components. In: Cosic, I. (ed.) Proceedings of 11th International Scientific Conference MMA - Advanced Production Technologies, pp. 231–234, Serbia (2012)
- [Jah13] Jahn, M., Brüning, H., Schmidt, A., Vollertsen, F.: Herstellen von Mikroverbundstrukturen durch Stoffanhäufen und Laserschweißen. In: Tutsch, R. (ed.) Tagungsband 6. Kolloquium Mikroproduktion, p. A11. Shaker Verlag (2013)
- [Jah14b] Jahn, M., Schmidt, A., Bänsch, E.: 3D finite element simulation of a material accumulation process including phase transitions and a capillary surface. Technical Report 14-03, ZeTeM, Bremen
- [Jah14] Jahn, M., Brüning, H., Schmidt, A., Vollertsen, F.: Energy dissipation in laser-based free form heading: a numerical approach. *Prod. Eng.* **8**(1–2), 51–61 (2014)
- [Kle15] Klein, B.: FEM. Grundlagen und Anwendungen der Finite-Elemente-Methode im Maschinen- und Fahrzeugbau. Springer Vieweg, Wiesbaden (2015)
- [Klo06] Klocke, F., König, W.: *Fertigungsverfahren—Umformen*. Springer-Verlag, Berlin, Heidelberg (2006)
- [Kov18] Kovac, J.; Heinrich, L.; Köhler, B.; Mehner, A.; Clausen, B.; Zoch, H.: Comparison of the tensile properties and drawability of thin bimetallic aluminium-scandium-zirconium/stainless steel foils and monometallic Al–Sc–Zr fabricated by magnetron sputtering. In: Vollertsen, F., Dean, T.A., Qin, Y., Yuan, S.J. (eds.) 5th International Conference on New Forming Technology (ICNFT 2018), MATEC Web Conference, vol. 190, p. 15012 (2018)
- [Kuh08a] Kuhfuss, B., Mouri, E., Piwek, V.: Micro rotary swaging: process limitations and attempts to their extension. *Microsys. Technol.* **14** 1995–2000 (2008a)
- [Kuh08b] Kuhfuss, B., Mouri, E., Piwek, V.: Influence of the feed rate on work quality in micro rotary swaging. In: 3rd ICOMM Pittsburgh, USA pp. 86–91 (2008b)
- [Kuh09a] Kuhfuss, B., Mouri, E., Piwek, V.: Manufacturing of micro components by means of plunge rotary swaging. In: Proceedings of the 9th Euspen, San Sebastian, pp. 58–61 (2009c)
- [Kuh09b] Kuhfuss, B., Piwek, V., Mouri, E.: Vergleich charakteristischer Einflussgrößen beim Mikro- und Makrorundkneten. In: Vollertsen, F., Büttgenbach, S., Kraft, O., Michaeli, W. (eds.) Kolloquium Mikroproduktion, pp. 219–228. BIAS Verlag, Bremen (2009d)
- [Kuh10] Kuhfuss, B., Mouri, E., Piwek, V.: Mikrorundkneten. Ein neues Umformverfahren mit viel Potenzial. *Industrie Manag.* **6**, 14–16 (2010)
- [Kuh11] Kuhfuss, B., Mouri, E., Piwek, V.: Load measurement during rotary swaging of micro components using strain gauges. In: 6th International Conference on Micro Manufacturing (ICOMM 2011) Tokyo, Japan, pp. 53–58 (2011)
- [Kuh12a] Kuhfuss, B., Mouri, E., Piwek, V., Hork, M., Werner, M.: Reib- und formschlüssige Verbindungen durch Rundkneten, VDI-Konstruktion, Ingenieurwerkstoffe Metall, Jan./Feb. 1/2-2012
- [Kuh12b] Kuhfuss, B., Mouri, E., Piwek, V.: Effects of dry machining on process limits in micro rotary swaging. In: 7th ICOMM 2012, pp. 243–247, Evanston, IL, USA (2012b)
- [Kuh13] Kuhfuss, B., Mouri, E.: Bulk metal forming. In: Vollertsen, F. (ed.) *Micro Metal Forming*, pp. 103–133. Springer, Heidelberg (2013). ISBN: 978-3-642-30915-1
- [Kur98] Kurz, W., Fisher, D.J.: *Fundamentals of Solidification*, 4 rev. ed. Trans Tech Publications, Aedermannsdorf (1998)

- [Lan02] Lange, K.: Umformtechnik Handbuch für Industrie und Wissenschaft: Band 4: Sonderverfahren, Prozeßsimulation, Werkzeugtechnik, Produktion. Springer, Heidelberg (2002)
- [Lan06] Lange, K.: Handbook of Metal Forming. Society of Manufacturing Engineers, Dearborn, Michigan (2006)
- [Liu17] Liu, Y., Herrmann, M., Schenck, C., Kuhfuss, B.: Plastic deformation history in infeed rotary swaging process. In: AIP Conference Proceedings 1896, 080013 (2017). <https://doi.org/10.1063/1.5008093>
- [Lut18] Luttmann, A.: Modellierung und Simulation von Prozessen mit fest-flüssig Phasenübergang und freiem Kapillarrand. Dissertation, Universität Bremen (2018)
- [Mes94] Messner, A.; Engel, U.; Kals, R.; Vollertsen, F.: Size effect in the FE-simulation of micro-forming processes. J. Mater. Process. Technol. **45**(1–4), 371–376 (1994)
- [Meß98] Meßner, A.: Kaltmassivumformung metallischer Kleinstteile: Werkstoffverhalten, Wirkflächenreibung, Prozessauslegung. Meisenbach, Bamberg (1998)
- [Mic03] Michel, J.F., Picart, P.: Size effects on the constitutive behaviour for brass in sheet metal forming. J. Mater. Process. Technol. **141**, 439–446 (2003)
- [Mou14a] Mouri, E., Ishkina, S., Kuhfuss, B., Hochrainer, T., Struss, A., Hunkel, M.: 2D-simulation of material flow during infeed rotary swaging using finite element method. Procedia Eng. **81**, 2342–2347 (2014a)
- [Mou14b] Mouri, E., Kuhfuss, B.: Properties of alloy 304 micro parts processed by rotary swaging. In: Fang, F., Brinksmeier, E., Riemer, O. (eds.) Proceedings of the NanoMan (2014b). (digital)
- [Mou14c] Mouri, E., Wilhelmi, P., Kuhfuss, B., Schenck, C., Tracht, K.: Wire joining by rotary swaging. Procedia Eng. **81**, 2012–2017 (2014c)
- [Mou15a] Kuhfuss, B., Mouri, E., Clausen, B., Epp, J., Koeler, B.: Investigation of deformation induced martensitic transformation during incremental forming of 304 stainless steel wires. Key Eng. Mater. 651–653, 645–650 (2015a)
- [Mou15b] Mouri, E., Kuhfuss, B.: Erhöhung der Umformgeschwindigkeit beim Mikrorundkneten durch Einsatz von Zwischenelementen. In: Hopmann, Ch., Brecher, Ch., Dietzel, A., Drummer, D., Hanemann, T., Manske, E., Schomburg, W.K., Schulze, V., Ullrich, H., Vollertsen, F., Wulfsberg, J.-P. (eds.) Kolloquium Mikroproduktion, pp. 72–78. IKV Aachen (2015b)
- [Mou18a] Mouri, E., Schenck, C., Herrmann, M., Kuhfuss, B.: High productivity micro rotary swaging. In: MATEC Web Conferences, vol. 190, p. 15014 (2018a). <https://doi.org/10.1051/mateconf/201819015014>
- [Mou18b] Mouri, E., Wilhelmi, P., Schenck, C., Herrmann, M., Kuhfuss, B.: Material flow control in plunge micro rotary swaging. In: MATEC Web Conference, vol. 190, p. 15002 (2018b). <https://doi.org/10.1051/mateconf/201819015002>
- [Neu12] Neumann, S.: Einflussanalyse beim single mode Faserlaserschweißen zur Vermeidung des Humping-Phänomens, volume 48 of Strahltechnik. BIAS Verlag, Bremen (2012)
- [Ron06] Rong, L., Nie, Z., Zuo, T.: FEA modeling of effect of axial feeding velocity on strain field of rotary swaging process of pure magnesium. Trans. Nonferrous Met. Soc. China **16**, 1015–1020 (2006)
- [Sao90] Saotome, Y.; Kaga, H.: Micro deep drawability of very thin sheet steels. Adv. Technol. Plasticity **3**, 1341–1346 (1990)
- [Sch16] Şchiopu, V., Luca, D.: A new net-shape plating technology for axisymmetric metallic parts using rotary swaging. Int. J. Adv. Manuf. Tech. **85**, 2471–2482 (2016)
- [Sch17a] Schattmann, C., Brüning, H., Ellmers, J.-H., Vollertsen, F.: Mikroverbund-Strukturen für die Kaltmassivumformung. UMFORMtechnik Massiv + Leichtbau **1**, 22 (2017a)
- [Sch17b] Schattmann, C., Hennick, C., Brüning, H., Pfefferkorn, F.E., Vollertsen, F.: Generation and shape control of line-linked preforms by laser melting. J. Technol. Plasticity **42**(1), 21–32 (2017b)

- [Sch17c] Schattmann, C., Vollertsen, F.: Metrisches Gewinde in dünnen Blechen. *Mikroproduktion* **06**, 60–61 (2017c)
- [Sch17d] Schattmann, C., Wilhelmi, P., Schenck, C., Kuhfuß, B., Vollertsen, F.: Erhöhung der Taktrate bei der Herstellung von Zwischenformen im Linienverbund. In: Vollertsen, F., Hopmann, C., Schulze, V., Wulfsberg, J.P. (eds.) *Fachbeiträge 8. Kolloquium Mikroproduktion*, pp. 227–236. BIAS Verlag, Bremen (2017d)
- [Ste11a] Stephen, A., Brüning, H., Vollertsen, F.: Fokuslagensteuerung beim laserbasierten Stoffanhäufen. In: Kraft, O., Haug, A., Vollertsen, F., Büttgenbach, S. (eds.) *Kolloquium Mikroproduktion und Abschlusskolloquium SFB 499*, pp. 155–160. KIT Scientific Reports (2011a)
- [Ste11b] Stephen, A., Vollertsen, F.: Influence of the rod diameter on the upset ratio in laser-based free form heading. In: Hirt, G., Tekkaya, A.E. (eds.) *Steel Research Int, Special Edition: 10th Int. Conf. on Technology of Plasticity*, pp. 220–223, Weinheim. Wiley-VCH Verlag GmbH (2011b)
- [Tet15] Tetzel, H.; Rathmann, L.; Vollertsen, F.: Entwicklung eines Simulationsmodells für mehrstufige Tiefziehprozesse im Mikrobereich. 7. Kolloquium Mikroproduktion (16–17 November 2015, Aachen), pp. 79–86. IKV Aachen (2015)
- [Tet16] Tetzel, H., Rathmann, L., Heinrich, L.: Simulation accuracy of a multistage micro deep drawing process. *JPT J. Technol. Plasticity* **41**, 1–11 (2016)
- [Thi93] Thiruvarduchelvan, S.: Elastomers in metal forming - a review. *J. Mat. Proc. Tech.* **39**, 55–82 (1993)
- [Toe18] Toenjes, A., Ishkina, S., Schenck, C., von Hehl, A., Zoch, H.-W., Kuhfuss, B.: Eccentric rotary swaging variants. In: *Proceedings of the 5th International Conference on New Forming Technology (ICNFT 2018)*, 18–21 September 2018, Bremen, Germany (DE), MATEC Web of Conferences, vol. 190, p. 15003 (2018). <https://doi.org/10.1051/mateconf/201819015003>
- [Tsc06] Tschätsch, H.: *Metal Forming Practise: Processes—Machines—Tools*. Springer-Verlag, Heidelberg (2006)
- [Val00] Valiev, R.Z.; Islamgaliev R.K.; Aleksandrov, I.V.: Bulk nanostructured materials from severe plastic deformation. *Prog. Mater. Sci.* **45**, 103–189 (2000)
- [Vee15] Veenaas, S., Behrens, G., Kröger, K., Vollertsen, F.: Determination of forming limit diagrams for thin foil materials based on scaled Nakazima tests. In: *Progress in Production Engineering*, WGP Kongress, pp. 190–198 (2015)
- [Vol08] Vollertsen, F., Walther, R.: Energy balance in laser-based free form heading. *CIRP Ann. Manuf. Technol.* **57**(1), 291–294 (2008)
- [Vol09] Vollertsen, F., Biermann, D., Hansen, H.N., Jawahir, I.S., Kuzman, K.: Size effects in manufacturing of metallic components. *CIRP Ann. Manuf. Technol.* **58**, 566–587 (2009)
- [Vol13] Vollertsen, F.: *Micro Metal Forming. Lecture Notes in Production Engineering*. Springer, Heidelberg (2013)
- [Wag05] Wagner, S., Felde, A.: Niederfrequenter schwingender Niederhalter beim Tiefziehen kleiner Bauteile. *Abschlussbericht DFG-Schwerpunktprogramm 1074*, Verlagshaus Mainz GmbH Aachen, pp. 364–371 (2005)
- [Wal09] Walther, R.: An enhanced model for energy balance in laser-based free form heading. *J. Micromechanics Microengineering* **19**(5), 054007 (2009)
- [Wan07] Wang, G.G., Shan, S.: Review of metamodeling techniques in support of engineering design optimization. *J. Mech. Des.* **129**(4), 370 (2007)
- [Wil13] Wilhelmi, P., Moumi, E., Foremny, E., Kuhfuss, B., Schenck, C.: Feeding and positioning of linked parts in micro production chains. In: Archenti, A., Maffei, A. (eds.) *Proceedings of NEWTECH 2013*, vol. 2, pp. 75–84 (2013)
- [Wil15] Wilhelmi, P., Moumi, E., Schenck, C., Kuhfuss, B.: Werkstofffluss beim Mikrorundkneten im Linienverbund In: Hopmann, Ch., Brecher, Ch., Dietzel, A., Drummer, D., Hanemann, T., Manske, E., Schomburg, W.K., Schulze, V., Ullrich, H., Vollertsen, F., Wulfsberg, J.P. (eds.) *Kolloquium Mikroproduktion*, pp. 72–78 (2015)

- [Woi15] Woizeschke, P., Brüning, H., Vollertsen, F.: Lasergenerierte Blechkanten. *Laser Mag.* **5**, 22–24 (2015)
- [Zha14] Zhang, Q., Jin, K., Mu, D.: Tube/tube joining technology by using rotary swaging forming method. *J. Mat. Proc. Tech.* **214**, 2085–2094 (2014)

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