Prod. Eng. Res. Devel. (2008) 2:269–276
DOI 10.1007/s11740-008-0110-8

PRODUCTION PROCESS

Determination of tribological conditions within hot stamping

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Received: 28 January 2008 / Accepted: 14 May 2008 / Published online: 14 June 2008
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Abstract Innovative hot sheet metal forming technologies are gaining an increasing significance in the scope of application of more and more innovative high and ultra high strength steels in the automotive industry. With respect to a numerical process design beside the mechanical and the thermal material characteristics the friction coefficient represents an important input parameter for finite element (FE) simulation. Within the scope of this paper an evaluation method for the determination of the friction coefficient μ for the direct hot stamping process of boron-manganese steels will be presented. Therefore cup deep drawing tests at elevated temperatures following the time-temperature-characteristic of the hot stamping process are carried out. For the calculation of the friction coefficient the approach according to Siebel for the modeling of the maximum drawing force is used.

Keywords Production process · Hot stamping · Friction coefficient

1 Introduction

Whereas in former years warm and hot forming was mainly applied for bulk metals, nowadays temperature assisted sheet metal forming technologies obtain more and more acceptance and increasing industrial relevance [1, 2]. This development is mainly driven by the potential high and ultra high strength steels offer for automotive lightweight construction regarding the reduction of mass and thus fuel consumption by simultaneously improving the safety standards. But increasing the strength of steels leads in general to a limited formability combined with a high tendency to spring back. With the objective to improve the formability as well as the geometrical accordance of components made out of those new high strength steel grades, forming at elevated temperatures represents a useful solution to counter the disadvantages mentioned above. Probably the key technology, which showed and approved, respectively, the big advantages and possibilities of temperature assisted sheet metal forming in the last 5–10 years is the so called hot stamping process of quenchenable ultra high strength steels of the type 22MnB5. Hot stamping represents a non isothermal forming process used for sheet metals combining forming and quenching in one process step, which offers the possibility to realize crash relevant structural parts with a complex geometric shape and a final tensile strength up to 1,700 MPa. So the sheet thickness can be minimized leading to a weight reduction without reducing the safety of the vehicle occupants [3]. Currently the hot stamping technology can be divided into two main variants, the direct and the indirect hot stamping (Fig. 1). Within the direct hot stamping process a flat blank is heated up in a furnace, homogeneously austenitized for a certain time above the material specific Ac_3-temperature, transferred to the press by robotic feeding systems and formed as well as quenched finally in one process step. In contrast to this for the indirect process a pre-formed component is used, which is only calibrated and quenched in the tool after the austenitization and transfer operation. Beside this operational

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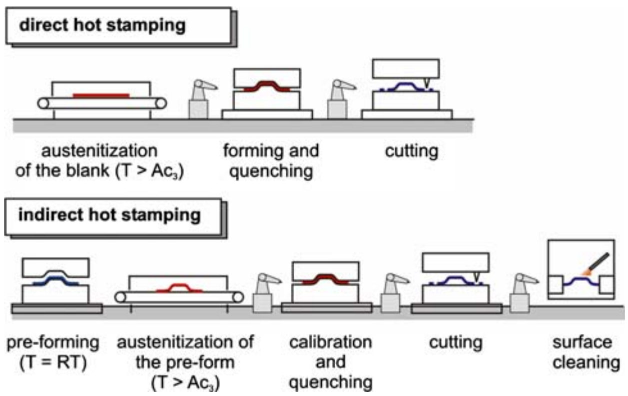

Fig. 1 Schematic illustration of the direct and indirect hot stamping process chain

difference for the direct hot stamping process pre-coated blanks of the boron-manganese steel 22MnB5 are commonly used. By contrast for the indirect method uncoated blanks are usually applied wherefore the so produced hot stamped components have to be cleaned by blasting in order to remove the scale and to apply an anti-corrosion coating.

With respect to a numerical process design among the others the friction coefficient μ describing the tribological conditions during forming displays an important material and process characteristic for the FE simulation. Regarding the hot stamping process in literature neither research activities nor results can be found with respect to the investigations on friction. The following paper presents a combined experimental-analytical-numerical evaluation method for the determination of the friction coefficient in dependency of the influencing parameters of the hot stamping process by using a modified cup deep drawing test. Experimental results will be shown with respect to the influence of temperature on the friction coefficient and thus on the tribological conditions within hot stamping.

2 Experimental setup and proceeding

2.1 Investigated material

As test material the boron-manganese steel 22MnB5 is used, which represents currently the standard high strength steel applied in the automotive industry for the manufacturing of hot stamped parts. Hereby an aluminum–silicon pre-coated grade with an initial sheet thickness of 1.75 mm was used. In the as delivered condition the material exhibits a fine grain ferritic–perlitic microstructure characterized by good formability a yield and tensile strength of about 400 and 600 MPa, respectively. After quenching an increase of its yield and tensile strength above 1,100 and 1,500 MPa could be achieved due to a martensitic phase transformation occurring during the hot stamping process assuring a minimum cooling rate of 27 K/s [4]. With respect to the aspired microstructural transformation a previous homogeneous austenitization of the boron-manganese steel is required above its specific Ac_3-temperature up to approximately 850°C. As shown in previous publications [5, 6] and exemplarily illustrated in Fig. 2, the plastic deformation behavior of the boron-manganese steel 22MnB5 in the austenitic state is characterized by a significant influence of the strain, the temperature and the strain rate. Regarding the modeling of material flow behavior for finite element analysis (FEA) those parameters have to be taken into account as shown in Merklein et al. [7, 8].

2.2 Modified cup deep drawing test for elevated temperatures

In order to determine the friction coefficient under process relevant conditions a modified cup deep drawing test setup had been developed and validated at the Chair of Manufacturing Technology at the University of Erlangen-Nuremberg [9]. The tool is integrated into a 1,000 kN hydraulic press type TSP100So (Lasco, Coburg) equipped with load cells for the continuous on-line recording of the punch force F_P and the blank holder force F_{BH} during the test. The punch, the blank holder and the die contain separated heating cartridges as well as compress air cooling units to control the temperature of the tool. Hereby a maximum tool temperature of 650°C can be realized. The diameter of the punch and the die is 50 and 59 mm, respectively, the according edge radii are 10 mm. Regarding the previous heat treatment of the blank according to the hot stamping process (compare Fig. 1) a furnace type K1150-3 (Heraeus, Hanau) is placed beside the press. To measure the temperature of the hot blank instantaneously before the complete closing of the die a thermo camera type Variotherm (Jenoptik, Jena) is applied, whereby an emission coefficient ε of 0.8 determined in prior tests is used. Hence reproducible results can be provided, because only blanks with comparable initial temperatures before the deep drawing process takes place, are used for the following evaluation of the data.

For the experimental reproduction of the hot stamping process blanks with a diameter between 80 and 90 mm had been exposed to a heat treatment of 5 min at an austenitization temperature of 900 and 950°C [8] before being transferred manually to the cold or defined heated tooling device and promptly being drawn. The total time elapsing before the actual forming process starts, including transfer duration and closing time of the die, is about 10 s. To avoid a too distinctive cooling or even quenching of the

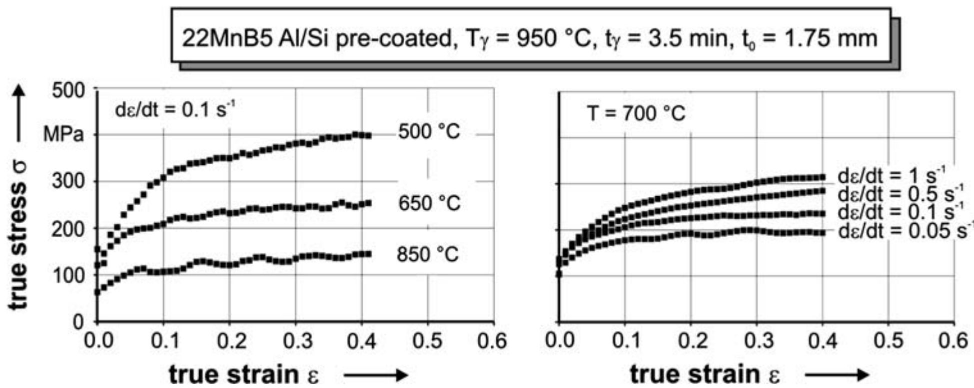

Fig. 2 Flow behavior of 22MnB5 in dependency of strain, temperature and strain rate at elevated temperatures in the austenitic state

specimens in the forming zone before the forming operation, the cup deep drawing tests have been performed without applying a blank holder force to the sheet in the flange area. In order to assure nevertheless constant conditions according to the real process a distance ring ($t_{\text{ring}} = 3$ mm) between drawing die and blank holder had been established (Fig. 3). Thus the material in the forming zone remains hot during the test, whereas the force transmitting zone of the blank cools down leading to an enhanced formability of the specimen [10]. Therefore, the temperature of the punch was kept constant at room temperature. In contrast to that the temperatures of the blank holder and the drawing die had been varied between room temperature and 500°C with the objective to investigate the influence of temperature on the tribological conditions within hot stamping. All the tests have been carried out with a punch velocity of 10 mm/s leading to an average strain rate of 0.1 s^{-1} respective to [12], what could be approved by FEA. Each parameter combination was tested at least five times.

2.3 Evaluation of the friction coefficient

According to Siebel [11] the maximum drawing force $F_{\text{draw,max}}$ can analytically be described using Eq. 1. Hereby d_m represents the diameter of the cup wall, d_p the external cup diameter at the maximum drawing force $F_{\text{draw,max}}$, r_R

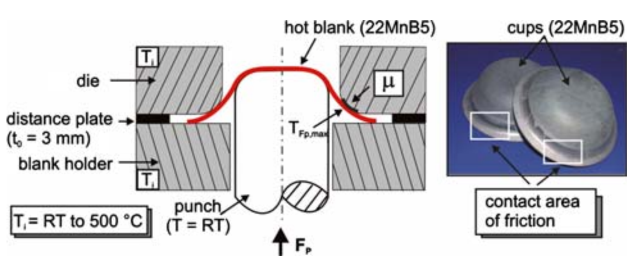

Fig. 3 Schematic sketch of the experimental setup of the cup deep drawing test (*left*) and two drawn cups at elevated temperatures with the contact areas of the main friction between blank and tool pointed out (*right*)

the edge radius of the die, t_0 the initial sheet thickness and σ_{fm1} and σ_{fm2} the mean true stresses in the flange and at the drawing die radius at the maximum drawing force, respectively. Due to the fact, that the test is performed without any blank holder force ($F_{\text{BH}} \approx 0$) only one friction coefficient term μ_3 characterizing the tribological conditions in the area between blank and die radius remains. Since $F_{\text{draw,max}}$ is provided as experimental data hence in Eq. 1 all other terms except of μ_3 are known or either can be calculated or obtained via FEA. Thus an integral friction coefficient related to the contact surface between blank and drawing die (see Fig. 3) can be determined by solving Eq. 1. The feasibility of this method had been already shown in [9, 12, 13].

$$F_{\text{draw,max}} = \pi \cdot d_m \cdot t_0 \left[e^{\mu_3 \frac{\pi}{2}} \left(1,1 \cdot \sigma_{\text{fm1}} \ln \frac{d_p}{d_m} + \frac{(\mu_1 + \mu_2) \cdot F_{\text{BH}}}{\pi \cdot d_p \cdot t_0} \right) + \sigma_{\text{fm2}} \cdot \frac{t_0}{2 r_R} \right] \quad (1)$$

One parameter in Eq. 1, which will neither be served as experimental result or geometrical characteristic, is the external cup diameter d_p. For the determination of d_p numerical studies in AutoForm Hotform (version 4.1 alpha) have been performed with the objective to investigate the evolution of the external diameter of the cup in dependency of the drawing progression. Regarding the validation of the numerical prediction of the outer cup diameter different specimens with an initial diameter d_0 of 90, 85, and 80 mm were drawn up to varying punch strokes between 0 and 35 mm. Afterward the respective current outer diameter of the cups had been measured. As follows from the good accordance between the numerical and the experimental results with a stability index of $R^2 = 0.9983$ (Fig. 4) it can be concluded, that FEA can be used providing reliable values for d_p at the maximum drawing force for solving Eq. 1. According to [14, 15] d_p also can be analytically approximated by the relation $d_{\text{p}} \approx 0.79 \cdot d_0$. With respect to the experimental results within the scope of this work, the drawing depth until the maximum force

$F_{\mathrm{draw,max}}$ is reached and thus the corresponding external cup diameter d_p is dependent on the thermal conditions as shown in Fig. 4 (right hand side). Therefore the approach predicting constant values for d_p as function of the initial blank diameter d_0 could neither be approved nor be applied as illustrated in Fig. 7.

For the calculation of the friction coefficient μ_3 according to Eq. 1, the mean true stresses σ_{fm1} and σ_{fm2} at the drawing force maximum had been approximated using the phenomenological approach according to Eqs. 2–4, whose applicability regarding the accurate modeling of the flow behavior of 22MnB5 in dependency of strain, strain rate and temperature had been shown and approved in previous works [7, 8]. The different coefficients included in Eqs. 2–4 are summarized in Table 1. The required mean strains $\varepsilon_{\mathrm{m1}}$ and $\varepsilon_{\mathrm{m2}}$ for the respective calculation of σ_{fm1} and σ_{fm2} can either be determined via geometrical considerations according to [11, 15] using the d_0, d_m and d_p, as well as the external diameter of drawing ring radius d_M assuming volume constancy, or via FEA.

$$\sigma(\varepsilon, \dot{\varepsilon}, T) = K \cdot \exp(\beta/T) \cdot (b + \varepsilon)^{n(T)} \cdot \dot{\varepsilon}^{m(T)} \quad (2)$$

with

$$n(T) = n_0 \cdot \exp(-c_n(T_i - T_0)) \quad (3)$$

and

$$m(T) = m_0 \cdot \exp(c_m(T_i - T_0)) \quad (4)$$

On the one hand the flow properties of the boron-manganese steel 22MnB5 are characterized by a significant dependency on the temperature (compare Fig. 2). On the other hand the cup deep drawing tests according to the time-temperature characteristic of the hot stamping process are not performed under isothermal conditions. Consequently with respect to a more accurate modeling of the true stresses σ_{fm1} and σ_{fm2} the cooling of the specimens during the forming operation cannot be neglected. Therefore, the temperatures acquired via thermo-graphic measurements directly before the tool is closed can hence not be applied as input parameters for the approximation of the true stresses at the maximum drawing force. As the temperature evolution of the cups cannot be measured online within the tool a FEA has to be performed serving the thermal conditions and thus the required temperature data. The quality of the simulation is hereby significantly dependent on the performance of the FE-model being capable to reproduce the thermal conditions during the deep drawing process. Therefore, the dependency of heat transfer coefficient α, in general applied in FE simulation for modeling the heat exchange between hot blank and tool on the contact conditions, has to be considered. In previous work [8] values for the heat transfer coefficient for both-sided full metallic contact as function of the contact pressure had been determined (compare Fig. 5) analytically, according to Newton's cooling law (Eq. 5) after a heat treatment duration $t_\gamma = 5$ min at an austenitization temperature $T_\gamma = 950$°C:

$$T_{\mathrm{blank}}(t) = (T_{\mathrm{blank,initial}} - T_{\mathrm{tool}}) \cdot \exp(-\alpha A t / c_p V \rho) + T_{\mathrm{tool}} \quad (5)$$

The illustrated heat transfer coefficients in Fig. 5 represent average values for a contact dependent cooling of sheet specimens within a temperature range between 800 °C and 400 °C. These data furthermore had been used for setting up the FE-model in AutoForm Hotform (version 4.1 alpha). The influence of occurring gap distances on the thermal conditions could not be considered by the applied FE tool. As heat transfer coefficient to the ambience an experimentally approved value of 125 W/m^2 K was chosen.

Table 1 Applied coefficients for the mathematical description of the flow behavior of 22MnB5 using Eqs. 2–4

	K	β	b	n_0	c_n	m_0	c_m
Coefficient	34.38	21.86	0.0025	0.2034	0.0024	0.00792	0.0019
Deviation	1.61	42.98	0.0006	0.0044	0.0016	0.0039	0.0002

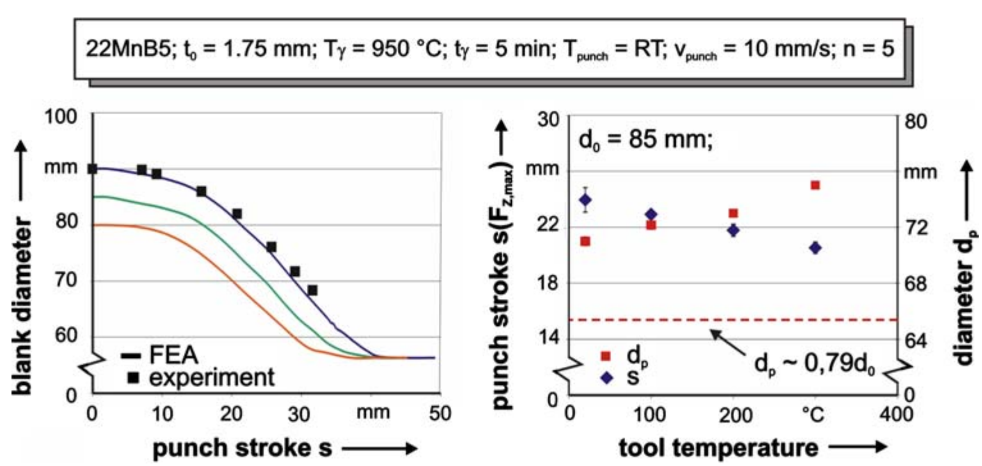

Fig. 4 FEA-predicted evolution of the external cup diameter d_p as function of the punch stroke s validated by experimental investigations (*left*); dependency of the temperature of the tool on the punch stroke until the reaching of the maximum drawing force and the respective external cup diameter d_p (*right*)

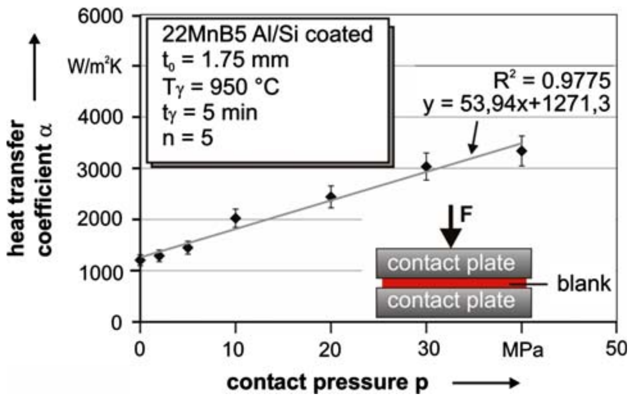

Fig. 5 Heat transfer coefficient as function of the contact pressure for both-sided full metallic contact

The values of α displayed in Fig. 5 had been determined for an assumed constant tool temperature T_{tool} of 20°C [8]. Generally the magnitude of the heat transfer between two bodies is mainly determined by their respective temperature difference (see Eq. 5). Consequently increasing the temperature of the die and the blank holder within the cup deep drawing tests will thus lead to a reduced heat exchange between tool and blank. To take this into account regarding the numerical reproduction of the experimental thermal conditions the heat transfer coefficient values had been standardized accordingly. Furthermore the numerical reproduction of the thermal conditions had been supported by additional hardness measurements and microstructural analysis of the drawn cups providing information about the occurred cooling of the sheets and associated phase transformations in comparison to the numerical predicted cooling of the blank. Thus finally a good accordance between the FE simulation and the experiment regarding the thermal conditions and the punch force stroke characteristic could be accomplished (compare Fig. 6). The discrepancy between the measured and the numerical calculated temperature characteristic of the blank at the edge area (Fig. 6 left hand side) can be related to different contact conditions between experiment and simulation due to thermal distortion of the positioned sheet. The difference in the maximum punch force values can be appointed to the properties of the bending enhanced membranes, which can only be used so far in AutoForm HotForm (version 4.1. alpha). In the following progress of this work the experimental-analytical-numerical procedure explained above for simplicity will be referred to as reference method 1.

3 Results and discussion

The friction coefficients displayed in Fig. 7 had been determined for an exemplarily tool temperature of 100°C and an initial specimen diameter d_0 of 90 mm after a heat treatment duration t_γ of 5 min at an austenitization temperature $T_\gamma = 950$°C. The results show on the one hand the influence or the sensibility of the parameters ε_{m1}, ε_{m2}, d_p and the temperature on the values of the calculated friction coefficient in comparison to the reference method 1, which follows the explained procedure before. On the other hand independently from the implemented values of ε_{m1}, ε_{m2}, d_p and temperature it can be noticed, that as expected hot forming leads to a significant increase of the friction coefficient in general compared to cold forming. This well-known effect can be related to various phenomena on the tribological interface between tool and workpiece like removing of fractures out of the oxide scale or the surface layer acting as a kind of lubricant, temperature induced adhesion forces, smoothing effects caused by elastic and plastic deformations of the surface as a result of the increased material's ductility etc. [16] Within the variation A of method 1 (M1-A) (compare Fig. 7) for the calculation of the mean true stress σ_{fm1} and σ_{fm2} the required respective strains have been determined via FEA. It can be clearly seen that M1-A leads to a value for the friction coefficient of approximately 0.45, which exhibits a very good accordance to the result of method 1 (M1). The geometrical assumptions applied in the reference method 1 could thus be approved by FE simulation. The significant difference of the results obtained by variation B of method 1 (M1-B) is caused by implementing the parameter d_p as a constant value according to the analytical relation $d_p \approx 0,79 \cdot d_0$. This should endorse the importance of considering the temperature dependency of d_p regarding its influence on the resulting magnitude of the calculated friction coefficient. According to Fig. 4 the impact is gaining more importance the higher the tool temperature is. The influence of neglecting the cooling of the sheets during the drawing operation is represented by the variation C (M1-C) of the reference method. Hereby the required true stresses respective to Eq. 1 had been approximated applying the average temperature in the flange just before forming takes place. It can be seen that the temperature and hence the resulting true stress values σ_{fm1} and σ_{fm2} have a significant influence on the following results. This effect is more aware the higher the difference between the blank and the tool temperature is, leading consequently to a more distinctive cooling of the sheets during the experiments. Beside the temperature dependent evolution of d_p this emphasizes the importance of the FEA providing the thermal conditions during the cup deep drawing test.

In [17] the friction coefficient of 0.47 determined according to the reference method 1 in Fig. 7 could be confirmed by inverse FEA of the hot cup deep drawing test using as well AutoForm HotForm. The version applied in [17] characterizes a newer test version of AutoForm HotForm, which offers the usage of shell elements, whereby a

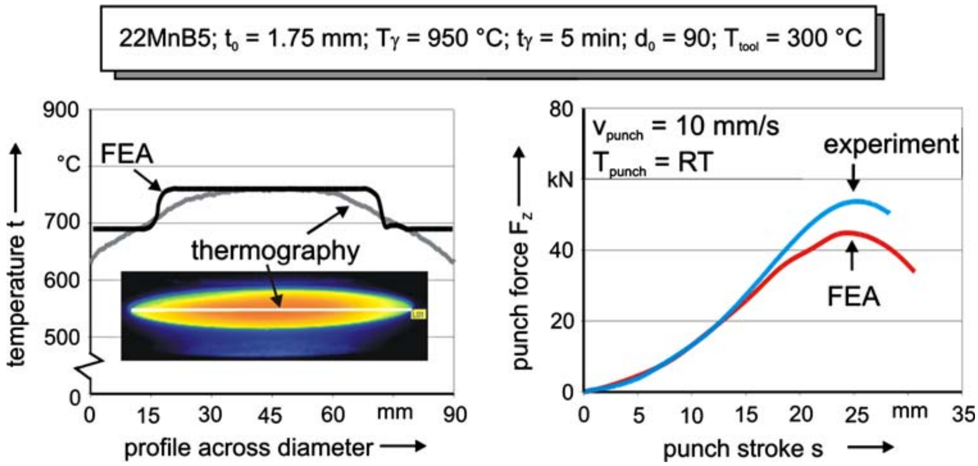

Fig. 6 Comparison between measured and via FEA predicted data regarding the temperature profile across the blank diameter instantaneous before the drawing operation (*left*) and the evolution of the drawing force as function of the punch stroke (*right*)

more accurate adjustment between the experimental and the by simulation predicted force stroke data could be performed. Within the scope of this work only the 4.1 alpha version of the FE tool limited to elastic bending membranes for setting up models was available. So an inverse comparison of the respective force stroke data could not be carried out as in [17]. For the further investigations on the tribological conditions within hot stamping method 1 was applied for the determination of the friction coefficients.

The influence of the tool temperature on the friction coefficient within hot stamping is shown in Fig. 8 for two initial blank diameters of 85 and 90 mm. It can be observed that with an increasing temperature of the die and the blank holder the values of the determined friction coefficients decrease significantly independent of the diameter. At a first view this effect appears to be different as expected from the literature. In order to find an explanation for this frictional behavior one has to point out some fundamentals first: in general if the temperature of a sliding material increases, so does the friction coefficient μ at the interface to its contact partner. But this is only valid if all the other circumstances affecting the tribological conditions and hereby mainly the normal force F_N exposed to metals sliding against each other remains constant. Then the increased ductility of the sliding material caused by a reduction of the material Young's Modulus E leads to an increase of real contact area $A_r \sim F_N/E$ due to elastic deformation of the asperities of the surface. If the resulting contact pressure exceeds the yield stress σ_0 of the material additional occurring plastic deformation of the asperities enforce the growth of $A_r \sim F_N/\sigma_0$ and the smoothing of the surface, respectively [18]. As known from the Amontons–Coulomb's friction law

$$F_R = \mu \cdot F_N \tag{6}$$

the friction force F_R and the respective friction coefficient μ is independent from the nominal contact area A_0 of the sliding partners, but only determined by the real contact area A_r. General speaking increasing the real

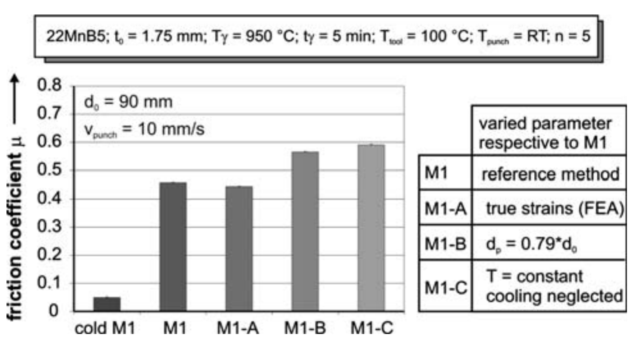

Fig. 7 Influence of varied values of ε_{m1}, ε_{m2}, d_p and the temperature on the calculated magnitude of the friction coefficient μ compared to the reference procedure M1

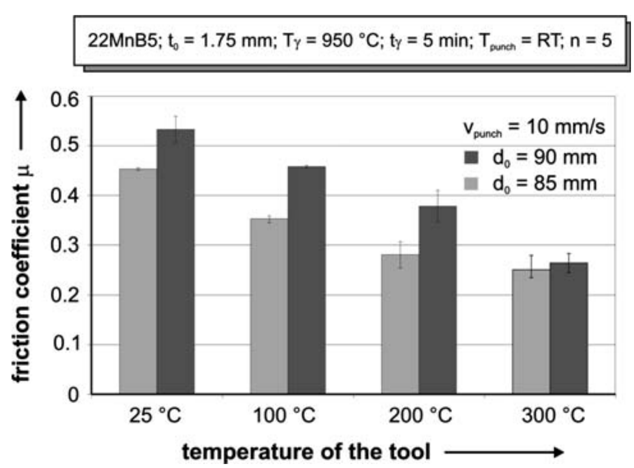

Fig. 8 Evolution of the friction coefficient μ in dependency of the tool temperature during the temperature of the punch remains at room temperature

contact area leads to an increase of the resulting friction force F_R. Thus, following Eq. 6 by assuming a constant load F_N it is obvious if the friction force increases, so does the respective friction coefficient μ. In the case of our investigations, increasing the temperature of the tool leads to a less distinctive cooling of the blank and thus to an increase of the ductility of it and its Al/Si coating, alloying to a ternary Al–Si–Fe system during austenitization, as well as the ductility of the tool material. Under the precondition of an unaltered normal load F_N interacting on the sliding partners compared to a tool temperature of 25°C this would lead to an increase of the real contact area A_r and consequently to an increase of the friction force and the friction coefficient, respectively (see Eq. 6). The further additional temperature induced adhesive processes are enforcing this effect. But on the other hand the significant temperature dependent plastic softening of the sheet material 22MnB5 (compare Fig. 2) leads to reduced normal forces transferred from the bulk sheet material to the interacting surfaces at the die, what could be approved by FEA. In contrary this effect balances the enhancement or even leading to a decrease of the real contact area ($A_r \sim F_N/E$) and thus to reduced friction forces.

The resulting friction coefficient μ is following according to Eq. 6 a result of the relation of the temperature dependent working friction and normal forces at the contact area.

$$\mu(T) = F_R(T)/F_N(T) \tag{7}$$

So increasing the temperature does not only affect the mechanical properties of the contact partners, its impact on the appearing contact forces has to be taken into account as well regarding the tribological conditions. With respect to the detected decrease of the friction coefficient with increasing tool and so the sheet temperature shown in Fig. 8 it can be assumed that the softening effect of the blank bulk material is probably more determent than the increase of the real contact area A_r caused by the ductility enhancement of the interacting surfaces. The discrepancy between the magnitudes of the friction coefficients obtained for an initial blank diameter of 85 and 90 mm in Fig. 8 is caused by the different cooling behavior of the specimens during the test. Thus at the point of maximum drawing force the specimens are characterized by dissimilar thermal conditions at the areas important for the calculation of the friction coefficient according to the evaluation method 1.

In Fig. 9 the within the scope of this work determined friction coefficients are plotted as function of the average blank temperature at die radius at the moment of maximum drawing force (compare Fig. 3). The dependency of the tribological characteristic μ on the sheet temperature can obviously be seen. For corresponding material

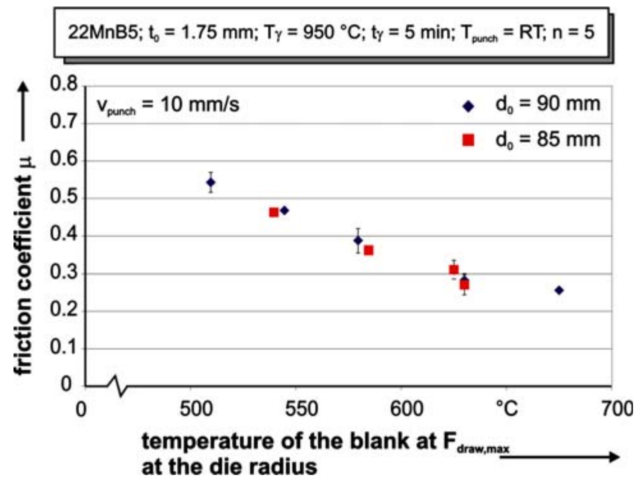

Fig. 9 Friction coefficient μ as function of the blank temperature in the contact area at the die radius at the moment of the maximum drawing force

temperatures comparable friction coefficients independent from the initial diameter or the heat treatment are obtained. The linear trend can be seen as indication that the real contact area and the resulting tribological conditions are mainly determined by elastic deformations. By Greenwood and Williamson [19] a linear relation between elastic deformation of flat surfaces and the resulting real contact area could be shown. With respect to the hot stamping process according to the shown results it can be concluded that increasing the forming temperature of the Al/Si pre-coated steel 22MnB5 in the austenitic state leads as well to an enhanced formability and reduced forming forces as to improved tribological conditions.

4 Summary and outlook

Within this paper a combined experimental-analytical-numerical method for characterization of the tribological conditions within hot stamping was presented. For the determination of the friction coefficient under process relevant conditions a modified cup deep drawing test setup was applied. The calculation of the friction coefficient was carried out according to Siebel's approach for the modeling of the maximum drawing force with neglected blank holder force. For the evaluation of the thermal conditions during the test within the closed die a Finite Element analysis was performed using AutoForm HotForm (version 4.1 alpha). Parameters mainly affecting the results of the described procedure had been discussed, whereby the temperature reveals to be the most significant one. Furthermore a significant dependency of blank temperature on the friction coefficient could be detected. With increasing sheet temperature at the interaction contact area decreasing friction values were observed. Due to the significant influence of a reliable numerical reproduction of the thermal conditions the determination of heat transfer coefficient for higher ambient temperatures are planned. The further the discovered trend for the temperature dependency of the friction coefficient will be analyzed for additional different hot stamping steels and coating systems as well.

References

1. Neugebauer R, Altan T, Geiger M, Kleiner M, Sterzing A (2006) Sheet metal forming at elevated temperatures. Ann CIRP 55(2):793–816
2. Steinhoff K, Maikranz-Valentin M, Weidig U, Paar U, Gücker E (2007) Bauteile mit maßgeschneiderten Eigenschaften durch neuartige thermo-mechanische Prozessstrategien in der Warmblechumformung. In: Geiger M, Merklein M (eds) Tagungsband 2. Erlanger Workshop Warmblechumformung, Erlangen, Deutschland. Maisenbach-Verlag, Bamberg, pp 1–12
3. Ultraleichte Automobil-Anbauteile aus Stahl (2000) Übersetzung des ULSAC Overview Report. 1. Auflage, ISSN 0175–2006
4. Hein P, Kefferstein R, Dahan Y (2006) Presshärten von USIBOR 1500P®: Simulationsbasierte Bauteil- und Prozessanalyse. In: Liewald M (ed) Neuere Entwicklungen in der Blechumformung, Stuttgart, pp 171–183
5. Merklein M, Lechler J, Geiger M (2006) Characterization of the flow properties of the quenchenable ultra high strength steel 22MnB5. Ann CIRP 55(1):229–236
6. Merklein M, Lechler J (2006) Investigation of the thermo-mechanical properties of hot stamping steels. In: Hartley P, Lin J, Jolly M, Pietrzyk M (eds) Tagungsband 11th international conference metal forming, Birmingham, England, pp 452–455
7. Merklein M, Lechler J, Geiger M (2007) Mathematical modeling of the flow behavior of hot stamping steels for a FE-based process design. In: Dimitri Dimitrov (Ed.) Tagungsband international conference on competitive manufacturing (COMA'07), Kapstadt, Südafrika, pp 191–196
8. Merklein M, Geiger M, Lechler J (2007) Ermittlung von thermischen und mechanischen Werkstoffeigenschaften höchstfester Vergütungsstähle für das Presshärten. In: Steinhoff K (Ed.) Tagungsband VDEh-Workshop: "Moderne thermomechanische Prozessstrategien in der Stahlumformung", Kassel, Deutschland, pp 24–36
9. Geiger M, Merklein M, Hecht J (2006) Investigations on formability and tribology with regard to warm forming of magnesium sheets. Ann Ger Acad Soc Prod Eng (WGP) XIII(2):101–104
10. Geiger M, Merklein M, Hoff C (2006) Determination of data for hot stamping. In: Proceedings of WGP (Hrsg.) production engineering. Annals of the German Academic Society for Production Engineering, vol XIII/2, pp 5–8
11. Siebel E, Beisswänger H (1955) Tiefziehen, München-Hanser
12. Witthüser K-P (1980) Untersuchungen von Prüfverfahren zur Beurteilung der Reibverhältnisse beim Tiefziehen. Dissertation, University of Hannover, Germany
13. Tolazzi M, Merklein M (2005) Determination of friction coefficients for the FE analysis of sheet forming of tailored welded blanks. J Adv Mater Res 6–8:313–320
14. Zünkler B (1973) Der Einfluss der Werkstoffverfestigung auf die Ziehkraft und das Grenzziehverhältnis beim Tiefziehen. Blech 20:343–346
15. Lange K (1990) Handbuch der Umformtechnik. Band 3: Blechbearbeitung. Springer, Berlin
16. Beyon JH (1998) Tribology of hot metal forming. Tribol Int 31:73–77
17. Kerausch M, Schönbach T (2007) FE-basierte Prozessauslegung mit Form-HotForming. In: Geiger M, Merklein M (Hrsg.) Proc. 2. Erlanger Workshop Warmblechumformung, Erlangen, Germany, pp 59–72
18. Czichos H (1978) Tribology—a systems approach to the science and technology of friction, lubrication and wear. Tribology Series 1. Elsevier-Verlag, Amsterdam
19. Greenwood J, Williamson J (1966) Contact of nominally flat surfaces. Proc R Soc Lond Band 295:300–319