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MULTI-SCALE FRICTION MODELING FOR MANUFACTURING PROCESSES: THE BOUNDARY LAYER REGIME

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ABSTRACT

This paper presents a multi-scale friction model for large-scale forming simulations. A friction framework has been developed including the effect of surface changes due to normal loading and straining the underlying bulk material. A fast and efficient translation from micro to macro modeling, based on stochastic methods, is incorporated to reduce the computational effort. Adhesion and ploughing effects have been accounted for to characterize friction conditions on the micro scale. A discrete model has been adopted which accounts for the formation of contact patches ploughing through the contacting material. To simulate metal forming processes a coupling has been made with an implicit Finite Element code. Simulations on a typical metal formed product shows a distribution of friction values. The modest increase in simulation time, compared to a standard Coulomb-based FE simulation, proves the numerical feasibility of the proposed method.

NOMENCLATURE

α Fraction of real contact area
 β Asperity radius
 β_v Linear hardening parameter

γ Internal energy factor
 ΔG_0 Activation energy
 ε Plastic strain
 $\dot{\varepsilon}$ Plastic strain rate
 ε_0 Initial plastic strain
 $\dot{\varepsilon}_0$ Reference plastic strain rate
 ζ Internal energy factor
 η Persistence parameter
 θ_{eff} Effective attack angle contact patch
 κ Asperity curvature
 λ Initial height of asperities
 μ Coefficient of friction
 ρ Asperity density
 σ_{dyn} Strain rate dependent stress
 σ_{f0} Initial static stress
 σ_{v0} Max. dynamic stress
 σ_{wh} Strain dependent stress
 σ_y Yield stress
 ϕ Normalized surface height distribution
 φ Main orientation elliptical paraboloid
 ψ Internal energy factor
 ω External energy factor
 ω_r Remobilization parameter
 a Major axis elliptical paraboloid
 A Area
 b Minor axis elliptical paraboloid

B Hardness parameter
 d Asperity indentation
 $d\sigma_m$ Stress increment parameter
 DC06 Cold rolled low carbon steel
 E Non-dimensional strain rate
 F_N Normal force
 F_w Shear force
 h Height elliptical paraboloid
 H Hardness
 k Boltzmann constant
 l Mean half spacing between asperities
 n Hardening exponent
 n_{asp} Number of asperities
 p Dynamic stress power
 P_{nom} Nominal contact pressure
 R_t Maximum peak to valley distance
 S Shear factor
 s# Rough surface nr. #
 T Temperature
 U Rise of asperities
 z Asperity height

INTRODUCTION

Finite Element (FE) simulations of sheet metal products are everyday practice in the automotive industry. The usability of such a code largely depends on the accuracy of the numerical models in these codes. An accurate forming analysis can only be made if, among others, the material behavior and friction conditions are modeled accurately. For material models, significant improvements have been made in the last decades, but in the majority of simulations still a simple Coulomb friction model is used. Consequently, it is still cumbersome to predict the draw-in and springback of a blank during forming processes correctly.

In the past, research has been carried out on contact and friction phenomena on different length scales. This paper focuses on the phenomena which play a role in the boundary lubrication regime, which is the most common condition during sheet metal forming. On the microscopic level, friction is due to the adhesion and ploughing effect between contacting asperities [1–4]. The real area of contact, defined as the area summation of contacting asperities, plays an important role in characterizing friction. The real area of contact depends on the roughness of both tool and workpiece, where the roughness of the workpiece is liable to changes due to flattening and roughening mechanisms. The main flattening mechanisms during sheet metal forming, which tends to increase the real area of contact, are flattening due to normal loading [4–8] and flattening due to combined normal loading and deforming the underlying bulk material [4, 9, 10]. Roughening of asperities, observed during deforming the bulk material without applying a normal load to the surface [11, 12], tends to decrease the real area of contact. The two mechanisms outlined

in this paper are flattening due to normal loading and flattening due to combined normal loading and stretching. Future work is planned on modeling the effect of combined normal loading and compressing the underlying bulk material, and the effect of unconstrained deformation of the bulk material on the real area of contact.

An advanced friction model is proposed which couples the most important friction mechanisms. Based on statistical parameters a fast and efficient translation from micro- to macro modeling is included. A newly developed flattening model, including work hardening effects, has been proposed to describe the increase of real contact area due to normal loading. Asperity flattening due to stretching has been described by the flattening model proposed by Westeneng [4] and the influence of ploughing and adhesion on the coefficient of friction has been described by the friction model of Challen & Oxley [2, 4]. A deterministic approach has been adopted to model ploughing conditions under high fractional contact areas. A brief overview of the friction model is presented and the translation from micro to macro modeling is outlined. Next, the theoretical background of the models used to describe the various friction mechanisms are briefly discussed. In the following section, the flattening models are validated by means of FE simulations on the micro-scale. Finally, the implementation of the advanced macroscopic friction model into FE codes is discussed.

THEORETICAL BACKGROUND

Numerical framework

A numerical friction framework has been developed to couple the various micro friction models. The friction model starts with defining the process variables and material characteristics. Process variables are the nominal contact pressure and strain in the material. Significant material characteristics are the hardness of the asperities and the surface properties of the tool and workpiece material. Once the input parameters are known, the real area of contact is calculated based on the models accounting for flattening due to normal loading and flattening due to stretching. The amount of indentation of the harder tool asperities into the softer workpiece asperities can be calculated if the real area of contact is known. After that, shear stresses due to ploughing and adhesion effects between asperities are calculated. Finally, by knowing the shear stresses and the nominal contact pressure subjected to the surface, the coefficient of friction can be obtained. It is noted that in reality flattening due to normal loading and flattening due to stretching will appear simultaneously during sheet metal forming, as well as the combination between flattening and sliding. Nevertheless, it has been assumed that the various mechanisms act independently of each other in this research.

FIGURE 1: MEASURED SURFACE TEXTURE

Characterization of rough surfaces

Friction models encompassing micro-mechanisms are generally regarded as too cumbersome to be used in large-scale FE simulations. Therefore, translation techniques are necessary to translate microscopic contact behavior to macroscopic contact behavior. Using stochastic methods, rough surfaces are described on the micro-scale by their statistical parameters (asperity density, mean radius of asperities and the asperity height distribution). Assuming that the surface height distribution on the micro-scale represents the surface texture on the macro-scale, it is possible to describe contact problems that occur during large-scale FE analyses of sheet metal forming processes.

Figure 1 shows a 3 dimensional roughness measurement of an electrical discharged textured (EDT) DC06-steel material. The location of asperities and the asperity density can be obtained by using the nine-point summit rule [13, 14]. Summits are points with a local surface height higher than their 8 neighboring points. Once the location of the asperities is known and assuming that asperities are spherically tipped, the radius of the asperities is related to the local curvature at the surface. The curvature κ is defined as the second order derivative of the function, which can be obtained by the second order finite difference method [14]. The expressions given in Equation 3 can be used to obtain the radius in two perpendicular directions ($\beta_{||}$ and $\beta_{\perp}$) and the equivalent radius β_{eq} . The expressions are based on the three point definition of a summit curvature in which $z_{x,y}$ represents the local surface height at the asperity location (x,y) .

FIGURE 2: SURFACE HEIGHT DISTRIBUTION

$$\kappa_{||} = \beta_{||}^{-1} = \frac{z_{x-1,y} - 2z_{x,y} + z_{x+1,y}}{dx^2} \quad (1)$$

$$\kappa_{\perp} = \beta_{\perp}^{-1} = \frac{z_{x,y-1} - 2z_{x,y} + z_{x,y+1}}{dy^2} \quad (2)$$

$$\kappa_{eq} = \beta_{eq}^{-1} = \frac{\kappa_{||} + \kappa_{\perp}}{2} \quad (3)$$

When using stochastic methods, only the mean radius of asperities is of interest:

$$\beta = \frac{1}{n_{asp}} \sum_i^{n_{asp}} \kappa_{eq,i}^{-1} \quad (4)$$

The histogram of all local asperities is called the asperity height distribution (Figure 2). To describe the histogram a continuous function is desirable to eliminate the need for integrating discrete functions during the solution procedure of the friction model. Various methods exist to describe discrete signals by continuous functions. The Gauss distribution function can be used if it is assumed that the surface height distribution is symmetric and approximates a normal distribution function. However, the initial surface height distribution is usually asymmetric and will become even more asymmetric if there is flattening of contacting and rising of non-contacting asperities. The asymmetric Weibull distribution function is a more flexible criterion but can only approximate smooth surface height distributions. A more advanced method to describe discrete signals can be achieved by using a Fourier series or by using B-splines. A Fourier series makes it possible to describe non-smooth asymmetric distribution functions from which the accuracy of the evaluation depends on the number of expansions used. Using B-splines, non-smooth asymmetric distribution functions can be evaluated from which the accuracy depends on the number of lines used to construct the curve. In Figure 2, the asperity height distribution corresponding to the measured surface roughness (Figure 1) is evaluated

FIGURE 3: ROUGH SOFT SURFACE FLATTENED BY A SMOOTH RIGID SURFACE

by a Gaussian, Weibull, Fourier and B-spline function. For the Fourier and B-spline function 10 Fourier expansions and 10 cubic lines were used, respectively. As can be seen, using a Gaussian or a Weibull function a large error in representing the histogram is introduced. A better evaluation can be obtained by using a Fourier series or a B-spline function, noting that the error could be reduced further by using more Fourier expansions or more lines to construct the B-spline. An advantage of the B-spline function, compared to the Fourier series, is that the derivative at the end points approaches zero, which can have a stabilizing effect in the friction algorithm. The Fourier series tends to oscillate towards the end points of the distribution when large tails are present, which represents unrealistic behavior and has a destabilizing effect on the friction algorithm. Concerning the flexibility of the B-spline function and the numerical stability of the friction algorithm, the B-spline function is favorable in describing complex distributions and will therefore be used in this research. The Weibull distribution function is favorable in case of normally distributed distributions.

Flattening models

Two flattening mechanisms have been implemented in the friction model to calculate the real area of contact of the workpiece: flattening due to normal loading and flattening due to stretching. A non-linear plastic load model has been developed inspired by the ideal-plastic load model proposed by Westeneng [4, 15]. Besides, Westeneng proposed an ideal-plastic stretching model [4, 15] which has been used in this research.

A rigid and perfectly flat tool is assumed which indents into a soft and rough workpiece material. This assumption is valid since the difference in hardness and length scales between the tool and workpiece material is significant in the case of sheet metal forming processes. The asperities of the rough surface are modeled by bars which can represent arbitrarily shaped asperi-

ties. Three stochastic variables are introduced: The normalized surface height distribution function of the rough surface $\phi(z)$, the uniform rise of the non-contacting surface U (based on volume conservation) and the separation between the tool surface and the mean plane of the asperities of the rough surface d , see Figure 3.

Contact between a flat hard smooth surface and a soft rough surface is assumed without sliding and bulk deformation. Only plastic deformation of asperities is assumed including work-hardening effects. Using the normalized surface height distribution $\phi(z)$, expressions to obtain the amount of flattening of contacting asperities d and the rise of non-contacting asperities U can be obtained by energy and volume conservation laws:

$$P_{nom} = \frac{B}{\rho \omega} (\gamma + \eta \zeta) + \frac{2\lambda S}{A_{nom}} \frac{\psi}{\omega} \quad (5)$$

$$U(1 - \alpha) = \int_{d-U}^{\infty} (z - d) \phi(z) dz \quad (6)$$

with:

$$\alpha = \int_{d-U}^{\infty} \phi(z) dz \quad (7)$$

ρ represents the asperity density, S a shear factor ($S = 1/\sqrt{3}$) following the Von Mises shear criterion) and λ the initial height of asperities. ω can be regarded as an external energy factor while γ , β and ψ can be regarded as internal energy factors: γ describes the energy required to indent asperities, β the energy

required to rise asperities and ψ the energy required to shear asperities which have a relative motion to each other. ω , as well as γ , β and ψ are variables which depend on the statistical parameters U (the constant rise of asperities) and d (the separation between the tool surface and the mean plane of the asperities of the rough surface). In addition, ω is a function of the normal forces acting on the asperities $F_N(z)$. It should be noted that an equal rise of asperities has been assumed in the derivation of ω , γ , β and ψ , which corresponds to the experimental results of Pullen & Williamson [8]. η represents the persistence parameter which describes the amount of energy required to lift up the non-contacting asperities. A value of $\eta = 0$ means that no energy is required to rise the asperities, a value of $\eta = 1$ implies that a maximum amount of energy is required to rise the asperities. Known parameters in Equation 5 are P_{nom} , the nominal contact pressure (input parameter), and B , a hardness parameter. Since non-linear plasticity is assumed, the hardness H of the softer material can be described by $H = B\sigma_y$ (with $B=2.8$ for steel materials). The yield strength σ_y can be described by a flow rule which analytically describes the relation between the strain in the material and the yield strength of the material. The physically based isothermal Bergström van Liempt [16–18] hardening relation is used in this research. This relation decomposes the yield stress σ_y in a strain dependent stress σ_{wh} and a strain-rate dependent stress σ_{dyn} . The relation accounts for the interaction processes between dislocations in cell structures including the changing shape of dislocations. Vegter [19] modified the Bergström van Liempt hardening relation for sheet metal forming processes, leading to the following formulation:

$$\sigma_y = \sigma_{wh} + \sigma_{dyn} \quad (8)$$

with

$$\sigma_{wh} = \sigma_{f0} + d\sigma_m (\beta_v (\varepsilon + \varepsilon_0) + \{1 - \exp[-\omega_r (\varepsilon + \varepsilon_0)]\}^n) \quad (9)$$

and

$$\sigma_{dyn} = +\sigma_{v0} \left(1 + \frac{kT}{\Delta G_0} \ln \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \right)^p \quad (10)$$

Typical values for DC06 steel material and a nomenclature of the hardening parameters can be found in Appendix A. The strain in the asperities is defined as the amount of indentation or rise of asperities relative to the initial height of the asperities λ . In this respect, a definition for the strain ε can be derived for 1) asperities in contact with the indenter or asperities which will come into contact due to the rise of asperities and 2) asperities

which will not come into contact with the indenter:

$$\varepsilon = \begin{cases} \ln \left(\frac{\lambda + d - z}{\lambda} \right) & \text{for } d - U \leq z \\ \ln \left(\frac{\lambda + U}{\lambda} \right) & \text{for } z \leq d - U \end{cases} \quad (11)$$

The model described above is based on a normal loading case without additional bulk strain. To account for flattening due to stretching, the model has to be adapted. The change of the fraction of the real contact area as a function of the nominal strain can be presented as:

$$\frac{d\alpha_S^i}{d\varepsilon} = \frac{l}{E} \phi (d_S^{i-1} - U_S^{i-1}) \quad (12)$$

with i the iteration number. The subscript S is used for variables that become strain dependent. The contact area ratio is updated incrementally by:

$$\alpha_S^i = \alpha_S^{i-1} + d\alpha_S^i \quad (13)$$

The initial values α_S^0 , d_S^0 and U_S^0 are obtained from the model without bulk strain. To calculate the change of α_S , the value of U_S and d_S needs to be solved simultaneously while ε is incrementally increased. Based on volume conservation and the definition of the fraction of real contact area (Equation 14) U_S and d_S can be obtained.

$$\alpha_S = \int_{d_S - U_S}^{\infty} \phi(z) dz \quad U_S (1 - \alpha_S) = \int_{d_S - U_S}^{\infty} (z - d_S) \phi(z) dz \quad (14)$$

Shear stresses

The model of Challen & Oxley [2, 3] takes the combining effect of ploughing and adhesion between a wedge-shaped asperity and a flat surface into account. Westeneng [4] extended the model of Challen & Oxley to describe friction conditions between a flat workpiece material and multiple tool asperities. For this purpose, statistical parameters (asperity height distribution, asperity density and mean radii) have been used to make the translation from single asperity scale to multiple asperity scale [15]. However, statistically based contact models tends to lose its applicability under fully plastic contact conditions. Under high fractional contact areas asperities are joining together, thereby forming contact patches penetrating into the softer workpiece material [20]. The frictional behavior of the contacting surfaces now depend on the geometry of the contact patches, rather

FIGURE 4: (A-C) INDENTATION TOOL ASPERITIES

than on the geometry of the individual asperities. Ma [21] proposed a multi-scale friction model which account for asperities forming contact patches under high fractional contact areas, see Figure 4. The deterministic approach of Ma excludes the use of statistical parameters, which implicitly excludes the scale dependency problem when describing rough surfaces by its statistical parameters.

The ‘macro-scale’ model of Ma has been implemented in the friction model to describe friction conditions between the tool and workpiece material. In this respect, the statistical based approach to calculate the deformation of workpiece asperities, as presented in the previous section, is coupled to the discrete contact model of Ma. The model of Ma is based on the projection of two rough surfaces onto each other. These surfaces can be experimentally measured or digitally generated. The surface height matrix of the workpiece material is adapted for the amount of flattening and rise of asperities, which follows from the statistically based flattening models. The plateaus of the flattened workpiece asperities are assumed to be perfectly flat, in which the harder tool asperities are indenting. The separation between the mean plane of the tool surface and the flattened peaks of the workpiece surface is calculated based on force equilibrium, obtained by the summation of the load carried by the formed contact patches.

Contact patches are observed by binary image processing techniques, which identifies a contact patch when a predefined number of connected pixels are indenting into the projected surface. To determine the attack angle of the contact patch which ploughs through the softer workpiece material, an elliptical paraboloid is fitted through the height data of the contact patch. The base of the paraboloid is fitted by an ellipse having the same area as the contact patch. The height of the paraboloid is determined by equating the volume of the indented contact patch by the volume of the elliptical paraboloid. The geometrical characteristics of this equivalent contact patch are indicated in Figure 4c.

The contact model of Ma has been coupled to Challen & Oxley’s friction model to calculate friction forces acting on individual contact patches. An effective attack angle, in the direction of the sliding velocity, should be determined since the model of Challen & Oxley is based on a plain strain assumption. A relation for the effective attack angle has been proposed in [21], taking into account the 3-D nature of the contact patch:

$$\theta_{eff} = \arctan \frac{2h\sqrt{b^2 \cos^2 \varphi + a^2 \sin^2 \varphi}}{\chi ab} \quad (15)$$

in which a shape factor χ has been introduced [22]. Since the 3-D nature of the contact patch is captured by this expression, anisotropic surfaces can be handled as well. Knowing the effective attack angle of each contact patch, the total friction force becomes the summation of all individual contributions. The coefficient of friction is finally obtained by dividing the total friction force by the total load carried by the contact patches:

$$\mu = \frac{F_w}{F_N} = \frac{\sum_{i=1}^m \mu_i(\theta_{eff}) A_i H}{\sum_{i=1}^m A_i H} \quad (16)$$

with m the number of contact patches and μ_i the friction force of a single contact patch according to Challen & Oxley.

VALIDATION

The newly developed non-linear load model as well as the ideal-plastic strain model of Westeneng have been validated by means of FE simulations on a 2-D rough surface. In the first analysis, a 2-D rough surface of 4mm long was deformed by a perfectly flat and rigid tool. The second analysis was focused on

FIGURE 5: REAL CONTACT AREA CASE 1

flattening a rough surface by a normal load including a bulk strain in the underlying material. Three simulations have been executed for each analysis case using different roughness profiles (s1, s2 and s3). The roughness profiles equal three roughness measurements of DC06 steel material. The surface was modeled by 4 node 2D plane-strain elements. The yield surface was described by the Von Mises yield criterion using the Bergström van Liempt hardening relation to describe work-hardening effects (Equation 8). The surface height distribution used for the analytical model corresponds to the roughness distribution of the FE simulations. The development of the real area of contact has been tracked during the simulation and compared with the analytical solution. Results are shown in Figure 5 and 6 for the first and second analysis case, respectively.

Two unknown parameters have been introduced in the non-linear loading model: 1) the persistence parameter η which describes the amount of energy required to lift up non-contacting asperities and 2) the initial height of asperities λ required to calculate shear stresses and work-hardening effects of the deforming asperities. The values of these parameters have been determined by equalizing the analytical results with the FE results of surface 1 (s1), i.e. minimizing the error between the results. A value of $\eta = 0$ means that no energy is required to lift up non-contacting asperities, a value of $\eta = 1$ implies that the same energy is required to lift up asperities as to indent asperities. For this purpose the persistence parameter has been fixed to a value of 0.5 and the initial height of asperities λ has been adopted to minimize the error between the analytical solution and the results obtained by the FE simulation. Using a value of $\lambda = 4R_t$ the exact development of real contact area can be found. The R_t value represents the maximum peak to valley distance between asperities. The amount of strain build up in the asperities, and therefore work-hardening effects, will be lower when using a higher value for the initial height λ . The obtained values have been used to analyze the development of real contact area of the two other sur-

FIGURE 6: REAL CONTACT AREA CASE 2

faces (s2 and s3). As shown in Figure 5, the mean error between the analytical solution and the results obtained by the non-linear plastic FE simulation is less than 10% for both surfaces. Representing an acceptable error regarding the assumptions made in the statistically based analytical model.

Combined normal loading and stretching the underlying bulk material decreases the effective hardness [9]. A lower hardness results in an increase of the real area of contact. Both the analytical and the FE results of analysis 2 are presented in Figure 6, where a rough surface has been flattened by a nominal load and a bulk strain has been applied to the underlying material. As for analysis case 1, simulations have been performed on three different rough surfaces indicated by s1, s2 and s3 respectively.

Results obtained by the analytical strain model shows the same trend as the FE results (Figure 6), however the development of the real area of contact is significantly higher. The difference could be subjected to the scale dependency problem of surface statistics and the expression used for the non-dimensional strain rate (E in Equation 12). The expression proposed by Sutcliffe has been used to describe the non-dimensional strain rate which is based on ideal-plastic material behavior. An overestimation is expected when describing non-linear material behavior using this model. Another issue is the scale dependency problem of surface statistics: other magnification factors of the measurement device will lead to other surface statistics. For this reason, the asperity density for each individual workpiece surface has been adapted until satisfactory results were obtained, i.e. minimizing the error between the analytical solution and the FE results. As shown in Figure 6, the FE results can be described well by using an asperity density of $2E5 \text{ asp/mm}^2$, $2E5 \text{ asp/mm}^2$ and $4E5 \text{ asp/mm}^2$ for surface 1, 2 and 3 respectively. Using these artificial values the mean error between the analytical solution and results obtained by the FE simulations does not exceed 10%, an acceptable limit as mentioned earlier. It should be noted that the values chosen for the asperity density are completely arbitrary and lies outside the

FIGURE 7: DEVELOPMENT REAL CONTACT AREA

physical region. Other, more advanced, models are required to describe the influence of bulk straining effects more accurately.

IMPLEMENTATION

The developed friction model has been coupled to the in-house implicit FE code Dieka, developed at the University of Twente. The coupling has been realized by initializing the coefficient of friction for a predefined range of process variables (constructing a friction matrix), after which an interpolation scheme is used to find nodal friction values. The interpolation scheme is called if a node of the workpiece comes in contact with the tool, resulting a friction coefficient belonging to that specific node.

Process variables are the nominal contact pressure and the bulk strain in the workpiece material. Since a rough guess can be made about the range of these variables a matrix can be constructed including friction values for all possible combinations. Figure 8 shows the friction matrix for DC06-steel for a nominal contact pressure in between 0 and 50 MPa and a bulk strain in between 0 and 10%. The friction model proposed in this paper has been used to construct the matrix. Values for the persistence parameter, initial height of asperities and the asperity density are used from the Validation Section. Friction values are lying within the physical region: in between 0.13 and 0.23. The coefficient of friction decreases for increasing pressure and for increasing strain. The evolution of friction values highly depends on the development of the real contact area and the surface properties of both workpiece and tool material. Figure 7 shows the development of the fraction of real contact area to emphasize its influence on the coefficient of friction. Asperities will flatten due to normal loading as described in the Section Flattening models, which increases the fraction of real contact area. The effective hardness of the bulk material will reduce due to combined nor-

FIGURE 8: DEVELOPMENT COEFFICIENT OF FRICTION

mal loading and straining, having a significant influence on the fraction of real contact area. Hence, a lower hardness result in an increase in real contact area. An increase in real contact area decreases the coefficient of friction, following from the proposed friction model in the Section Shear stresses: A higher real contact area decreases the effective attack angle of a contact patch and the number of active contact patches, resulting in lower friction values.

APPLICATION

A cross-die product is used to test the numerical performance of the developed friction model in a large-scale FE simulation (Figure 9). Due to symmetry only a quarter of the workpiece was modeled. The workpiece was meshed with 9000 triangular Discrete Kirchhoff shell elements using 3 integration points in plane and 5 integration points in thickness direction. The coefficient of friction used in the contact algorithm was calculated on the basis of the friction model presented in this paper. For this purpose, the equivalent plastic strain has been used as a strain measure, which treats tensile and compressive strains equivalently. A distribution of friction coefficients can be observed from the results presented in Figure 9. Values of the friction coefficients are found ranging from 0.13 to 0.20. The gray area represents the non-contacting area.

It can be observed from Figure 9 that lower values of the coefficient of friction occur at regions where high strains occur (region A, B and C). Region A is purely stretched, region B is compressed which causes thickening of the material and region C is stretched over the die radius. Higher values are found at regions where low strains/ low pressures occur, such as the area clamped in between the blankholder and lower die. Overall it can be concluded that the distribution of the coefficients of friction

FIGURE 9: COEFFICIENT OF FRICTION CROSS-DIE

lies within the range of expectation.

The draw-in pattern of the simulation is compared to the simulation result in which the standard Coulomb friction model has been used with a friction coefficient of 0.13 (Figure 10). It can be observed that the draw-in significantly deviates from the draw-in obtained with the Coulomb friction model. This is logical since the maximum obtained friction coefficient by the developed friction model is much higher than the fixed value of 0.13. When a fixed value of 0.20 is used, which is the maximum value found when using the developed friction model, failure of the cross-die will occur. Comparing FE computation times of both friction models only an increase of 1% was observed, which shows the numerical feasibility of the proposed method in combination with FEM.

CONCLUSIONS

A friction framework, to be used for modeling large-scale sheet metal forming processes, is presented. The friction framework include models to describe the two dominating flattening mechanisms during sheet metal forming operations: asperity flattening due to normal loading and flattening due to stretching. Statistically based models are used for this purpose. The real area of contact is used to determine the influence of ploughing and adhesion effects between contacting asperities on the coefficient of friction. A coupling has been made between a deterministic contact model, which determines the effective attack angle by the formation of contact patches, and the well-known friction model proposed by Challen & Oxley.

The friction model has been validated by means of FE simulations at a micro-scale. A good comparison was found between the FE simulations and the results obtained by the newly developed non-linear loading model. It has been shown that the

FIGURE 10: FLANGE DRAW-IN

non-linear load model can be used to describe non-linear plastic material behavior. If a nominal strain is applied to the bulk material, the effect of work-hardening becomes significant. The ideal-plastic strain model is able to describe the trend of the FE results, but an accurate prediction of the real contact area could not be made with realistic values of the asperity density. However, it is possible to tune the analytical model to the elastic non-linear plastic FE simulations using unrealistic values. Other, more advanced, models are required to accurately describe the influence of bulk straining on the flattening behavior of asperities on a more physical basis.

The friction model has been applied to a full-scale sheet metal forming simulation to test the numerical performance and feasibility of the developed friction model. The results are very promising. The modest increase in simulation time proves the feasibility of the friction model in large scale sheet metal forming simulations.

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Appendix A

See Table 1 for DC06 Bergström van Liempt hardening parameters.

TABLE 1: HARDENING PARAMETERS

Material parameter	Value	Unit
Initial static stress (σ_{f0})	101.47	MPa
Stress increment parameter ($d\sigma_m$)	251.83	MPa
Linear hardening parameter (β_v)	0.5	
Remobilization parameter (ω)	9.951	
Hardening exponent (n)	0.75	
Initial strain (ϵ_0)	0.005	
Max. dynamic stress (σ_{v0})	600	MPa
Temperature (t)	300	K
Dynamic stress power (p)	2.2	
Activation energy (ΔG_0)	0.8	eV
Reference strain rate ($\dot{\epsilon}_0$)	10^8	s^{-1}
Boltzmann constant (k)	$8.617 \cdot 10^{-5}$	eV