Pressure and sliding velocity dependent surface asperity based friction model: Application to springback simulation

K J Lee¹ and M G LEE¹, *
Department of Materials Science and Engineering and RIAM, Seoul National University, 1 Gwanak ro, Gwanak-Gu, Seoul 151-744, South Korea

* Corresponding author: myounglee@snu.ac.kr

Abstract. For accurate predictions of the formability and springback in the finite element sheet forming simulations, significant advances have been achieved in constitutive modeling that captures complex deformations occurring in the sheet forming process. However, friction behavior has been still modeled and implemented as a simple way though it is one of critical factors for accurate numerical simulations. For instance, sheet metal forming simulations often employ the Coulomb friction law with a constant friction coefficient, but it is known to be a function of other factors such as contact pressure, sliding rate and other surface quality. In this study, the microscopic surface asperity based friction model originally proposed by Westeneng [1], as one of microscale level friction models, was revisited by using additional contact assumptions. In particular, the model added the strain rate effect to the existing contact pressure dependent model in order to include the effect of both contact pressure and sliding velocity during sheet forming process. The calculated contact friction coefficient was compared to that by the measured for dual-phase 780 steel sheet under different pressure and sliding velocity. Moreover, the predicted friction model was employed in the simulation of U-draw bending springback by using a user friction subroutine, which was evaluated by the comparison of experimentally measured springback profile.

1. Introduction
Finite element (FE) simulations have been commonly used to optimize process and product in sheet metal forming for automotive parts. In order to improve the accuracy of FE simulations in sheet metal forming process, implementation of associated constitutive models such as yield function and hardening law has been regarded as one of the most critical factors along with robust numerical algorithm for stress integration of the constitutive models. However, in most numerical simulations, the frictional behavior between tools and sheet metals have been modeled using a simple Coulomb friction law that assumes a constant friction coefficient regardless of change in contact condition during forming process. It is well known that the friction coefficient highly depends on many other factors such as lubricant properties, contact pressure, surface quality and environment [2]. Therefore, appropriate explanation for the friction behavior and corresponding modeling is required for precise modeling of sheet metal forming process. There have been various approaches that could be classified into phenomenological or micromechanical approaches to model the friction law for sheet metal forming simulations. In the phenomenological approach, mathematical equations are derived based on experimental results in friction tests under different contact conditions. For example, friction coefficients for different sliding velocities and contact pressures were measured and a simple function
in terms of the sliding velocity and contact pressure was proposed by Lee et al [3]. This approach describes the actual friction coefficient measured by the experiment, but requires a large number of tests for limited contact conditions. On the other hand, the basic mechanism for interaction between contact surface and sheet metal is considered in the micromechanical approach. The plowing effect, a localized plastic deformation of the asperities, is a major mechanism under high contact pressure [4]. This mechanism is implemented in a micromechanical model to derive friction coefficient [1].

In the present study, the microscopic surface asperity based contact friction model proposed by Westeneng [1] was revisited by using additional contact assumptions. For this, the strain rate effect on the surface was included. Though lubricant flow between contact surfaces is generally described by three lubrication regimes, this study considers only the boundary lubrication in the significant contact regions such as tool corners. For the validation purpose, the predicted friction coefficient was implemented in the simulation of U-draw bending springback using a user subroutine of a commercial FE software and its profile was compared with experimentally measured value. Note that the present model is a modified version of existing friction model which includes the effect of contact pressure [5].

2. Micro-mechanical friction model: previous works
The idea of the friction model proposed by Westeneng [1] is based on the concepts of flattening and plowing. In the flattening model, as a workpiece is significantly rougher and softer than those of tool in the typical sheet metal forming process, tools are assumed as perfectly rigid and flat. In contrast, the asperities of the rougher workpiece are modeled by a group of (rectangular) bars consisting of different heights, which represents surface height distribution function as shown in Figure 1. When contact occurs due to normal pressure, asperities are indented under the assumption that energy and volume conservation laws are satisfied. In addition, the sliding and bulk strain effect which occur during sheet metal forming greatly affects the real contact area [8]. In this study, the only sliding effect is considered and the bulk strain effect will be included in our future work.

The plowing effect is dominant to account for friction in the metal forming process. The model of Challeng and Oxley [6, 7] derived the friction coefficient equation as a single asperity, taking into account the combined effect of the plow and the adhesive between the rounded asperity and flat surface. Westeneng [1] adopted these model to illustrate the friction behavior between a flat workpiece and several tool asperities as following.

\[ F_w = \rho_l \alpha A_{nom} \int_{0}^{\omega} f_{asp} (\omega) \phi_s (s) ds \]  

with \( \omega \) the indentation length, \( \rho_l \) the asperity density of the tool summits, \( A_{nom} \) the nominal contact area, \( \phi_s \) the normalized surface height distribution function of the tool summits, \( f_{asp} \) the frictional force at single asperity scale [7], and \( F_w \) the total friction force. The bounds of the integral are expressed as
s_{max}, the maximum height of tool summit, and \( \delta \), the separation between the mean plane of the tool summits and the flat workpiece surface. The friction coefficient can be obtained from

\[
\mu = \frac{F_n}{F_N} = \frac{\rho \alpha A_{nom} \int_{\delta}^{s_{max}} f_{asp}(\omega) \phi(s) ds}{p_{nom} A_{nom}} = \frac{\rho \int_{\delta}^{s_{max}} f_{asp}(\omega) \phi(s) ds}{p_{nom}}
\]

where \( F_N \) is the normal contact force, and \( p_{nom} \) is the nominal contact pressure. More details on the friction model proposed by Westeneng can be referred to [1, 8]. \( \delta \) is a critical factor in determining the friction coefficient.

The indentation depth of the tool summit asperities is a constant value with increasing contact pressure in Westeneng friction model, but in our study [5] the force equilibrium between the applied external force (\( p_{nom} A_{nom} \)) and indented force (\( A_{asp} H \)) was considered as illustrated Figure 2. Therefore, a new relationship determining \( \delta \) was proposed.

\[
F_N = p_{nom} A_{nom} = \rho \alpha A_{nom} \int_{\delta}^{s_{max}} A_{asp}(\omega) H \phi(s) ds = \rho \alpha A_{nom} \int_{\delta}^{s_{max}} F_{N_{asp}}(\omega) \phi(s) ds
\]

where \( A_{asp} \) is the area of each tool summit, \( F_{N_{asp}} \) is the normal force of each tool summit, and \( H \) is the hardness of the workpiece. Approximately, \( H \) is given by \( 2.8 \sigma_y \) where \( \sigma_y \) is the yield stress [9]. To consider work hardening effect of tool asperities, it is assumed that \( \sigma_y \) is flow stress and function of plastic strain [1]. \( \delta \) is iteratively determined by solving the above equation. The relationship between \( \delta \) and contact pressure is shown in Figure 3(a).

**Figure 2.** Schematic view on force equilibrium between the external force and total indented force by tool summit asperities [5]

**Figure 3.** (a) The magnitude of indentation of the tool summit asperities as a function of contact pressure and (b) experiment data of friction coefficient and friction coefficient with modified friction model as a function of the nominal pressure for TRIP780 [3].
3. Effect of sliding velocity on the friction coefficient

Sliding velocity is another factor affecting the friction behavior [3,10]. Lee et al. [3] derived the mathematical equation based on the friction results under different sliding velocity. Molinari et al. [10] and List [11] proposed the friction model which assumes that the shear stress between the junction of rough surfaces is dominant for friction. In this study, it is assumed that the sliding velocity influences the strain rate dependent response of surface deformation in workpiece. For example, when the strain rate increases during plastic deformation, flow stress and hardness become larger in most common structure metals. Then, less indentation of tool summit asperities occurs and affects the plowing effect. Therefore, the plowing effect, which dominates the friction behavior, is affected by sliding velocity. The flow stress is a function of plastic strain rate and the hardness is also its function. Thus, equation (3) can be transformed into an equation involving the strain rate effect as follows. The detailed derivation is omitted here but will be discussed in our future work.

\[
F_N = p_{nom} A_{nom} = \rho \alpha A_{nom} \int_{\delta} A_{ug} (\omega) H (\dot{\varepsilon}) \phi (s) \, ds = \rho \alpha A_{nom} \int_{\delta} F_{ug} (\omega, \dot{\varepsilon}) \phi (s) \, ds
\]

(4)

Figure 4 shows the effect of sliding velocity on the calculated friction coefficient but its effect is not much pronounced because of less sensitivity of TRIP780 steel on the strain rate [12]. Therefore, only the effect of friction under different the contact pressure was compared in the present study. For more validation of the friction model, materials with large strain rate dependency, such as mild steel, will be applied to the friction model in future work.

4. Validation with finite element simulations of U-draw/bending springback

To validate the calculated friction model, a FE model of U-draw/bending was constructed in Abaqus/Standard. The geometry of tool follows Numisheet'93 benchmark [13], which is shown in figure 5. To compare only the friction effect on U-draw/bending, advanced constitutive models, such as Yld2000-2d [14], HAH anisotropic hardening model [15], were implemented. The blank holding force was 70kN. As introduced in the previous section, the influence of sliding velocity on friction behavior for TRIP780 can be negligible. Thus, calculated friction coefficient by the modified friction
model is not much different compared to that by only contact pressure dependent model. Figure 6(a) is schematic view of springback process after U-draw/bending.

![Figure 5. U-draw/bending tool geometry](image)

![Figure 6. (a) Schematic view of U-draw bending springback and (b) comparison of springback profiles between experiment and calculations](image)

Springback predictions with modified friction model and Coulomb friction law with commonly used constant friction coefficients were compared with experimentally measured profile in Figure 6(b). For the constant friction model, friction coefficients $\mu=0.1$ and 0.15 were selected because the friction coefficients are commonly suggested value for the simulation of springback. In fact, in the Numisheet benchmark, the friction coefficient of 0.1 was suggested. As shown in Figure 6(b), the calculated springback profile with variable friction model as a function of contact pressure and sliding velocity matches very well with experiment, but those with constant coefficients shows deviations, especially at the sidewall curl region.

5. Conclusions
In this study, microscale asperity based friction model was used for the simulation of springback. In the present friction model, the distance between the mean plane of tool summits and workpiece surface was iteratively determined by applying force equilibrium between externally applied pressure and reaction forces of contacted tool asperities. Moreover, the effect of sliding velocity on the frictional behavior was added in the previous model. The model was applied to the U-draw bending springback simulation proposed in the Numisheet benchmark problem, and validated by comparing the calculated
springback profile with experimental value. The springback profile predicted with the proposed friction model showed better agreement with experiment than that with constant friction coefficient.

Acknowledgment
This work has been supported by National Research Foundation of Korea (NRF-2017R1A2A2A05069619 and NRF-2014R1A2A1A11052889).

References
[1] Westeneng J 2001 *Modeling of contact and friction in deep drawing processes* Ph.D. thesis University of Twente
[2] Keum Y T, Wagoner R H and Lee J K 2004 *AIP Conf. Proc.* 712 989
[3] Lee J Y, Barlat F and Lee M G 2015 *Int. J. Plast.* 2015 113
[4] Greenwood J A and Williamson J B P 1966 *A Math. Phys. Sci.* 295 300
[5] Lee K J and Lee M G 2018 *J. Phys.: Conf. Ser.* 1063 012140
[6] Hol J, Alfaro M V C, de Rooij M B and Meinders T 2012 *Wear* 286 66
[7] Challen J and Oxley P 1979 *Wear* 53 229
[8] Challen J and Oxley P 1984 *Int. J. Mech. Sci.* 44 103
[9] Tabor D 1959 *Proc. Royal Soc. Lond.* 251 378
[10] Molinari A, Estrin Y and Mercier S 1999 *J. Tribol.* 121 35
[11] List G, Sutter G, Arnoux J J and Molinari A 2015 *Mech. Mater.* 80 246
[12] Rahmaan T, Bardelcik A, Imbert J, Butcher C and Worswick M J 2016 *Int. J. Impact. Eng.* 88 72
[13] Taylor L, Cao J, Karafillis A P, Boyce M C 1995 *J Mater Process Tech* 50 168
[14] Barlat F, Yoon J Y, Chung K, Dick R E, Lege D J, Pourbogharat F, Choi S H and Chu E 2003 *Int. J. Plast.* 19 1297
[15] Lee J, Lee J Y, Barlat F, Wagoner R H, Chung K and Lee M G 2013 *Int. J. Plast.* 45 140