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A Contact Friction Modelling at Hot and Cold Rolling

1. Introduction

An important part in the morden theory of plasticity take the simulation models for plastic forming processes used in computer aided design. The subject of our investigation is rolling of elements with rectangular cross-section. Very often this is a wide workpiece at which the rolling is modelled as a deformational process at a plane strain condition. In the case of a narrow and tall workpiece (as slabs and etc.), it is necessary to account the space effects across to the rolling direction. One of the important items in creating simulation models is the adequate friction modelling viaroll-workpiece interface at hot or cold rolling. This consideration is based on the fact that the rolling process is due to the friction and that is the reason for many authors' attempt to creat a resonable friction law model [3] [8]. The most widely used models are the classical ones as Coulomb and Zibel friction models. The straight application of such simple models of contact friction leads to significant differences in experimental results. Recently there grew up an interest in creating contact friction laws describing in a more realistic way the physics and the mechanics of the fenomena. Coulomb model describes dry or quasidry friction when overcoming the asperities between the contact surface of roll and material.

Dry friction is a process which is observed mainly in case of coldrolling without oil in the roll-gape. Quasidry friction is observed in case of hot rolling where exists an oxide layer playing the role of oil. The process of plastic friction is observed in both cases when the material subsequently sticks to the roll contact surface and cutting occurs in the plastically deformed subcontact layer. The acceptance of only one of this mechanisms is a strong simplification. An attempt to include these two types of friction is made by (F. P. B o w d e n, D T a b o r) [8]. The authors have offered a simple model in which the friction force is a result of a direct summation of the resistant force against polishing the surface asperities (Coulomb friction law) and the resistant cutting force inside the subcontact layer (Zibel friction law). The coupling of both mechanisms is not taken into account in this model as well as the dependence of the resistant friction force on the deformation process factors (plastic deformation, strainrate, temperature, etc.).

In [1] the authors of the present paper offer a model of the Zibel friction law in which these effects are taken into account. In the present paper we have proposed a generalised model of the Zibel friction law.

2. On the friction modelling at rolling

The conact zone, according to the concept of the mutual action ofboth friction mechanisms, can be devided in the following way (see Fig.1):

1) roll material; 2) roll contact surface - S_{roll}; 3) workpiece conact surface - $S_{(c)}$; 4) contact layer containing the asperities that lead to Coulomb friction; 5) dividing surface between the contact and subcontact layer; 6) subcontact layer; 7) surface separating the subcontact layer and the material in the volume; 8) workpiece material.

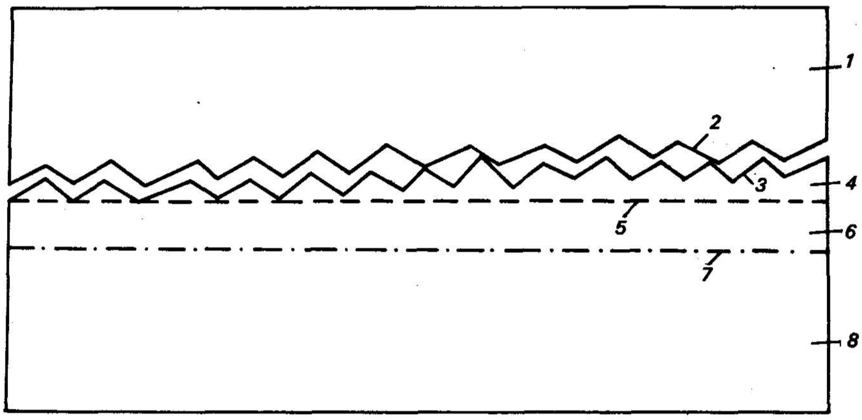

Fig. 1

The friction mechanisms are realized in both layers 4) and 6) which are important for our considaration. The two layers determine the ralation between the friction mechanisms acting in them. We take the Zibel friction law,and we give this law a new meaning. It's classical form is:

$$\tau_F = m\,\tau_p\,, \tag{1}$$

where τ_F is a limit stress of friction; τ_p is the pure shear yield limit; m - a constant friction factor, $m \in [0,1]$. According to the model proposed, there sistance to cutting $\tau^{res}_{(c)}$ acts at the subcontact layer. It will be a part n^{res} of subcontact layers yield limit $\tau_{p(c)}$.

$$\tau^{res}_{(c)} = n^{res}\,\tau_{p(c)}\,. \tag{2}$$

In general n^{res} will depend on the values of the subcontact layer process factors. Our aim is to achieve a reasonable model, which takes into account the major effects, possibly in the simplest way. We accept n^{res}= const., as average value experimentally determined for the layer. During the rolling, plastic deformation develops in the subcontact layer that can lead to considerable structural changes compared to those in the workpiece volume. In general shear yield limit in the subcontact layer will be a function of the process parameters as follows:

a - plastic strain intensity $\bar{\varepsilon}^p$

$$\bar{\varepsilon}^p = \sqrt{\frac{1}{2}\,\varepsilon^p_{ij}\,\varepsilon^p_{ij}}\,, \tag{3}$$

where ε^p_{ij} is the plastic strain tensor, presented in the Cartesian coordinate system $Ox_1x_2x_3$ $(i,j = 1,2,3)$. Plastic incompesibility is assumed e.g. $\varepsilon^p_{kk}=0$

b - strain rate intensity $\dot{\bar{\varepsilon}}$

$$\dot{\bar{\varepsilon}} = \sqrt{\frac{2}{3}\,\dot{\varepsilon}_{ij}\,\dot{\varepsilon}_{ij}} \tag{4}$$

where $\dot{\varepsilon}_{ij}$ is the stran rate tensor. We assume a rigid plastic model for the rolled material so that, $\dot{\varepsilon}_{ij} = \dot{\varepsilon}^p_{ij}$ [9].

c - subcontact layer temperature $\vartheta_{(c)}$

d - Deformation mesures $\bar{\varepsilon}^p$ and $\dot{\bar{\varepsilon}}^p$ are assumed as average values of the layer thickness.

$$\bar{\varepsilon}^p = \bar{\varepsilon}^p_{(c)} \quad \text{and} \quad \dot{\bar{\varepsilon}} = \dot{\bar{\varepsilon}}^p = \dot{\bar{\varepsilon}}^p_{(c)}\,. \tag{5}$$

It is convinient to express the average subcontact yield limit $\tau_{p(c)}$ by the wellknown material shear yield limit τ_p e.g.:

(6) $$\tau_{p(c)} = k_{(c)} \tau_p ,$$

where in general the coefficient $k_{(c)}$ is a function of $\dot{\varepsilon}^p_{(c)}$, $\bar{\varepsilon}^p_{(c)}$ and $\vartheta_{(c)}$. We assume, $k_{(c)}=$ const. according to the concept of the reasonable simplified model, which reflects the average effect on the layer hardening . Shear yield limit τ_p is known from experiment as a function of the process parameters:

(7) $$\tau_p = \tau_p(\bar{\varepsilon}^p, \dot{\varepsilon}^p, \vartheta)$$

Hence the subcontact yield limit is:

(8) $$\tau_{p(c)} = k_{(c)} \tau_p\left(\bar{\varepsilon}^p_{(c)}, \dot{\varepsilon}^p_{(c)}, \vartheta_{(c)}\right) ,$$

The resistance to cutting at the subcontact layer, based on (8), can be presented in the form:

(9) $$\tau^{res}_{(c)} = n^{res}\tau_{p(c)} = c\,\tau_p ; \quad \text{where } c = n^{res} k_{(c)} = \text{const.}$$

The friction factor m in our model is treated as a local value, which changes on the contact surface and represents the Coulomb friction in the contact layer . This local value will depend on the process parameters related to this layer. We accept the scalar - effective strain ε^p_{eff} as a measure of the plastic deformation in the contact layer. In the most common case ε^p_{eff} will be equal to ε^p_{t1} - longitudal strain in the rolling direction (in case of rolling at a plane strain condition). If we have slabs, it is necessary to take into account the strain component - ε^p_{t2} longitudal strain accross the rolling direction. In the case of plastic isotropy we propose avector summation of this components to express ε^p_{eff}

(10) $$\varepsilon^p_{eff} = \sqrt{(\varepsilon^p_{t1})^2 + (\varepsilon^p_{t2})^2} ,$$

If we accept plastic incompresibility in the layer - $\varepsilon^p_{t2} = \frac{1}{2}\varepsilon^p_{t1}$ and $\varepsilon^p_{eff} = \sqrt{\frac{5}{4}}\,\varepsilon^p_{t1}$. In the case of plastic anisotropy in the layer we have:

(11) $$\varepsilon^p_{eff} = \sqrt{\alpha_{11}(\varepsilon^p_{t1})^2 + \alpha_{12}(\varepsilon^p_{t1})(\varepsilon^p_{t2}) + \alpha_{22}(\varepsilon^p_{t2})^2}$$

where α_{11}, α_{12}, α_{22} are coefficients reflecting upon this anisotropy. If we also assume plastic incompresibility, we receive:

$$\varepsilon^{p}_{eff} = A_{(a)}\varepsilon^{p}_{t1}\,, \text{ where } A_{(a)} = \sqrt{\alpha_{11} + \frac{1}{2}\alpha_{12} + \frac{1}{2}\alpha_{22}} \tag{12}$$

All these cases can be generalised in the following manner:

$$\varepsilon^{p}_{eff} = n_{(a)}\varepsilon_{t1}, \quad n^{p}_{(a)} = \begin{cases} 1 & \text{- plane strain isotropy} \\ \sqrt{\frac{5}{4}} & \text{- isotropy with across effects} \\ A_{(a)} & \text{- anisotropy} \end{cases} \tag{13}$$

Then the friction factor will be the function of:

1) v - relative velocity between the roll and the material $v = V/V_{roll}$, where V is material peripheral velocity absolute value and V_{roll} is peripheralroll velocity; 2) effective strain ε^{p}_{eff} 3) - the effective strain rate$\dot{\varepsilon}^{p}_{eff}$; 4) $\vartheta_{(c)}$ - contact temperature; e.g.:

$$m = m^{s}\left(v,\ \varepsilon^{p}_{eff},\ \dot{\varepsilon}^{p}_{eff},\ \vartheta_{(c)}\right) = m\left(v,\ \varepsilon^{p}_{t1},\ \dot{\varepsilon}^{p}_{t1},\ \vartheta_{(c)}\right)\,, \tag{14}$$

where c is included in the m function coefficints. The friction factor m couldbe obtained directly from the experiment [4]. The coefficients obtained experimentally will contain implicitly the multiplayers $k_{(c)}$, n^{res} and $n_{(a)}$. Friction anisotropy could appear in m, g.e. slabs rolling. This fact isdue to different asperation in both directions. In this case we introduce limit friction stresses in both directions as τ_{f1} and τ_{f2} (across to the motion).

Zibel friction law will take the form:

$$\tau_{F\alpha} = m_{\alpha}\,\tau_{p}\,, \qquad (\ \alpha = 1,2\ )\,, \tag{15}$$

If we take into account the average values from friction anisotropy, then we can assume, $m_{2} = k_{(a)}m_{1}$, where $k_{(a)}$ is an experimentally determined constant.(i.e. $k_{(a)} = 0.2$ Ref.[8])

Microdefects (microvoids, microcracks, etc.) [7], may appear in the subcontact layer due to the plastic deformation developed in it. They could be taken into account, if we assume τ_{p} dependent on the microdamage parameters.After introducing microdamage density ζ, the yield limit τ_{p} will take the form:

$$\tau_{p} = \tau_{p}\left(\bar{\varepsilon}^{p}_{(c)},\ \dot{\bar{\varepsilon}}^{p}_{(c)},\ \vartheta_{(c)},\ \zeta_{(c)}\right) \tag{16}$$

Hence $\zeta = \zeta_{(c)}$ is the average value of subcontact layer damage density. We are not aware of any sufficiently adequate experimental results on surface damage at rolling, in order to introduce these effects in the generalised Zibel law proposed.

The Zibel friction law according to our model will take the form:

$$\tau_F = m\left(v,\ \varepsilon^p_{t1},\ \dot{\varepsilon}^p_{t1},\ \vartheta_{(c)}\right)\tau_p\left(\bar{\varepsilon}^p_{(c)},\ \dot{\bar{\varepsilon}}^p_{(c)},\ \vartheta_{(c)}\right) . \tag{17}$$

The friction law offered in that way allows to describe sevaral experimentally observed effects [4][8]:

1. A coupling between the Coulomb and the Zibel friction mechanisms.
2. An influence of the normal presure on τ_F. It is included implicitly through the effective plastic deformation ε_{eff} in the contact layer.
3. An influence of the process factors on the limit stress of friction - τ_F.
4. A change of the friction factor on the contact length.

Moreover, the friction law proposed gives the following possibilities:

a - To determine the plastic anisotrpoy of the contact layer.

b - To take into account the friction anisotropy in the generalised Zibel friction law.

3. Experimental determination of the friction factor

We shall precise and give some new results for the friction factor *m* based on the approach proposed in [1]. There are two material functions in the generalised Zibel friction law offered: friction factor *m* and material yield limit τ_p. The yield limit could be determined following the usual approach (g.e.[6],[9]). The straitforward experimental determinaton of the friction factor in each contact point during the rolling process is extremely difficult at real circumstances. We assume, as a first aproximation, that this material function could be presented as a linear function of its arguments. Then one can determine the function using its average value on the contact zone e. g.,

$$m_{av} = m_{av}\left[v_{av},\ (\varepsilon^p_{t1})_{av},\ (\dot{\varepsilon}^p_{t1})_{av},\ \vartheta_{(c)}\right] \tag{18}$$

at $\vartheta_{(c)}$ = const. and an averaging of the type:

$$f_{av} = \frac{1}{S}\int_{(c)} f\, dS , \tag{19}$$

where $S_{(c)}$ is the contact area. The arguments' average values are determined following the wellknown technological relations offered in [4],[8], using the

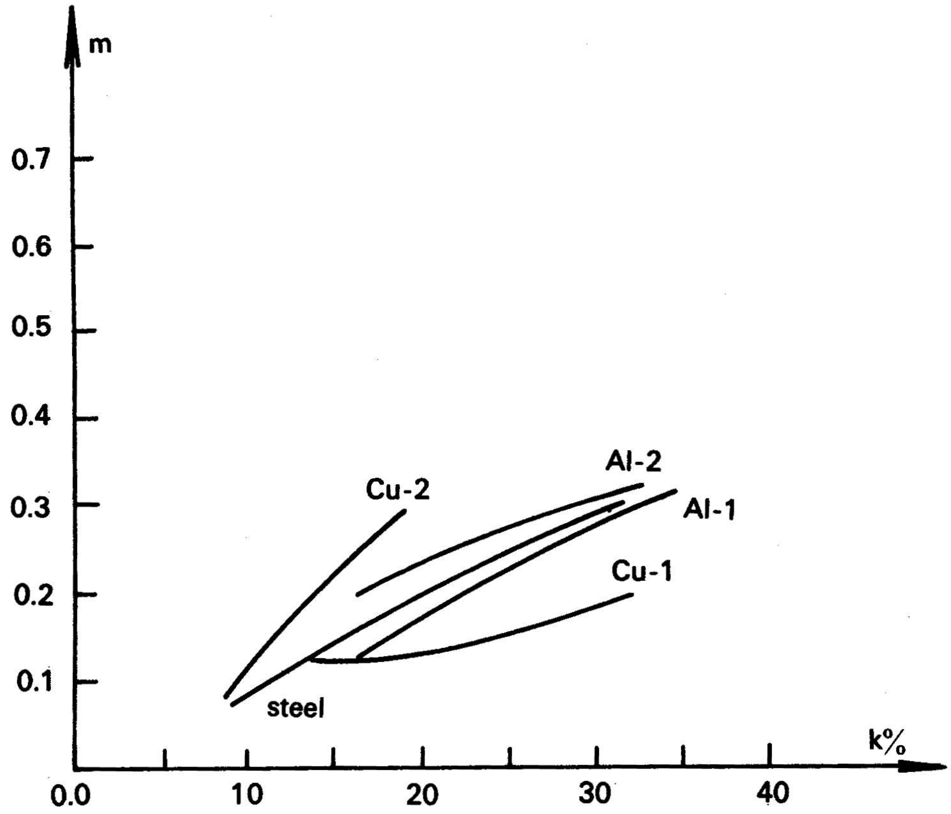

Fig. 2

parameters: reduction - $\mathbf{k} = \frac{\Delta H}{H_o}$, $\Delta H = H - H_o$ (where H_o and H are work piece initial and final height); D - roll diameter and V_{roll}.

(20)
$$v_{av} = \frac{1}{2}\mathbf{k}\,, \qquad (\varepsilon^{p}_{t1})_{av} = \frac{1}{3}\mathbf{k}$$

$$\left(\dot{\varepsilon}^{p}_{t1}\right)_{av} = \frac{2\pi}{60} N\left(1 - 0.75\,\mathbf{k}\right)\sqrt{\frac{kD}{2H(1-k)}}$$

where N is the number of revolutions per minute. Friction factors average value is expressed through the average friction coefficient as follows:

(21)
$$\mu_{av} = \frac{1}{S_{(c)}}\int \mu dS\,, \qquad m_{av} = \mu_{av}\left[\frac{|\sigma_n|}{2\tau_p}\right],$$

were σ_n is the normal stress on the contact surface. Its average value is obtained following the way described in [4]. We assume a linear aproximation of function (18) of the type:

$$m = m_o + Av + B\varepsilon_{t1} + C\dot{\varepsilon}_{t1} \; . \tag{22}$$

The coefficints m_o, A, B, C are obtained after perfoming a regression based on the experimental results from [3],[4], [7] following LSM.

$$m_{av} = m_0 + A\, v_{av} + B\left(\varepsilon^{p}_{t1}\right)_{av} + C\left(\dot{\varepsilon}^{p}_{t1}\right)_{av} \tag{23}$$

Table 1

M e t a l	m_o	A	B	C	from reference [3]
1. Steel	0.0	2.3	-0.28	1.10^{-3}	[5]
2. Copper - 1	0.0	2.4	-0.8	0.0	[2]
3. Copper - 2	0.0	3.0	0.0	0.0	[2]
4. Alumin. -1	0.0	-0.5	1.8	5.10^{-3}	[2]
5. Alumin. -2	0.0	-0.08	-0.34	0.05	[2]

Material constants' values for some widely used metals are given in Table 1. Friction factor dependence on the reduction is illustrated in Fig.2. This aproximation shows that the linear appoach to the friction factoris convinient from an experimental point of view.

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