Prediction of Mill Load in Multistage Stretch-Reducer of Tubes

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Abstract:

In order to evaluate the mill load in the stretch-reducing mill of seamless steel tubes, a numerical model is proposed on the basis of an elementary slab theory. This elementary method is improved so that the velocity discontinuity of the material at the entrance of the grooved roll may be taken into account. We find that the calculated results agree with the results obtained in a single stand experimental mill. Furthermore, by considering the change in the material temperature and the distribution of the interstand tension, the mill loads are calculated throughout the multistage rolling process. The results so obtained agree fairly well with the measurements in the practical operation.

1. Introduction

A stretch-reducing mill is a finishing mill for producing seamless steel tubes with relatively small diameters, and it consists of grooved rolls arranged in tandem. During the tandem rolling, the diameter of the tube is gradually reduced, and by the tension between the neighbouring stands the wall thickness can also be controlled as desired.

The deformation behaviour of the tube in the process is three-dimensional and the inside surface of the tube remains free. The theoretical analysis in this case is found to be very difficult, and there were formerly only too simplified theories, such as Neumann and Hancke's theory[1]. Therefore, it is common that the operation of the mill has been carried out on this experience only. However, in order to produce flexibly the tubes of various sizes as well as to operate the mill on the optimal condition, it is necessary that the deformation behaviour of the tube be analysed in the stretch-reducing process.

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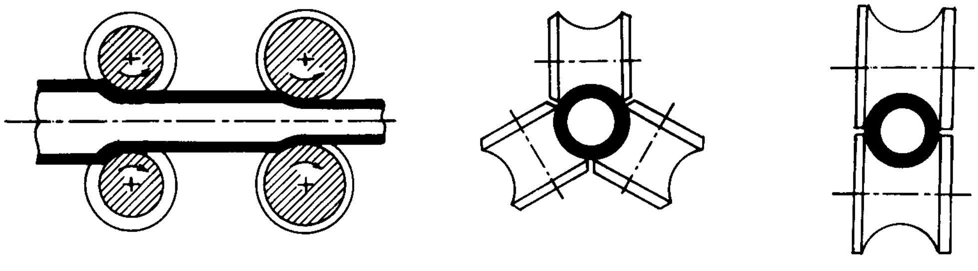

Figure 1 Schematic view of stretch-reducing process of tube.

Akiyama et al.[2] and Mori et al.[3] have recently applied the Finite-Element-Method to this problem. But the FEM is now not yet a practical method because of too much computation time.

We, therefore, wish to develop another method on the basis of elementary theory. Okamoto[4] has modified the slab theory for tube-sinking proposed by Swift[5]. However, there seems to be some doubt as to the general validity, as Kawanami et al.[6] have pointed out from an experimental standpoint. That is, it seems to be essentially unallowable to neglect the peening effect[7] in the stretch-reducing process.

In this paper, for a more correct evaluation of the mill load, Okamoto's theory is improved so that the peening effect at the entrance of the roll may be considered. Furthermore, by taking into consideration the temperature change of the material and the distribution of the interstand tension, the mill loads throughout the multistage stretch-reducing process are numerically estimated. The result so obtained is compared with the experimental one and discussed from a practical point of view.

2. Calculation model for a single pass stretch-reducing

In the stretch-reducing process, the tube is commonly formed by two or three grooved rolls in each stand, as shown in **Figure 1**. The diameter of the tube is reduced, and at the same time the wall thickness of the tube is changed in accord with the interstand tension of the tandem mill. The mechanics of the deformation have been treated by Okamoto[4] on the basis of the elementary slab theory for tube-sinking[5]. However, in the Okamoto theory, the velocity discontinuity of the material at the entrance of the roll is not taken into consideration. The velocity discontinuity causes the so-called peening effect. As pointed out in the report by Kawanami et al.[6], the mill load calculated by this theory is usually

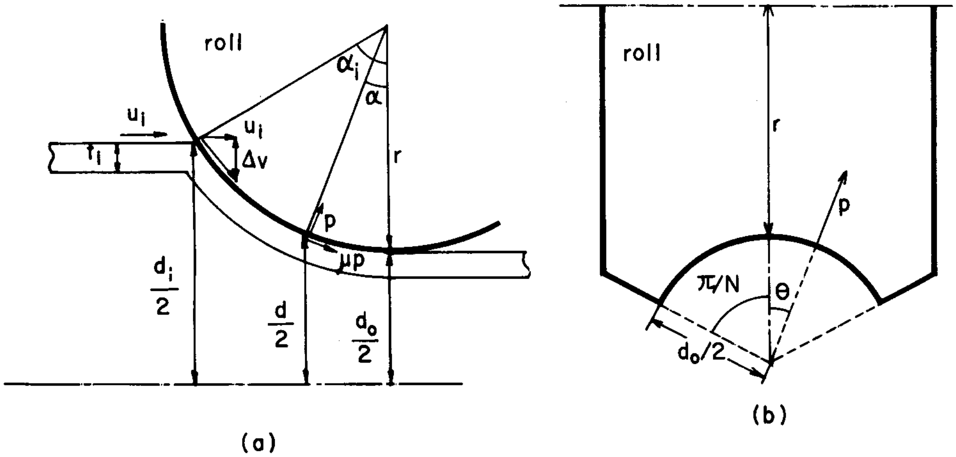

Figure 2 Material flow into a grooved roll gap.

much smaller than the measured one.

Here, omitting a detailed description of Okamoto's theory, we intent to write only about the improvement concerning the peening effect.

In the slab theory, the material flow is assumed to be discontinuous (see **Figure 2 (a)**). We believe that the velocity discontinuity Δv at the entrance of the roll should not be neglected in the case where the roll diameter is comparatively small. Δv is given as

$$\Delta v = u_i \cdot \tan\alpha_i \quad (1)$$

where u_i and α_i are the velocity component in the rolling direction and the bite angle at the roll entrance, respectively. The increment in the energy rate of dissipation $\Delta\dot{W}$ caused by Δv has to be considered, and is given as

$$\Delta\dot{W} = \int_S k\Delta v dS$$
$$= \pi d_i t_i k \Delta v / N \quad (2)$$

where, k: shear yield limit,

S: discontinuity interface,

d_i: tube diameter at the entrance,

t_i: wall thickness at the entrance,

N: number of rolls.

$\Delta\dot{W}$ is taken into account as the increment of the rolling torque i.e.,

$$\Delta T = \Delta\dot{W}/\omega \quad (3)$$

where ω is the angular velocity of the roll.

Again, the increment of the roll separating force ΔP caused by Δv is calculated by integrating the shear stress k over the cross section of the tube at the entrance, and is approximately given by

Table 1 Experimental conditions of a single pass stretch-reducing

Material: Steel (C: 0.25 %, Si: 0.35 %, Mn: 0.3 ~ 0.9 %, P: 0.04 %, S: 0.04 %)
Rolling temperature: 1000 ℃
Initial diameter of tube: 76.3 mm
Initial wall thickness of tube: 7.0 mm
Reduction ratio: 10 %
Roll diameter: 260 mm (2-roll type)
Rolling speed: 72.4 rpm

Table 2 Comparison between caluclation and experimental results of a single pass stretch-reducing

	Roll separating force (kN)	Rolling torque (kN · m)	End wall thickness (mm)
Caluculation without peening effect	44.8	1.02	7.28
Caluculation with peening effect	70.6	2.17	7.28
Experiment (average value)	73.9	2.27	7.18

$$\Delta P = k t_i d_o \sin(\pi/N) \tag{4}$$

where d_o is the tube diameter at the exit of the roll (see **Figure 2 (b)**).

ΔT and ΔP are added to the rolling torque and the roll separating force which are calculated by the slab method.

In order to inspect the validity of this calculation model, an experiment was carried out. **Table 1** indicates the experimental conditions. **Table 2** shows the comparison between the experimental and the calculated results with and without consideration of the peening effect. Shida's equation[8] is applied to the flow stress of hot steel in the calculation. We find from **Table 2** that the peening effect is fairly significant in the stretch-reducing process, and that the torque and force calculated by the model agree fairly well with the measured ones.

3. Calculation model for multistage stretch-reducing mill

3. 1. Calculation method for temperature change of material

For the prediction of the mill loads in the multistage rolling process, the temperature change of the material has to be evaluated. It is assumed in this calculation that the inside wall of the tube is thermally insulated.

When the rolled material is contacted with the rolls in each stand, the change in the material temperature is caused by the plastic deformation, the friction and the conduction.

a) Temperature rise by plastic deformation

The energy of the plastic deformation per unit volume is commonly given by

$$W_p = \int_0^{\varepsilon} k_f d\varepsilon \tag{5}$$

where k_f is the flow stress and ε is the equivalent strain. For the case of the stretch-reducing process, by using the mean flow stress k_m

$$W_p = \frac{2}{\sqrt{3}} k_m \ln(d_i/d_o) \tag{6}$$

By assuming that all the energy of the plastic deformation is converted to heat, the temperature rise $\Delta\theta_p$ is given by

$$\Delta\theta_p = W_p/(c\gamma) \tag{7}$$

where c and γ are the specific heat and the density of the tube, respectively.

b) Temperature rise by friction

The frictional energy per unit volume of the tube during the contact time t_c is given by

$$W_f = \int_0^{t_c} \dot{W}_f F dt \tag{8}$$

where F indicates the contact area per unit volume i.e.,

$$F = 1/(t_i \cos\alpha) \tag{9}$$

and $\dot{W}_f$ indicates the frictional energy rate per unit surface area i.e.,

$$\dot{W}_f = \mu p \Delta v_r \tag{10}$$

where μ is the frictional coefficient, p is the roll pressure, and Δv_r is the relative velocity between the roll and tube.

The integration of Eq. (8) is transformed to that along the roll angle α as follows;

$$W_f = \int_0^{\alpha_i} \dot{W}_f F r \frac{d}{d_i} \frac{1}{u_i} d\alpha \tag{11}$$

where r is the roll radius, and d is the tube diameter at a roll angle α.

The frictional heat is distributed both to the tube and the roll. The temperature of the roll is much lower than that of the tube. This is due to the reason why the heat conductivity of the roll may be considered to be about twice as high as that of the tube[9]. Therefore, the temperature rise by the friction $\Delta\theta_f$ is approximately given by

$$\Delta\theta_f = W_f/(3c\gamma) \tag{12}$$

c) Temperature drop through contact with the rolls

The rolled material and the roll are regarded as semi-infinite bodies and are assumed to perfectly contact each other. In so doing, the heat transfer ΔQ_c from the unit surface area of the tube to the roll during contact is written as follows;

$$\Delta Q_c = \int_0^{t_c} \lambda_1 \frac{\partial\theta}{\partial y}\Big|_{y=0} dt \tag{13}$$

where θ is the temperature of the tube, λ_1 is the heat conductivity of the tube, and y is the vertical distance from the outside surface of the tube. ΔQ_c is theoretically expressed as follows;

$$\Delta Q_c=2\sqrt{t_c}\,\lambda_1(\theta_1-\theta_0)/\sqrt{\pi a_1} \tag{14}$$

where

$$\theta_0=(\lambda_1 a_1^{-1/2}\theta_1+\lambda_2 a_2^{-1/2}\theta_2)/(\lambda_1 a_1^{-1/2}+\lambda_2 a_2^{-1/2}) \tag{15}$$

where, θ_1: temperature of tube before contact,
θ_2: temperature of roll before contact,
θ_0: temperature of contact surface between roll and tube,
λ_1: heat conductivity of tube,
λ_2: heat conductivity of roll,
a_1: thermal diffusivity of tube,
a_2: thermal diffusivity of roll.

Therefore, the temperature drop $\Delta\theta_c$ is estimated by

$$\Delta\theta_c=\Delta Q_c S_c/(Vc\gamma) \tag{16}$$

where S_c is the contact surface area, and V is the volume of tube which is contacted with roll.

Furthermore, the tube temperature drops in the stand intervals through convection and radiation from the tube's outside surface. This is obtained by the method which Kokado[9] has proposed for the temperature calculation of slabs in a hot strip mill. Here, a detailed description is omitted on accout of space consideration.

3. 2. Distribution of interstand tension in tandem mill

For the prediction of the mill load in a tandem mill, the interstand tension has to be evaluated. From some calculated results, it has been clarified that the interstand tension must be high enough to reduce the wall thickness in the stretch-reducing process. Also, the maximum tension depends on the size of the tube, the roll radius, the reduction ratio and the frictional coefficient between the tube and roll.

Table 3 indicates the draft schedule of a stretch-reducing mill in an actual operation. It consists of 17 stands in tandem. In the case where the neutral point is controlled within the contact area, the calculation examples of the distributions of the interstand tension as well as the wall thickness are shown in **Figure 3**. It is observed that the interstand tension increases gradually from zero in the earlier stands, is nearly constant in the middle stands and decreases to zero in the later stands again. Therefore, the wall thickness decreases in the middle stands where the interstand tension is high. Meanwhile, the thickness

Table 3 Rolling conditions of multistage tandem stretch-reducing mill

Stand number	Diameter of tube	No.	Diameter of tube	No.	Diameter of tube
	172.0		128.5		94.3
1		7		13	
	168.5		121.6		91.6
2		8		14	
	161.9		115.1		90.2
3		9		15	
	152.7		109.0		89.8
4		10		16	
	144.1		103.3		89.62
5		11		17	
	136.0		98.0		89.56
6		12			

Initial wall thickness of tube : 6.6 mm Roll diameter : 420 mm (3-roll type)
Initial temperature of tube : 890.0 ℃ Stand interval : 385 mm

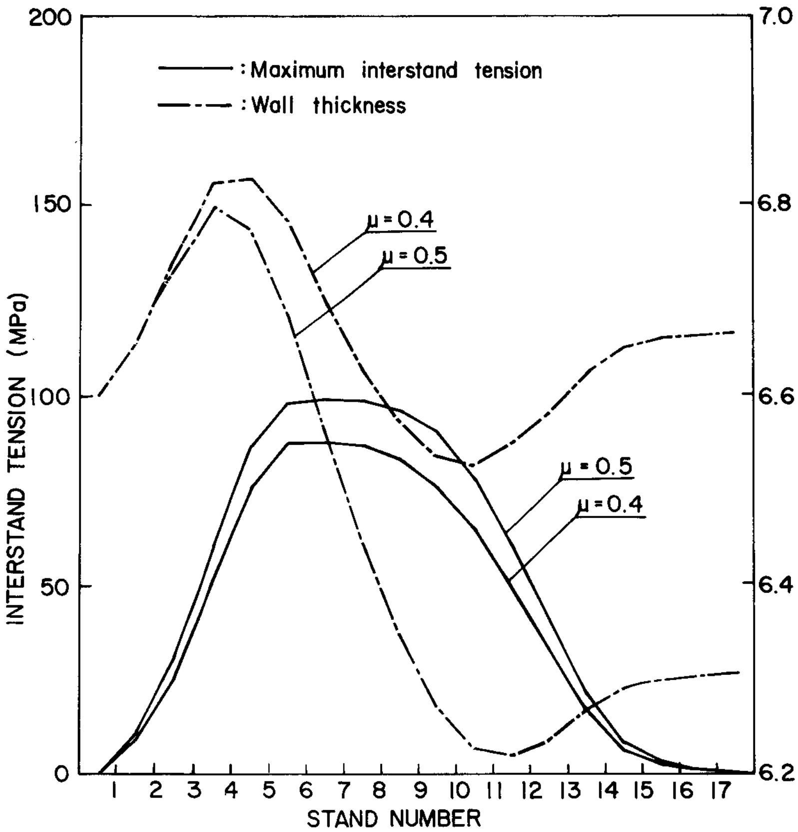

Figure 3 Distributions of maximum interstand tension and changes of wall thickness of tube in the multistage stretch-reducing mill in cases where the frictional coefficient $\mu = 0.4$ and 0.5.

becomes large in the earlier and later stands. Again, with an increase in the frictional coefficient μ, the maximum interstand tension increases, and accordingly the wall thickness decreases.

3. 3. Result and discussion

Using the aforementioned model and values, the mill loads are calculated in the multistage stretch-reducing mill (see Table 3). In this calculation the frictional coefficient is assumed to be 0.4.

Figure 4 shows the measured and the calculated rolling torques in the multistage mill. The calculated torques agree, although roughly, with the measured ones from a qualitative as well as a quantitative point of view. It would be concluded that the calculation model may be applied to practical use.

However, it is observed that the calculated torques at the stands after the 14-th stand are much lower than the measured ones. It is commonly accepted that it is necessary for an accurate estimation of the mill load at the later passes in the hot strip finishing mill to take into consideration not only the strain at the pass concerned but also the strain accumulated in the previous passes. The calculation error in this stretch-reducing mill may be caused by the same reason. Therefore, we try to apply the calculation model of the accumulated strain.

The Avrami formula to estimate the recrystallization ratio $X(t)$ is given as

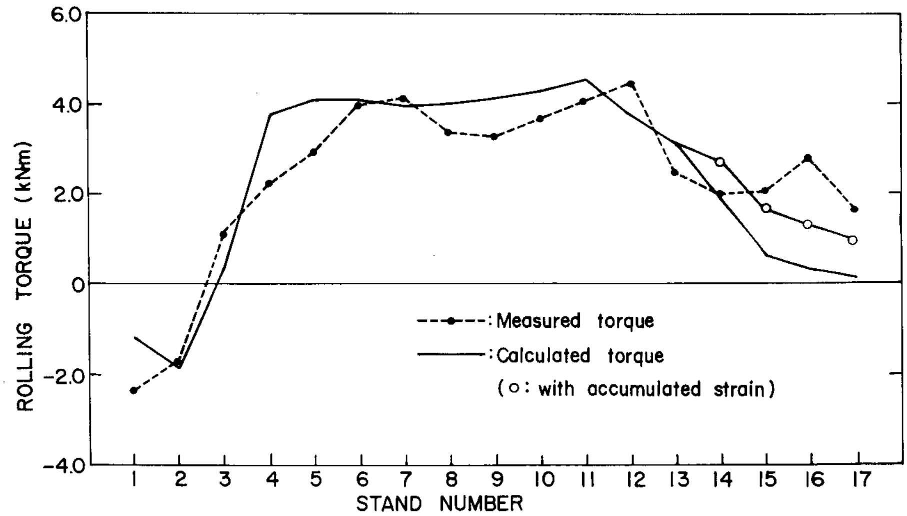

Figure 4 Comparison between measured and calculated torques in the multistage stretch-reducing mill.

a function of time t by

$$X(t)=1-exp(-At^n) \tag{17}$$

The value of A depends on the temperature and is expressed in the Arrhenius type

$$A=A_0\, exp\left(-\frac{Q_s}{RT}\right) \tag{18}$$

where, R is the universal gas constant, T is the absolute temperature, and Q_s is the activation energy for self-diffusion, and formulated as a function of carbon content $[c]$ as follows;

$$ln\, Q_s=5.37-0.0796 ln[c] \tag{19}$$

Putting $n=3$ and $A_0=1.5 \cdot 10^{11}$, the non-recrystallization ratio $Y(t)$ can be expressed by[10]

$$Y(t)=1-X(t)=exp\left(-1.5\cdot 10^{11}\, exp\left(-\frac{Q_s}{RT}\right)t^3\right) \tag{20}$$

Finally, the real strain ε^{sum} at the i-th pass can be written in the following form:

$$\varepsilon_i^{sum}=\varepsilon_i+\varepsilon_{i-1}^{sum}Y(t_{(i-1)\sim i}) \tag{21}$$

where $t_{(i-1)\sim i}$ indicates the interpass time between the $(i-1)$ th and the i-th stands.

By applying Eq. (21) to the stands after the 14 th stand, the rolling torques are calculated again and shown in Figure 4 by the open circles. The torques so obtained have a better fit with the measured ones.

4. Conclusions

In this study, the elementary slab theory for the analysis of deformation in the stretch-reducing process of seamless tubes has been developed. It was done by taking account of the so-called peening effect which is caused by the velocity discontinuity of the material at the entrance of the roll gap. It turned out, from the comparison with the experimental result of the single pass, that the peening effect in the stretch-reducing process is fairly significant, and that the mill load calculated by the model agrees with the experimental one.

Using this model and by combining the numerical model for the temperature change of the material in a hot rolling process, the mill loads in the multistage tandem mill have been calculated. The results so obtained showed a good fit to the measured mill loads in the actual stretch-reducing mill. However, at the same time, they suggested the necessity of the consideration of the accumulated strain at the later stands.

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