THERMO-MECHANICAL MODELLING OF THE WORK-ROLL DURING THE HOT ROLLING PROCESS*

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Abstract

This paper describes the study of the work-roll during the hot rolling process thanks to a 3D model built with the Finite Element Method (FEM). In terms of computational cost, that method needs high resources compared to 1D model which are mostly used. However, the 3D model is built to have a better understanding of the work-roll thermal and mechanical behavior during the hot rolling process. The thermal model shows that the highest temperature reached during rolling are within the range of 500°C and 600°C. The thermal field input used to simulate the mechanical behavior of the work-roll shows a biaxial thermal stress state, in the circumferential and axial planes. However, a peak of radial stress is observed in the backup zone. It did not generate plastic strain but could influence the fatigue behavior of the work-roll.

Keywords: Hot Rolling; Thermal Fatigue; Work-Roll Degradation; Work-Roll Cooling.

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1 INTRODUCTION

The rolls are the first source of cost in a hot strip mill. To get a strip with good quality, a proper wear resistance and a resistance to surface degradation is needed. In the hot rolling process, the work-rolls undergo two types of fatigue solicitations. The most damaging one is the thermal fatigue. D. Spera [1] defines it as a phenomenon due to a progressive damage and cracks of a material or a structure by alternating heating and cooling while free expansion is partially or totally hindered. Applied to the hot rolling case, the contact between the work-roll and the strip generates fire crazing (thermal cracks) at the roll surface. Two subsequent steps are noticed during the roll degradation process. The first one is the oxidation stage (Figure 1) where the oxide layer in the work-roll surface is built. Generally, it is observed in the first 20 strips. Then, a second stage occurred, the degradation phase. In the latter, successively pitting and peeling of the work-roll is observed before the banding phase. That generates some parts which are removed from the work-roll surface. They dropped and printed in the strip and could create defects on the strip surface. The most well-known defect is the rolled-in-scale one. It is well explained in B. Picque thesis [2]. Then, to reduce the industrial use cost and to have a good strip surface quality, its degradation must be mastered. The work-roll cooling can have a strong effect on that phenomenon as it plays a key role. Therefore, a suitable and mastered work-roll cooling can avoid or delayed the roll degradation.

To assess the work-roll lifetime or to optimize its efficiency, different ways of modelling are available. Currently, most of the thermo-mechanical models used are based on 1D thermal transient hypothesis.

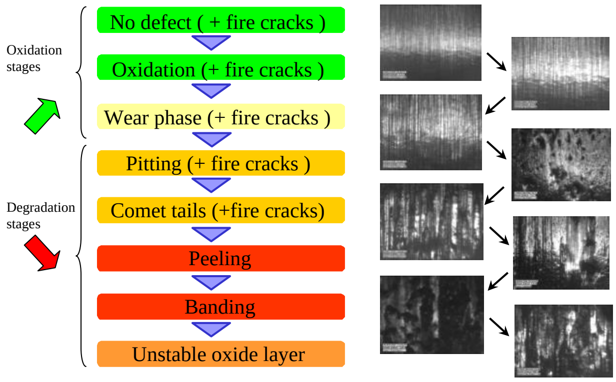

Figure 1: work-roll degradation process, with two subsequent phases: the oxidation stages then the degradation ones.

That kind of model allows to quickly predict the effects of design constraints on the cooling efficiency. Nevertheless, it is too conservative and not adequate to determine a thermal fatigue criterion based on multiaxial solicitations as it is the case for the work-rolls. In the literature, different publications can be found regarding the modeling based on the Finite Elements Method (FEM). Due to the computational costs, most of the time 2D-simulations are used as in K. Tieu *et al* paper [3], where two 2D simulation models were used to assess the thermal and mechanical fields within the work-roll. That study is interesting as a correlation attempt is done to understand the link between the 2D-computations, the roll microstructure and the residual stresses created during its manufacturing. Other type of simulations was done in D. Benasciutti *et al* paper [4] with a plane finite element model approach. That one was quite simple and more methodological than quantitative as specified by the author. However, the first results were very promising as they give some insights about the thermo-mechanical behavior of the work-roll under operations. To cope with the computational cost due to the 3D modeling, B. Wright [5] proposed in his thesis to use the submodeling techniques. It allows to have a global 3D-model with a coarse mesh then a local 3D-submodel with a finer mesh. However, both 3D and 2D models were used to understand the work-roll stress profiles. The 2D models are often used to avoid computational costs as done and shown by Felipe Weildlich *et al*. paper [6]. In the latter a 2D-plane-strain model was used to calculate the depth of the heat penetration through the work-roll to correlate it with pilot mill trials.

Consequently, the purpose of this work is to propose a 3D model of the finishing mills work-rolls based on FEM. It will allow us to have a better understanding of the work-roll thermal and mechanical fields during operations and estimate its lifetime. If the latter is mastered it could result to an increase of the work-roll campaign length, to lower the rolling stop frequency, to do energy savings etc.
Therefore, the first part of this paper will deal with the description of the 3D model and its specifications (materials, meshing etc.). In the second section, the thermal hypotheses will be discussed as well as the mechanical ones. Then the two last sections will talk about the different results obtained thanks to the simulations. To finish, after the conclusion, a paragraph dedicated to the outlooks and room of improvements of the model will be proposed.

2 MATERIAL AND METHODS

A 3D-model is used to validate the thermal field of the work-roll (Figure 2). The latter is composed with two materials. The core part of the work-roll is a spheroidal graphite iron and the shell layer is a high-speed steel. The material parameters (conductivity, specific heat, young modulus, etc.) are temperature-dependent. The Table 1 shows a sample of the material parameters used for the computations.

Conductivity	Density	Expansion coefficient	Young modulus	Poisson's ratio	Specific heat
23 mW/mm/K	7.59e-9 T/mm3	1.3 10-5°C^{-1}	210000 MPa	0.3	4.98e+08 mJ/T/K

Table 1: material parameters of the high-speed steel used for the computations at ambient temperature.

An eighth of the work-roll is modeled to save computational resources. Afterwards, a sequentially coupled heat transfer computation is done, as the temperature field is found without knowing the stress/deformation field. Consequently, a heat transfer calculation is done first, to obtain the temperature field. Then, this one will be an input field for the mechanical calculations. The thermal problem is governed by the heat equation for the temperature field ***T***:

$$\rho c \frac{\partial T}{\partial t} - div\, k \nabla T = r \qquad (1)$$

Where: ***c*** is the heat capacity of the material, ***k*** is the conductivity of the material, ***r*** is a heat volume source as explained by Lemaître *et al* [7].

For the thermal analysis, different heat transfer coefficients (HTCs) are applied to the work-roll according to their functions: removing heat from the roll thanks to the cooling sprays (exit and entry cooling) or adding heat to the roll thanks to the strips. It is difficult to calculate the heat fluxes or HTCs of those cooling sprays as the work-roll is a moving surface. Those heat fluxes due to the convection of the coolant is governed by the equation below:

$$q = -h(\theta - \theta_0) \qquad (2)$$

Where: q is the heat flux removed from the roll

h is the heat transfer coefficient

θ is the temperature at the roll surface

θ_0 is the water or air temperature

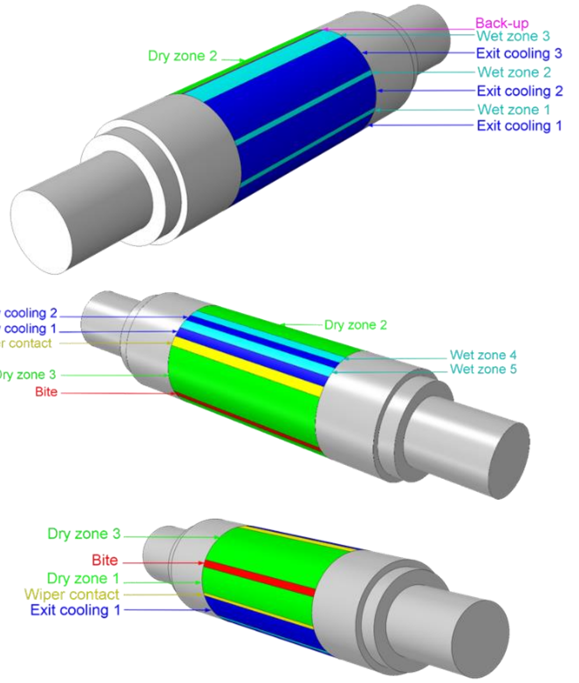

Figure 2: the thermal boundary conditions applied to the work-roll.

Furthermore, the coefficients depend on many factors as the spray angle, the impact angle, the pressure, the sprays' overlapping etc. as specified by P. Kotrbacek *et al*. [8]. In that paper the authors tried to study the influence of each of these parameters on the HTCs. In our calculations, it is assumed that the cooling sprays are homogeneous along the roll width. Then, an HTC of 20 kW/m². K will be used at the exit and entry cooling sprays locations. Those values were fixed after several industrial trials handled in the R&D laboratory facilities. The entry-cooling is turned-off after two slabs as it is done in some hot mills to operate well the roll-gap lubrication. In the contact zone between the strip and the roll, an HTC of 60 kW/m². K is applied to the roll bite with a perfect thermal contact assumption. It is very tough to measure it with industrial trials. It is also well known that the oxide scale influences the work-roll surface temperature by playing the role of a thermal insulator. However, in his paper, N. Legrand *et al.* [9] proposed a method thanks to temperature sensors and simulations to get those coefficients. He showed that there is a non-uniform HTC in the contact zone between the roll and strip. The peak value observed in the roll bite is between the range of 50 and 60 KW/m².K. In these simulations, the strip is not modeled, and its temperature (~900°C) is considered as a fixed value all along the campaign. The dry zones and wipers contact with the work-roll have the same HTC with a value set to 0.5 kW/m². K. The HTC of the wet zones close to the cooling sprays have an HTC set to 5 kW/m². K.

For the finite element model, a linear hexahedral element which is suitable for this type of study is used for the transient heat transfer computations with one temperature degree of freedom for each node. The mechanical analysis is done with the same brick element. However, at each node three degrees of freedoms are used along with 8 integration points and 13 internal degrees of freedom for incompatible modes. The latter allows to solve issues linked to shear locking in bending motions, like in the work-roll, compared to the fully integrated first-order elements. The mechanical problem is governed by the equation below:

$$div\,\boldsymbol{\sigma} = \mathbf{0} \qquad (\mathbf{3})$$

Where: σ is the stress tensor.

A simple constitutive law, with a unique isotropic hardening hypothesis without any kinematic hardening effect, is used thanks to monotonous trials handled in the laboratory. Its governing equations are expressed below with a respect to the Von Mises criterion and at an isothermal state [7]:

$$\boldsymbol{\varepsilon} = \boldsymbol{\varepsilon^p} + \boldsymbol{\varepsilon^e} \qquad (4)$$

$$\boldsymbol{\sigma} = \boldsymbol{E(\varepsilon - \varepsilon^p)} \qquad (5)$$

$$\boldsymbol{f} = |\boldsymbol{\sigma} - \boldsymbol{X}| - \boldsymbol{R} - \boldsymbol{\sigma_y} \leq \mathbf{0} \qquad (6)$$

$$\dot{\boldsymbol{\varepsilon}}^{\boldsymbol{p}} = \mathbf{0}\ \boldsymbol{if}\ \boldsymbol{f} < \mathbf{0} \qquad (7)$$

$$\dot{\boldsymbol{\varepsilon}}^{\boldsymbol{p}} > \mathbf{0}\ \boldsymbol{if}\ \boldsymbol{f} = \mathbf{0}\quad \boldsymbol{the\ sign\ of}\ (\boldsymbol{\sigma} - \boldsymbol{X}) > \mathbf{0} \qquad (8)$$

$$\dot{\boldsymbol{\varepsilon}}^{\boldsymbol{p}} < \mathbf{0}\ \boldsymbol{if}\ \boldsymbol{f} = \mathbf{0}\quad \boldsymbol{the\ sign\ of}\ (\boldsymbol{\sigma} - \boldsymbol{X}) < \mathbf{0} \qquad (9)$$

Where $\boldsymbol{\varepsilon}$ is the strain ($\boldsymbol{\varepsilon^e}$ and $\boldsymbol{\varepsilon^p}$ are respectively the elastic strain and the plastic strain), $\boldsymbol{\sigma}$ is the stress tensor, $\boldsymbol{X}$ is the kinematic hardening tensor and $\boldsymbol{R}$ is the isotropic parameter. The equation (6) is the charged surface. However, like said previously, the kinematic behavior of the material is not considered in these calculations.

For the mechanical boundary condition applied to the work-roll, there is the roll force applied to the strip that changes throughout the rolling campaign and the mechanical contact with the backup roll. By neglecting the thermal contribution and with simplified analytical computations we can roughly estimate the contact pressure between the work-roll and the strip to roughly 300 MPa. In the backup contact zone, an Hertzian contact is considered. The mechanical pressures are higher and rise to a level of ~1.5 GPa by neglecting the stress concentration at the roll chamfers. In the upcoming sections, the thermal results will be discussed first, then the mechanical ones.

3 RESULTS AND DISCUSSION

3.1. Thermal analyses

For the thermal analysis, a complete rolling campaign with 84 strips is computed. This represents a schedule of roughly 4 hours.

The initial work-roll temperature is fixed at 25°C. It can have an effect as a higher initial work-roll temperature generates lower compressive thermal stresses at the work-roll surface. This is discussed in G.Y Deng *et al.* paper [10]. The Figure 3 below shows the thermal map obtained right after the end of the campaign.

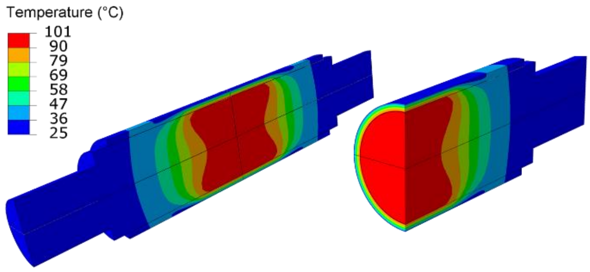

Figure 3: thermal field of the work-roll observed at the end of a rolling campaign.

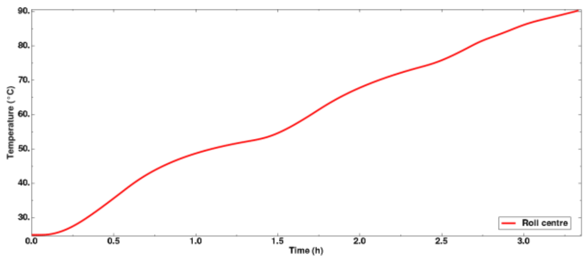

Figure 4: the evolution of the work-roll temperature at its centre.

In these simulations a late cooling of 20 seconds is done after the last strip. Therefore, the roll surface is cold with a temperature of roughly 25°C. Whereas, the centre of the roll is very warm with a temperature very close to the one measured by the operator 20 to 30 minutes later after the rolling campaign. That practice is done by the mills to allows the heat inside the work-roll to spread up to the surface. The thermal map obtained did not consider the cooling sprays heterogeneity along the roll's barrel length. However, the thermal field obtained is satisfying as the roll centre temperature (Figure 4) can be compared to the one done with a contact thermocouple at the end of the campaign.

The Figure 5 shows the evolution of the work-roll surface temperature in one roll revolution at the very beginning of the campaign. It shows that the peak temperature is reached within the roll bite, then it decreases sharply thanks to the exit cooling sprays.

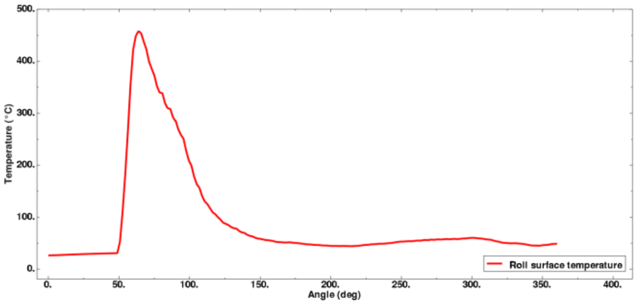

Figure 5: fast thermal evolution of the work-roll

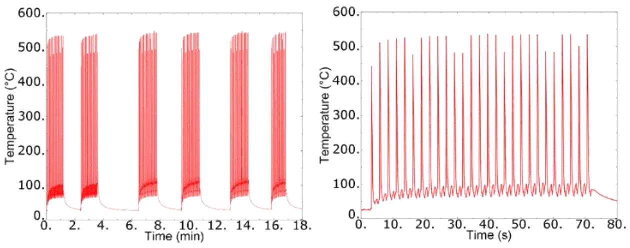

Figure 6: [Left] work-roll surface temperature evolution for the first six strips of the rolling campaign; [Right]: Focus on the first strip

The Figure 6 shows the work-roll surface temperature evolution at the beginning of the schedule with a focus on the first six strips. It shows very high thermal gradients at the work-roll surface where the thermal cracks occur. These results confirm observations made by K. Tieu *et al* [3] with roughly same peaks of temperature at the work-roll surface. However, it is a way higher than the results obtained by D. Benasciutti *et al* [4]. These differences can be explained by the difficulties to get or to know the right HTC for the cooling sprays as well as the one for the roll bite. Furthermore, the friction energy which is dissipated inside the roll bite is not considered in the computations.

Afterwards, the thermal output is used and plugged in the mechanical computations as a boundary condition. In the next sub-section, the results of the mechanical analysis will be discussed.

3.2. Mechanical analyses

To perform the mechanical analysis, an incremental numerical method is considered. It is typically used in finite element analysis solvers such as Abaqus/Standard for nonlinear problems [11]. The most well-known and used is the Newton method. It has a great advantage that is quadratically convergent. One of the drawbacks is the calculation and the solving of the Jacobian matrix. It is expensive because the matrix must be solved at each iteration which is very time consuming and it is not so easy to choose a suitable initial condition. Nevertheless, at a first approach that method will be used. However, other methods to reduce the computational costs could be investigated later like the quasi-Newton method or direct methods. The graph below shows the stresses evolution during one revolution (Figure 7). It shows a bi-axial stress state: one along the circumference and the other one along the roll axis. These results confirm the ones obtained in the papers [3] and [4]. The thermal stresses reached at the roll bite are roughly identical with values nearly to 1500 MPa. The radial stresses at the backup zone are not negligible with a very high compressive stress (roughly 1300MPa). At that location the work-roll is cold with a temperature near to the ambient room (roughly 45°C). There is no plastic strain which is observed at the level of the campaign in that backup zone contact. In the radial dimension, the thermal contribution to the stress field is very negligible as the level of the stress is equivalent to the one obtained analytically without considering thermal aspects.

The Figure 8 shows the work-roll stresses observed at the backup and strip locations. The results show a constant level of compressive stresses, when the strip is rolled, with a value of -1200 MPa. This could be explained by the uniformed cooling which is applied along the barrel of the work-roll. Some stresses fluctuations could be expected if the cooling sprays heterogeneities were considered in the simulations. Whereas, in the contact of the backup the level of stresses reached roughly 600 MPa.

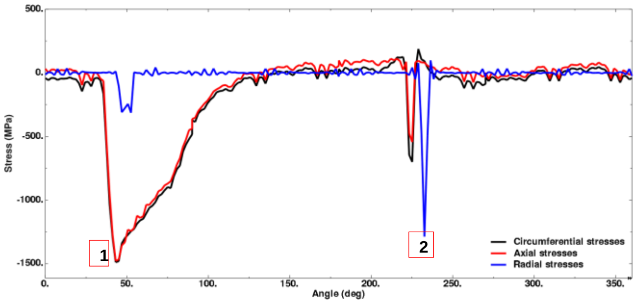

Figure 7 : comparison between the radial, axial and circumferential stresses of the roll. (1) the contact angle between the strip and the work-roll is fixed to 8 degrees and 2 degrees between the work-roll and the backup roll (2)

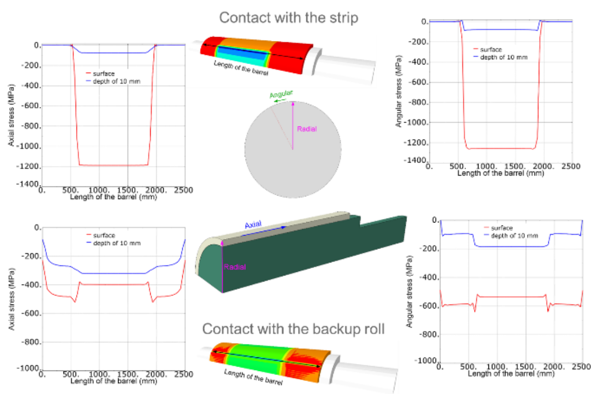

Figure 8: axial (left) and angular (right) stresses variation along the roll barrel length

The Figure 9 shows that there is a large domain of plastic strain at the contact between the work-roll and the strip at the very beginning of the rolling campaign. As the constitutive law is built with an isotropic hardening assumption, the plastic strain grows revolution after revolution as expected. To have a better understanding of the crack networks formation within the work-roll, this study will be continued with a more reliable constitutive law and numerical methods that could allow us to handle the computational cost of these kind of applications.

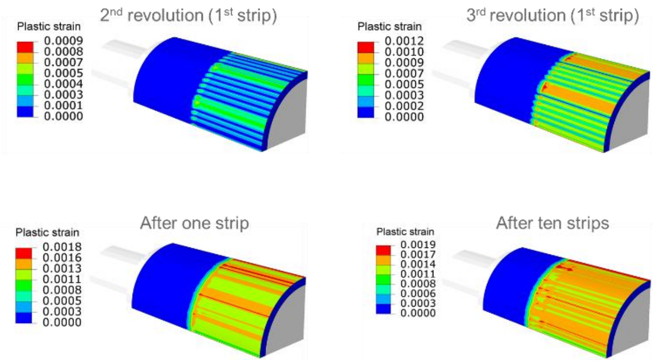

Figure 9: evolution of the plastic stain. It appears very earlier at the very beginning of the rolling campaign.

4 CONCLUSIONS

That study shows that the thermal field obtained by simulations at the centre of the work-roll could be compared with the one done by the operators in the mill, at the end of the campaign. It proves also that the thermal gradients within the work-roll surface layer are very high with temperatures between 500°C and 600°C at the roll bite location. Those high temperature variations generate thermal stresses in the work-roll surface layer. The simple constitutive law used to run the mechanical analysis allows to determine the bi-axial stress state (circumferential and axial) within the work-roll. Nevertheless, the radial stress should be considered to assess the work-roll lifetime as high compressive stresses are noticed at the backup zone location but without any plastic strain observed.

To go further, for the thermal analysis, the next simulations will consider the heat fluxes variations along the roll barrel length to get the stress variations in the axial length of the roll.
This study will continue also to find a thermal fatigue criterion to assess the work-roll lifetime during the hot rolling processes. To have a better modeling of the work-roll behavior in the framework of the low-cycle fatigue, a combined non-linear kinematic hardening and an isotropic hardening should be considered. This to have a better prediction of the two material asymptotic states that could occurred in the thermal fatigue, namely the plastic shakedown and the ratchetting effect. To cope with the computational resources, multiscale simulations will be investigated. For instance, to faster the calculations, direct methods allow to compute directly the stabilized fatigue cycle. In the other hand submodeling techniques could be used to have a precise result in the location of the thermal cracks.

Acknowledgments

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