Hardening of steel axially symmetrical elements heated with two-turn inductor

S Iskierka¹ and I Iskierka¹

¹ Faculty of Electrical Engineering, Czestochowa University of Technology, 69 Dabrowski Str., 42-200 Czestochowa, Poland

Abstract. This research the complex model of induction hardening of tool steel was shown. Electromagnetic phenomena, thermal phenomena and phase transformations were taken into considerations. The electromagnetic and the heat transfer equations have been solved by finite elements method by Galerkin formulations. Model of estimation of phase fractions and their kinetics has been based on the isothermal transformation diagram (TTT). Phase fractions which occur during the continuous heating and cooling (austenite, pearlite or bainite) are described by Johnson-Mehl-Avrami formula. To determine of the formed martensite the modified Koistinen-Marburger equation is used.

1. Introduction

Induction heating is a well-known method for processes such as quenching, annealing, heating for hot forging, and melting. The induction heating process has many advantages over traditional methods, such as no open fire or atmosphere safety problems, repeatability, high efficiency, reduced scale, fast targeted heat and ease of use [1-3].

![Figure 1. Phenomena during induction hardening.](image-url)
The mutual influences of individual phenomena accompanying heat treatment are presented in the diagram in figure 1. Hardening steel is a thermal treatment that requires heating above the austenite conversion temperature followed by sufficiently rapid cooling to obtain a bainitic or martensitic alloy structure. Induction hardening is used to obtain high surface hardness of the component layer while maintaining a ductile core, which ensures high strength and, at the same time, high resistance to abrasion. Eddy currents induced in the material cause the formation of volumetric heat sources with high powers. As a result of these sources, the material heats up to a high temperature, and due to the fact that the heat sources operate in a small volume, the temperature field in the material is highly inhomogeneous.

2. The electromagnetic field

The electromagnetic field in a conducting medium is determined by Maxwell equations. Assuming steady-state and considering that the displacement current density is negligible for line frequency, and by introducing Coulomb gauge $\nabla \cdot A = 0$ [4], the magnetic vector potential in two-dimensional or axisymmetric geometries is given by the complex equation

$$\left( \nabla \frac{1}{\mu} \nabla - j \omega \gamma A = - J_s \right)$$

(1)

where: $A$ is the component of magnetic vector potential (complex r.m.s. value), $\omega, \gamma = \gamma(\Theta)$ and $\mu = \mu(H, \Theta)$ are angular frequency conductivity and permeability, respectively, $\Theta$ is the temperature and $J_s$ is the component of the time-harmonic source current density vector (complex r.m.s. value).

Integrodifferential formulation [4] allows to substitute equation (1), containing two unknown quantities: magnetic vector potential and source current density vector, by the integrodifferential equation containing only unknown magnetic vector potential and measurable total current in the conductor. In the cylindrical coordinate system (axial-symmetric problem) is obtained

$$\frac{1}{\mu} \left( \nabla^2 A - \frac{A}{r^2} \right) - j \omega \gamma A + j \frac{\omega \gamma}{\Delta} \iint_{\Omega} A d\Omega = - \frac{I}{\Delta}$$

(2)

where $\Delta$ is the coefficient is determined by the relation

$$\Delta = r \iint_{\Omega} \frac{1}{r' dr' dz}$$

(3)

The application of the Bubnov-Galerkin method to the equation (2) with some selected in the area $\Omega$ of the base function $\varphi$ leads to the equation

$$\iint_{\Omega} \left( \nabla A \cdot \frac{1}{\mu} \nabla \varphi + \frac{1}{\mu} \frac{A}{r^2} \varphi + j \omega \gamma A \varphi - j \frac{\omega \gamma}{\Delta} \iint_{\Omega} A d\Omega - \frac{I}{\Delta} \varphi \right) d\Omega = \iint_{\Gamma} \frac{\partial A}{\partial n} \varphi d\Gamma$$

(4)

Numerical analysis of the magnetic field in the environment with ferromagnetic materials requires knowledge of the magnetization characteristics of these materials. In the case of induction heating (strong magnetic fields), hysteresis losses can be omitted and a primary magnetization curve [5] can be adopted. The dependence of the magnetic permeability of hardened material (steel 1.1830) on magnetic field strength and temperature was approximated by function $\mu = \mu(H, \Theta)$ (conversion of maximum values into r.m.s. values).

The volume density of power losses in a heating element can be calculated from the dependence

$$\dot{Q} = \omega^2 \gamma AA^*$$

(5)
3. Thermal field

In the model of heat transfer phenomena for solid bodies, the equation is assumed as follows:

$$\nabla \cdot (\lambda \nabla \Theta) - C_{\text{ef}} \frac{\partial \Theta}{\partial t} + \hat{Q} = 0$$

where $\lambda$ [W/(mK)] is the heat conductivity coefficient (generally it depends on temperature), $C_{\text{ef}} = \rho c$ [J/(m$^3$K)] is the effective heat capacity (generally it depends on temperature and contains phase transformation heat), $\rho$ [kg/m$^3$] is density, and $\hat{Q}$ [W/m$^3$] is the volume power density of internal sources.

The propagation of heat in the material during induction hardening is always accompanied by a change in temperature, mainly due to the power delivered to the material, thermal properties of the material, geometric shape and position of the object, and the type of heat exchanged by the surface with the surroundings. The actual temperature distribution in the billet will differ from the distribution of heating power density due to heat transfer and thermal losses caused by radiation and convection. Consequently, not only the distribution of the power density, but also the heating time affects the temperature distribution in the billet.

A strict mathematical description of the physical phenomena that make up the heat exchange with the environment in the induction hardening processes is very complicated and difficult to generalize. Bearing in mind utilitarian considerations, a simplified model of heat exchange by radiation and convection was adopted at work, assuming that the coefficient of heat exchange $\alpha(\Theta)$ depends on temperature.

The total heat flux can be expressed by the sum of heat streams resulting from convection and radiation

$$q = q_k + q_r = (\alpha_k(\Theta) + \alpha_r(\Theta))(\Theta - \Theta_a) = \alpha(\Theta)(\Theta - \Theta_a)$$

The ratio determines $\alpha(\Theta)$ the ability of the heated body to transfer heat through the surface to the environment. The change of this coefficient from temperature was approximated by the formula obtained on the basis of empirical data [5].

The following boundary conditions were assumed for the tested axial-symmetric systems:

$$\lambda \frac{\partial \Theta}{\partial r} = -\alpha(\Theta)(\Theta - \Theta_a) \quad \text{on the external surface}$$

$$\frac{\partial \Theta}{\partial r} = 0 \quad \text{on the axis}$$

The power of internal sources originating from eddy currents is calculated on the basis of formula (5).

4. Phase transformations

It is difficult to predict the structure of steel after various heat treatment variants. For this purpose, only TTT and CCT diagrams can be used [8]. In practice, the cooling patterns are such that the transformation of supercooled austenite takes place under both isothermal and continuous cooling conditions. There are no CCT diagrams for such cooling methods, and their preparation is pointless due to the infinite number of cooling variants. In this situation, only calculations taking into account any heating and cooling methods can be useful for determining the structure of steel after heat treatment. The TTT diagrams for a given steel grade and the temperature description as a function of the time treatment are used as inputs. The method of calculating the shares of individual structures during continuous heating and cooling applied in the work uses data from the isothermal process (TTT diagram) and is based on the principle of additivity. It was used by several authors [6-8] and only its foundations will be presented here. The temperature-warm-up curve was approximated by a sequence of isothermal steps. At each step the share of the new phase is calculated on the basis of isothermal transformation kinetics, which is modeled according to the laws of Avrami and Johnson-Mehl [9]
where: $\eta_d$ - content of the austenite formed, $b(\Theta)$, $n(\Theta)$ - coefficients dependent on temperature.

The form of coefficients $b(\Theta)$ and $n(\Theta)$ was determined from the solution of the system of two equations corresponding to the percentage shares of the phase being created ($1\%$ and $99\%$)

$$n(\Theta) = \ln \left( \frac{\ln(1-\eta_f)}{\ln(1-\eta_s)} \right) \ln \left( \frac{t_s}{t_f} \right), \quad b(\Theta) = \frac{-\ln(1-\eta_s)}{(t_s)^{n(\Theta)}}$$

(10)

where: $t_s$ and $t_f$ - times of beginning and end of transformation at the same temperature. Coefficients $b(\Theta)$ and $n(\Theta)$ must be calculated for each time step.

When the maximum heating temperature does not allow full conversion to be achieved within a certain time, the calculation of the phase contributions during heating is interrupted when the maximum temperature is reached and the volume fraction of the structural component (austenite) less than one is taken into account for the calculation of the phase contributions. The volume shares of structures generated during cooling can be determined, just like the share of structures generated during heating, from the Avrami patterns, taking into account the share of the austenite formed during the heating:

$$\eta_p(\Theta,t) = \eta_d(1 - \exp(-b(\Theta)t^{n(\Theta)}))$$

$$\eta_B(\Theta) = \eta_d(1 - \eta_p)(1 - \exp(-b(\Theta)t^{n(\Theta)}))$$

(11) (12)

The participation of the resulting martensite can be determined on the basis of the empirical equation of Koistinen and Marburger [10]

$$\eta_M = \eta_d(1 - \eta_B - \eta_p)(1 - \exp(-k(M_S - \Theta)^m))$$

(13)

For steel 1.1830 the coefficients occurring in the above formula are correspondingly equal and $k = 0.011$ and $m = 1$.

5. Numerical example

The longitudinal section of the tested system is shown in figure 2. The calculations were carried out for a steel 1.1830 shaft with the following dimensions:

- $R_1 = 0.02 \text{ m}$
- $R_2 = 0.022 \text{ m}$
- $R_3 = 0.024 \text{ m}$
- $R_4 = 0.028 \text{ m}$
- $R_5 = 0.03 \text{ m}$
- $R_6 = 0.07 \text{ m}$
- $L_1 = 0.0325 \text{ m}$
- $L_2 = 0.015 \text{ m}$
- $L_3 = 0.0325 \text{ m}$
- $L_4 = 0.011 \text{ m}$
- $L_5 = 0.005 \text{ m}$

Heating was carried out with stabilization of the inductor current $I = 3 \text{ kA}$ and $I = 3.5 \text{ kA}$ and the frequency $f = 10 \text{ kHz}$. In the first case, the heating time was 6 s, and in the second, 4.2 s. The voltage changes on the inductor, the impedance of the inductor and the power dissipated in the shaft and inductor are shown in Figures 3 and 4 respectively.
Figure 2. Longitudinal section of the considered system.

Figure 3. Changes of: a) voltage on the inductor b) impedance of the inductor-shaft system in the heating process.

Figure 5 shows the distribution of the relative current density of the inductor at the beginning and end of the heating process. The presented diagrams show that the change in the magneto-electric parameters of the charge in the heating process has a small effect on the current density distribution in the inductor. In the Figure 5 the effect of proximity bringing the inductor wires is clearly visible. The skin effect and the ring shape of the inductor contribute to the inhomogeneous current density distribution in the inductor. The omission of skin effect and proximity effect in the inductor in the calculations leads to significant errors in the calculation of the temperature field. Figure 6 shows the temperature fields in the shaft after heating. Longer heating with less power results in a deeper and more homogeneous hardened zone (figure 7).
Figure 4. Changes of power during heating: a) in the shaft, b) in the inductor.

Figure 5. The distribution of the relative current density in the inductor at the moment of a) switching on, b) switching off the power in the case of I = 3.5 kA.

Figure 6. Distribution of temperature along the shaft at the moment of switching off the power in the case of: a) I = 3 kA, b) I = 3.5 kA.

Figure 7. Distribution of the hardened layer ($\eta_{M} \geq 0.5$, cooling in water) in the case of: a) I = 3 kA, b) I = 3.5 kA.
In future research, the authors will focus on an attempt to build an induction hardening model that allows to determine the method of powering the inductor in order to obtain the assumed hardened layer.

6. References

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