The proposed modification of the computational algorithm nevertheless enhances the resolving power of the method and can be used to solve inverse problems of thermal conductivity with coefficients.

NOTATION

T_0, initial temperature; T_1 and T_2, temperatures of the isotropic and orthotropic regions, respectively; t, time; z, r, cylindrical coordinate system; a_1, a_2, and a_3, thermal diffusivities of the isotropic and orthotropic materials; q_0, thermal flux density; $R(T_2)$, contact thermal resistance; R_1 and R_2, inner and outer radii of the isotropic region; L, length of the complex cylindrical region; λ_1, thermal conductivity of the isotropic material; and λ_2 and λ_3, principal thermal conductivities of the isotropic material.

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EFFICIENT ORGANIZATION OF THE QUENCHING OF ROLLED PRODUCTS ON THE BASIS OF SOLUTION OF AN EXTERNAL INVERSE HEAT-CONDUCTION PROBLEM

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Results are presented from mathematical modeling of the thermal interaction of sprayed liquid with a surface. The results made it possible to determine the depth of hardening of a flat-rolled product.

One of the most promising methods of heat treatment for strengthening rolled products is intensive cooling of a hot surface with a disperse liquid produced by flat-jet nozzles [1]. Of particular importance in this operation is the cooling rate $\Delta T/\Delta\tau$ in the temperature range from the critical point corresponding to the beginning of austenite decomposition to the temperature at which its stability is minimal, i.e., within the range 850-450°C. It is evident that mathematical modeling of the quenching process is impossible without reliable information on heat-transfer conditions and thermophysical characteristics (TPC) of the metal. A method was described in [2] to determine boundary conditions in the cooling of a surface by a sprayed liquid on the basis of solution of an external inverse heat-conduction problem (ICP) with the use of an iterative filter. The same study demonstrated the possibility of simultaneous identification of the TPC and boundary conditions by the solution of a combination ICP.

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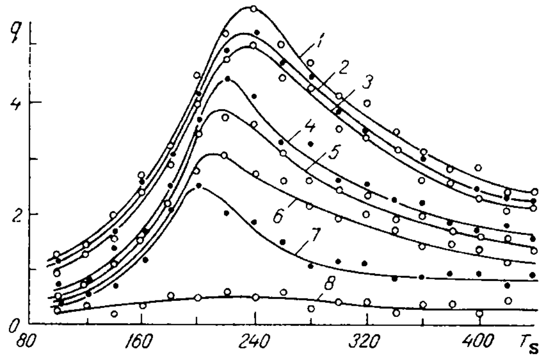


Fig. 1

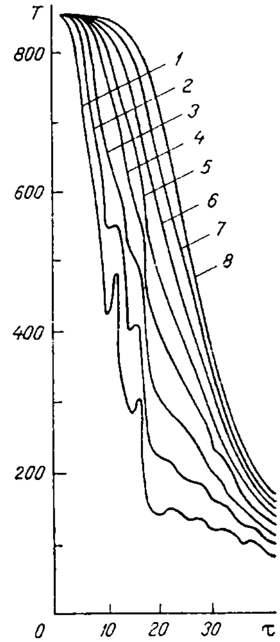


Fig. 2

Fig. 1. Dependence of heat flux on surface temperature and local spray density: 1) g = 147.2 kg/(m²·sec); 2) 98.6; 3) 76.3; 4) 45.4; 5) 22.7; 6) 8.9; 7) 6.1; 8) 0.78. T_s, °C.

Fig. 2. Temperature field of the cross section of a plate, δ_p = 50 mm, $\bar{g}$ = 30 kg/(m²·sec): 1) z = 0 mm; 2) 2.5; 3) 5.0; 4) 7.5; 5) 10.0; 6) 12.5; 7) 20.0; 8) 25.0. τ, sec.

The present study is a continuation of [2] in the region of practical application. It explores the possibilities of the method for the solution of specific problems in technical thermophysics.

Figure 1 shows the dependence of the heat flux q on the temperature of the surface T_s with different spray densities g. The results were obtained by solving a set of external ICP through the use of the above method. The curves $q(T_s, g)$ were approximated by relations of the form

$$\begin{aligned} q &= 0{,}63\cdot 10^{-6} g^{0,14} T_s^{2,83} \quad (\text{at } 100\,^\circ\mathrm{C} \leqslant T_s \leqslant T_{cr}), \\ q &= 7{,}1\cdot 10^{3} g^{0,32} T_s^{-1,58} \quad (\text{at } T_{cr} \leqslant T_s \leqslant 450\,^\circ\mathrm{C}), \\ q &= 0{,}456 g^{0,32} \quad (\text{at } T_s \geqslant 450\,^\circ\mathrm{C}), \end{aligned} \tag{1}$$

where $T_{cr} = 205.2\ g^{0.041}$ is the critical surface temperature corresonding to the heat-flux maximum.

With the known relation (1) and the rate $\Delta T/\Delta\tau$ from thermokinetic curves [3], necessary spray density can be established from analysis of the temperature field of a product determined by the solution of the direct heat-conduction problem.

In methods currently used to calculate the thermal state of a flat-rolled product [4, 5], along with assumptions of unidimensionality of the temperature field and independence of the thermophysical characteristics of the metal on its temperature, it is customary to assume that the spray density g at any moment of time is the same at each point of the surface in the cooling zone of the plate and is equal to a certain mean integral value. This assumption in turn reduces to the fact that the heat flux within this zone is assumed to be the same over the entire surface. However, in reality, the quenching operation as performed using flat-jet nozzles occurs in such a way that each element of the surface in the direction of motion experiences a local spray density which is variable over time. Here, the law of change in $g(x, \tau)$ and, thus, $q(g, x, \tau)$ is determined by the regime-geometric charactersitics of the coolant system.

Using information obtained in [6] on the distribution of local spray density in a flat-jet nozzle and employing Eqs. (1), we can determine the two-dimensional nonsteady temperature field of a plate of thickness δ_p, moving at the velocity W_p, while it is cooled from above and below. Here, the boundary conditions for heat transfer are determined both by the change in the local temperatures of the surface and by the local spray densities.

In accordance with the actual cooling conditions, we assume that a change in local spray density occurs only along the plate, i.e., along the x axis. Then, with allowance for the equation expressing the distribution of spray density [6], we have

$$g(x) = g_{\max} \exp[-(x - x_{\max})^2/2x_{\max}\sigma_x^2], \tag{2}$$

where g_{max} is the maximum spray density at the center of the jet; x_{max} is the distance from the center of the jet to its boundary in the horizontal plane, located a distance H along a normal from the mouth of the nozzle; σ_x is the parameter of the distribution function which is constant for a wide range of variation of the regime-geometric characteristics of flat-jet nozzles and is equal to 0.43 [6]; $g_{max} = 8.6\ K^{1.2}\xi^{-0.68}$, $K = F(2\Delta P\rho_q)^{0.5}H^{-2}$; F is the cross-sectional area of the nozzle; ΔP is the pressure drop of the liquid; ρ_q is the density of the liquid; ξ is the form parameter of the nozzle.

With a prescribed plate velocity W_p and its corresponding displacement Δx, the functions $g(x, \tau)$ and $q(g, T_s, \tau)$ are easily determined by means of Eqs. (1) and (2).

Ignoring the heat transfer on the unsprayed end surfaces and examining only 0.5 δ_p in connection with the symmetrical cooling conditions above and below the plate, we can write:

$$\frac{\partial}{\partial x}\left[\lambda(T)\frac{\partial T}{\partial x}\right] + \frac{\partial}{\partial z}\left[\lambda(T)\frac{\partial T}{\partial z}\right] = C_V(T)\frac{\partial T}{\partial \tau}, \tag{3}$$

$$\frac{\partial T}{\partial \tau} = 0 \quad \text{at} \quad x = 0 \text{ and } x = L, \tag{4}$$

$$-\lambda(T)\frac{\partial T}{\partial z} = q(T_s, \tau) \quad \text{at} \quad z = \delta_p, \tag{5}$$

$$\frac{\partial T}{\partial z} = 0 \quad \text{at} \quad z = 0. \tag{6}$$

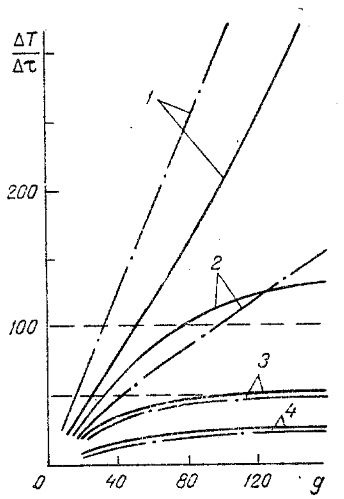
Here, (4) and (6) are the boundary conditions for heat transfer on the unsprayed and sprayed surfaces; z is the coordinate over the thickness of the plate.

The initial temperature distribution of the plate was assumed to be uniform $T(x, z, \tau)|_{\tau=0} = T_0$. The temperature field was determined by numerical solution of the direct heat-conduction problem using the finite-difference method (implicit scheme).

Preliminary calculations showed that, for all of the investigated variants of regime-geometric parameters of the plate-cooling process, the space discretization may be chosen equal to $h_1 = L/4$ along the length L and $h_2 = \delta_p/20$ over the thickness of the plate. Further discretization increased the accuracy of the results by no more than 1%. The time discretization was determined by the velocity of the plate, the quantity x_{max}, and the total time of interaction of the surface with the cooling system.

As an example, Fig. 2 shows the results of calculations of the temperature field in the cross section of a steel 10KhSND plate. The results reflect the transience of the spraying operation. Here, $\tau = 0$ corresponds to the moment when the leading edge of the plate enters the cooling zone, while $\bar{g}$ is the mean integral value of spray density calculated from Eq. (2) for the specific regime-geometric characteristics of the flat-jet nozzle. Numerous calculations showed that, with a prescribed plate velocity W_p and $\bar{g}$, the relations $T(z, \tau)$ remain nearly the same for any cross section and are shifted only by a certain time interval x/W_p. The characteristic "valleys" in the curves $T(z,\tau)$ correspond to the moments the cross section of the plate being examined passes the maxima in the distribution $g(x, \tau)$. It is evident that a change in spray density has less effect on the temperature field with increasing distance from the plate surface.

To make a comparative evaluation of the effect of the transience of the spraying operation, we performed a series of calculations with the usual assumption that the distribution of the unit flow rate of the liquid was uniform.



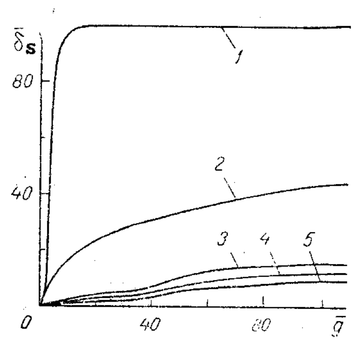


Fig. 3 Fig. 4

Fig. 3. Effect of spray density on cooling rate, δ_p = 50 mm: 1) z = 0 mm; 2) 2.5; 3) 5.0; 4) 7.5. g, kg/(m^2·sec); $\Delta T/\Delta\tau$, °C/sec.

Fig. 4. Relative depth of hardening $\bar{\delta}_s$ in relation to spray density $\bar{g}$: 1) δ_p = 8 mm; 2) 20; 3) 30; 4) 40; 5) 50.

The resulting relations T(z, τ) made it possible to calculate the distribution of the mean cooling rate $\Delta T/\Delta\tau$ over the thickness of the plate in the temperature range 850-450°C. These relations correspond to the region in which strutural transformations take place in the metal during quenching.

To illustrate the effect of the unsteady nature of the spraying process, Fig. 3 shows the distribution of the rate of change in temperature $\Delta T/\Delta\tau$ over the plate thickness with different values of mean integral spray density. The solid curves correspond to the calculation variant performed with the assumption of a uniform distribution of local spray density. The dot–dash curves show the results of calculations performed using the distribution function g(x, τ). Analysis of the thermokinetic curves [3] shows that the minimum cooling rate at which a stable martensitic structure can be obtained is 100-50°C/sec, depending on the grade of steel. This rate corresponds to the boundaries represented by the dashed lines in Fig. 3.

It is easy to use the curves to determine the most important charcteristic of the quenching process – depth of hardening δ_s. Thus, for example, with δ_p = 50 mm, g = 80 kg/(m^2·sec), and the allowable value $\Delta T/\Delta\tau$ = 100°C/sec, the quantity δ_s = 2.5 mm. At $\Delta T/\Delta\tau$ = 50°C/sec, depth of hardening reaches 5 mm.

Comparison of the relations for cooling rate obtained in the two above-examined calculation variants shows that the simplified prescription of the boundary conditions could result in an error from 40 to 80% in the determination of the required spray density (particularly for thin plates).

The above method of calculating the distribution of cooling rate through the thickness of a plate allowed us to find the cooling rate $\Delta T/\Delta\tau$ as a function of spray density g and the distance between the axes of adjacent nozzles a_x for plate layers located different distances from the surface of the plate. In the general case, the distance between the axes of adjacent nozzles in a cooling system will be assigned in the form $a_x = nx_{max}$, where n = 1, 2, 3, ... On the unsprayed sections (when $a_x > 2x_{max}$), where film flow of the liquid takes place, we take $g = 0.1\, g_{max}$. This corresponds to the "three sigmas" law of normal distribution for local spray density [6].

In each specific variant corresponding to the prescribed plate thickness δ_p, the operating regime of the nozzles was assigned so that the spray density $\bar{g}$ remained the same with a change in a_x. The calculations were performed for the nozzle most commonly used in rolling-mill practice [1]. The geometric parameters of this nozzle F = 104.2 mm^2, ξ = 0.225. The pressure drop in the liquid was varied within the range $0.10 \le \Delta P \le 0.25$ MPa.

The calculations established that with a plate thickness $8 \leq \delta_p \leq 50$ mm and $x_{max} \leq a_x \leq 3x_{max}$, the effect of the distance between the axes of the nozzles a_x on the relative depth of hardening $\bar{\delta}_s = \delta_s/\delta_p$ can (with an error of 1.0-1.5%) be ignored. This allowed us to obtain a generalized relation $\bar{\delta}_s(\delta_p, \bar{g})$. This relation is shown in Fig. 4 for steel 10KhSND (with the condition that the cooling rate $\Delta T/\Delta\tau$ necessary to strengthen the steel is 60°C/sec).

It is evident from the figure that each plate thickness corresponds to a certain threshold value of spray density g_{thr}. After the threshold value is reached, a further increase in the rate of flow of the sprayed liquid per unit of surface leads to a negligible increase in the relative depth of hardening $\bar{\delta}_s$. The practical value of the established relations $\bar{\delta}_s(\delta_p, g)$ consists of the possibility of selecting an expedient value of spray density and thus minimizing the consumption of coolant water (which is in short supply in several regions of the country) and, as a result, also minimizing the consumption of electric power for the pumps.

NOTATION

q, heat flux; g, spray density; T_s, temperature of surface; T_{cr}, critical temperature of surface; $\Delta T/\Delta\tau$, cooling rate; L, δ_p, length and thickness of plate; W_1, velocity of plate; $\lambda(T)$, thermal conductivity of the steel; $C_V(T)$, volume heat capacity; x, y, z, space coordinates; δ_s, depth of hardening; a_x, distance between axes of nozzles.

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