

Chapter 2

Input Parameters of the Coating Process

2.1 Stages of the Arc Spraying Process

To study the input parameters, it is rational to divide the process of AS into several stages (Fig. 2.1). They differ in the effect caused by input parameters on properties of atomized material, which is resulted in final coating properties.

The stages have the following peculiarities. Heating and melting of metal take place on the end face of electrode. It is occurred due to arc heat and joule heat that exuded due to the current run through the electrodes as well as via chemical reactions between the metal and the carrier gas components.

Supplementary liquid metal is fractured in the arc-heating zone into separate particles under the impact of gas-dynamic head of the carrier gas and the electro-magnetic forces. The latter occurs because of the current run. The exchange of thermal, kinetic energy, and chemical reaction of the liquid metal drops with carrier gas and surrounding atmosphere takes place along the spraying distance. These processes determine the type of interaction “atomized metal–substrate” while formation of coating. Stages have overlapping zones due to continuous change of the parameters. However, for the sake of modeling it is preferential to describe the stages as discrete objects followed by the analysis of the accuracy of the taken assumption.

Accuracy and adequacy of the model of thermal, chemical gas-dynamic, and electro-dynamic ongoing processes are to be evaluated by correlating results of the model analysis with the data about metal coating properties. Effective development of equipment and technological processes is possible based on the modeling and coating properties studying.

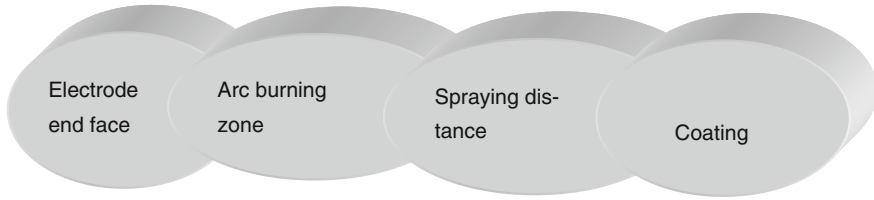


Fig. 2.1 Stages of coating process in AS

2.2 Temperatures, Velocities, Gas Atmosphere Components—Initial Values

At PS, the temperature of the arc in its axis reaches 50,000 K [1]. There is no such purposeful force compression of the arc at AS comparing to PS. Consequently, temperatures' level here is lower. The closest analog of this type of arc and gas burning is the process welding in atmosphere of air, where the arc temperature is 5000–6000 K [2–4].

At AS, the gas carrier temperature when it reaches the arc burning is varied from 300 K, in case of using compressed air, to the several thousand degrees, in case of using combustion products of various hydrocarbons as a carrier gas. Fuels' burning temperature depends on the type of oxidant and the chemical bond of characteristics (Table 2.1).

The propane is the most favorable for AS among hydrocarbon fuels. First, the use of propane is well compatible with wide spread gas-welding and cutting technological processes, and therefore, it does not require any significant starting costs. Natural gases with similar characteristics require a trunk pipeline, which quite often might be an obstacle for on-site applications. Second, propane is safer than acetylene which is also widely used gas in a welding processes. It is determined by the fact that propane inflammation velocity (1.31 m/c) is four times lower than acetylene inflammation velocity [5]. Therefore, the danger of the backfire decreases. It is significant due to the presence of melted particles as they can perform multidirectional high-velocity fly away. To reduce the costs and to secure the technological process in case of high flow rate of a carrier gas, it is highly recommendable to use a compressed air as an oxidant.

Table 2.1 Burning temperatures of the hydrocarbon fuels in the stoichiometric mixture [5, 6]

	Oxidant	Gas			Liquid		
		Methane CH ₄	Acetylene C ₂ H ₂	Propane C ₃ H ₈	Ethyl Alcohol C ₂ H ₆ O	Patrol C ₈ H ₁₈	Kerosene C _{7,21} H _{13,5}
T, K	Air	2148	2598	2198	2090	2243	2203
	Oxygen	2673	3423	2973	3053	2773	2673

Let us evaluate the maximum gas temperature for AS. The initial gas temperature T_{g0} that triggers its interaction with atomized metal will differ from the initial temperature of a carrier gas T_{g0}^{ucx} :

$$T_{g0} = T_{g0}^{ucx} - \Delta T_k + \Delta T_H \quad (2.1)$$

where ΔT_k corresponds to the gas cooling due to the heat irradiation toward the combustion chamber walls (by using combustion products of the “fuel-oxidant”; ΔT_H corresponds to the rising of the temperature due to gas heating while transiting through the electric arc.

Gas cooling calculation in the combustion chamber is to be done according to the scheme of recuperative parallel flow heat exchanger with the heat irradiation through monolayer cylindrical wall [7]. Air is used as a cooling heat carrier (coolant). Change of the temperature is:

$$\Delta T_k = \frac{(T_{zop} - T_g) \cdot \left(1 - \exp \left(- \left(1 + \frac{G_1 \cdot c_{p1}}{G_2 \cdot c_{p2}} \right) \cdot \left(\frac{k \cdot F}{G_1 \cdot c_{p1}} \right) \right) \right)}{\left(1 + \frac{G_1 \cdot c_{p1}}{G_2 \cdot c_{p2}} \right)}, \quad (2.2)$$

where $k = 1 / \left(\frac{1}{\alpha_1 \cdot d_1} + \frac{1}{2\lambda} \cdot \ln \frac{d_2}{d_1} + \frac{1}{\alpha_2 \cdot d_2} \right)$; $\alpha = Nu \cdot \lambda_g / d$; $Re = W_g \cdot d / \nu$; $Nu_1 = 0.18 \cdot Re_1^{0.8}$; $Nu_2 = 0.245 \cdot Re_2^{0.6}$; T_{zop} is fuel burning temperature, K; T_g is initial temperature of cooling air, K; $G = \rho_g \cdot W_g \cdot f$ is mass flow rate of a heat carrier, kg/s; ρ_g is gas density, kg/m³, W_g is gas velocity m/s, c_p is gas thermal capacity, J/(kg K); F is surface area of heat emission, m²; α is gas heat emission factor (coefficient), W/(m² K); ν —kinematic viscosity factor, m²/s; Nu —Nusselt number; Re —Reynolds number; combustion chamber operation parameters: f is outlet duct, m²; k is coefficient of heat transfer through the wall W/(m² K); δ is wall thickness, m; λ is wall thermal conductivity, W/(m² K); d is diameter, m, indexes “1”, “2” refer to the combustion products and cooling air, consequently.

Calculations (Table 2.2) were done for a case, when compressed air and combustion products of propane–air mixture are used as a carrier gas.

Combustion chamber is constructed according to the arc spraying gun [8], W_g is adopted due to the equality relations of carrier gas and outlet of combustion outlet

Table 2.2 Calculating results of combustion products cooling from the combustion chamber walls (line 1) and cooling air heating (line 2)

	T_0^{ucx}	F	f	ρ_g	W_g	λ_g
1	2100	5.2×10^{-3}	3.8×10^{-5}	0.17	100	0.149
2	300		6.3×10^{-6}	1.18	300	0.026
	δ	λ	ν	c_p	α	ΔT_k
1	0.001	22	1.5×10^{-5}	1400	85600	224
2			4.8×10^{-4}	1000	372	92

chamber and nozzle; Nu_1 is considered as for combustion flow in a tube when turbulence occurs; Nu_2 is considered as for that transfer of external tube flow.

Thermal-physics characteristics of gases (ρ_g , λ_g , v , c_p) are considered in accordance with [9], material used for combustion chamber construction—as for [10].

To evaluate the heating of a gas while passing through the arc, let us assume the heating of the gas is a result of the heat emitted by arc column minus radiation loss. In this case:

$$\Delta T_H = (q_\partial - q_u)/\alpha_T \text{ is gas heating from the arc;} \quad (2.3)$$

$$q_\partial = I \cdot E / (\pi \cdot d) \text{ Is heat emitted in the arc column;} \quad (2.4)$$

$$q_u = \beta \cdot \sigma \cdot T_\partial^4 \text{ is radiation heat loss of the arc column;} \quad (2.5)$$

$$\alpha_T = Nu \cdot \lambda_\partial / d, \quad (2.6)$$

$$Nu = 2 + 0.459 \cdot Re^{0.55} \cdot Pr^{0.333} \quad [11], \quad (2.7)$$

$$Re = v \cdot \rho \cdot d / \eta \quad (2.8)$$

In Eq. (2.4) electric field intensity $E = 2.5$ V/mm is considered according to [2, 11]. This corresponds to the arc length $l_\partial = 4$ mm and to a voltage drop in the arc column $U_c = 10$ V. According to Eq. (2.5), we assume the arc column to be opaque and completely black, with respect to the assumption made by Leskov [2]. In the Eq. (2.8), we consider the electrode's diameter size equals the diameter of the arc column.

According to the results after applying Eq. (2.1) with the data from Tables 2.2, 2.3 the temperature of carrier gas will be approximately 450 K. If using combustion products of the propane–air mixture the temperature will be 2200 K.

For the gases used in the arc spraying the most thermodynamically expected reactions are the reactions of thermal dissociation of the molecules on atoms and radicals: H_2 , O_2 , H_2O



The temperature of plasma jet formed during gas heating by plasma arc is 3500–16,000 K depending on the type of plasma forming gas [15–17] in the outlet nozzle. Calculations show that temperatures are responsible for greater share of the ionization products and dissociation gases (Fig. 2.2.) According to the thermodynamical calculations for these gases (Table 2.4.), the presence of the sizable

Table 2.3 Gas heating while passing through the electric arc

Parameters	Gas type	
	Air	Combustion products
Arc temperature T_o , K [2]	6000	
Arc current I , A (Typical mode)	200	
Electric field intensity, E , V/mm	2.5	
Arc column diameter, d , mm	2	
Arc column thermal conductivity, λ_o , W/(m K) [12]	0.5	1
Emissivity factor, β	1	
Stefan-Boltzmann constant, σ_o , W/(m ² K ⁴) [2]	5.67×10^{-8}	
Gas velocity, W_g , m/s [13]	300	900
Gas density, ρ_g , kg/m ³ [9]	1.29	0.17
Dynamic viscosity, η , Pa s [9, 13]	1.7×10^{-5}	5×10^{-5}
Prandtl number, Pr , [14]	0.7	
Reynolds number, Re	41404	3750
Nusselt number, Nu	146	40
Heat transfer coefficient, α_m , W/(m ² K)	3.8×10^4	2.6×10^4
Radiation heat loss of the arc column, q_o , W/m ²	8×10^7	
q_u , W/m ²	7.3×10^7	
Gas heating from the arc, ΔT_n , K	160	239

Table 2.4 The equilibrium constants of dissociation reactions of gases (k_i) [18]

T, K	lg K_i			
	Reaction (2.9)	Reaction (2.10)	Reaction (2.11)	Reaction (2.12)
1000	-17.2924	-19.6128	-10.545	-17.213
2000	-5.5816	-6.3553	-3.400	-5.719
3000	-1.6974	-1.8984	-1.002	-1.822
4000	0.4005	0.3393	–	0.149
5000	1.6120	1.6846	–	–
10000	4.0393	4.400	–	–

amount of the atomic particles and radicals shall be considered when the temperature of the atmosphere gas is >2500 K.

Comparing the calculation results applying Eq. (2.1) and data on gas dissociation (Fig. 2.2, Table 2.3) showed that the content of atoms and radicals is negligible and therefore the molecule dissociation can be ignored. This simplifies the calculation of the atmosphere content at AS compared to PS.

Initial gas flow velocity is determined by nozzle geometry, pressure, and gas temperature. In modern arc spraying guns, transonic velocity of gas is provided. Gas temperature has an indirect influence on the dynamic pressure of the jet $(\rho_g \cdot W_g^2)/2g$. Change in temperature causes the change in gas density and sound

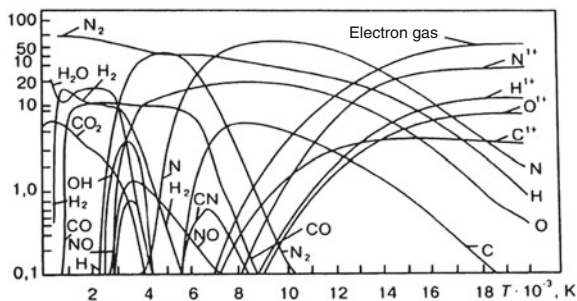


Fig. 2.2 Function of equilibrium plasma composition of combustion products of neutral mixture “methane-air” on temperature [17]

velocity rate. Velocity of sound ($a_{\text{св}}$) determines Mach number, which influences the gas velocity that by turn depends on gas flow temperature:

$$a_{\text{св}} = \sqrt{k \cdot g \cdot R \cdot T_g}, \quad (2.13)$$

where ρ_g is gas density, kg/m^3 ; W_g is gas velocity, m/s ; T_g is gas temperature, K ; k is adiabatic index (depends on temperature and gas composition); g is standard acceleration of gravity, m/s^2 ; R is universal gas constant.

For instance, for nozzle providing gas flow rate (0.8–0.9 M), in case of cold air $W_g \sim 300 \text{ m/s}$, propane–air mix combustion in case of $W_g \sim 900 \text{ m/s}$. The density of cold and hot air is 0.17 and 1.29 kg/m^3 consequently [9].

2.3 Metal Heating and Melting on the End Face of Electrode

Metal heating and melting on the end face of electrode for AS are similar to arc welding. They are provided by the heat from chemical reactions, electric arc, Joule’s heat. Heat retrieves when the electric current passes through metal wires. Metal temperature (steel) on the end face of the electrode when welding is 2000–2700 K, although the most characteristic temperature is closer to 2500 K [4]. In AS researches, it is viewed similar to welding intervals—in between the boiling and melting points of the material used for electrodes [19, 20]. At AS the most reliable experimental metal temperature values on the end face of electrode are 2323–2573 K. The results were obtained via laser spectroscopy [21]. As shown by W. Tillmann et al., [103], and A.P. Newbery et al., [104], particle temperature is 2700 K near arc burning zone at high voltage (34 V) and 2500 K at 28 V. However, higher voltage leads to intensive burning of atomized metal [34], so more preferable is voltage 28 V. Although if the gap between the electrodes is small (nearly 0.1–1.0 mm), the results of the measurement refer to the metal drops escaped from the electrodes according to Royanov

[22]. Influenced by high-velocity airflow their temperature is lower than those at the surface of the end face. For our calculations, let us assume the temperature of the steel end face of the electrode is 2500 K.

There are some differences in the gas flow blow-off of the electrodes. For arc welding, a lamellar gas flow is more preferable. Transition to turbulent mode lowers the arc stability. In this case, the gas flow velocity which is flowing around liquid metals is 10–30 m/s and gas consumption does not exceed 1.2 m³/h [23]. During AS process, the gas flow of high kinetic energy and high turbulence is forcedly formed. Duct gas flow rate is 250–1500 m/s and its consumption is between 90 and 150 m³/h. That is, by 1–2 orders higher as compared to arc welding [24].

The direction of gas flow influence on electric arc also changes compared to welding. When welding gas flow goes along the electrode axe, whereas during AS, the latter forms an angle 15–300 toward the electrode axe. All this leads to the reinforced convective heat output of electrode extension (the length of the wire from the current-carrying contact tip to the fused end). This heat output should be taken into account when calculating the heat balance in contrast to welding [10, 25].

Such calculation seems interesting if using enlarged electrode extension, when wire notably heats from the passing current. Researches of arc welding process with enlarged electrode extension showed that it leads to the increasing of the metal fusing efficiency, rising of the average particles size, and decrease of spattering [4, 23]. Therefore, AS is also arc-type process, we can assume that the consequences of the enlarged electrode extension at AS will be similar to the shown above for welding. Total power density obtained by the electrode (q_{Σ} , W/m³) can be retrieved from the heat power equation:

$$q_{\Sigma} = q_{\text{мок}} + q_m - q_{\text{конв}} \quad (2.14)$$

where $q_{\text{мок}}$ is heat generated in the electrode due to passing current, q_m is electric arc heat, $q_{\text{конв}}$ is convective heat losses.

Let us expand the equation:

$$\begin{aligned} c\gamma v_n \frac{dT}{dL} = j^2 r + \frac{c\gamma v_n (T_k - T)}{L - L_x} \\ \times \exp\left(\frac{c\gamma v_n (L - L_x)}{\lambda}\right) - \left(\frac{4\alpha(T - T_g)}{d}\right) \end{aligned} \quad (2.15)$$

where j is current density, A/m²; r is specific electric resistance, ом м; γ is material density of solid electrode, kg/m³; v_n is wire feed rate, m/s; L , L_x are electrode extension (general and actual value), m; T , T_k are particle temperatures (actual value and on the electrode end face measurement), K; T_g is gas temperature, T_g is λ -conductivity coefficient of metal droplet, W/(m k); λ is surfacing heat transfer coefficient calculated according to Nusselt number, W/(m² K); d is diameter of the electrode, m.

Thermal heating of the electrode by the arc active spots due to rapid heat supply can be calculated according to the equation of heat spreading from the continuously functioning flat source in the rod moving with a constant velocity [10].

Temperature dependences of specific electrical resistivity (r), specific heat metal capacity (c) were approximated by the following equation type: $y = A_1 \cdot T^2 + A_2 \cdot T + A_3$ (Table 2.5).

Gas velocity at the electrode meeting point is 300 m/s. Calculating results are shown in Fig. 2.3. Qualitative ratio differs from the data, acquired in arc welding [23] in the following: the influence of heat of electrode extension from the arc heat starts from 3 mm distance, which is 5–8 times more compared to AS.

This is due to the physical characteristics of the AS, where most of the molten metal is blown away by gas flow from the electrode end-face.

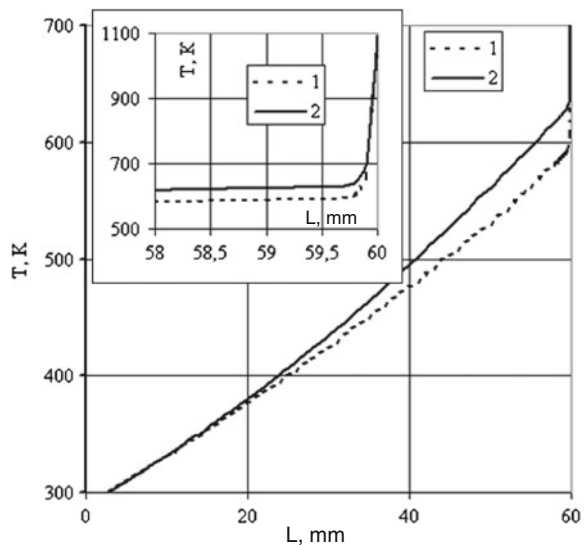
Moreover, an intensive gas cooling of the electrode leads to a decrease in electrode extension heating by 20–40 K in comparison to welding. Wherein the heat transfer coefficient from the surface increases to $\alpha = (860\text{--}1500) \text{ W}/(\text{m}^2 \text{ K})$, which is two orders more than in welding, where $\alpha = (0.5\text{--}5) \text{ W}/(\text{m}^2 \text{ K})$ [27].

Table 2.5 Temperature dependences of the electrode parameters [23, 26]. AWS70S-6 solid wire is taken

Parameter	Temperature interval, K	A_1	A_2	A_3
$c, \text{ J}/(\text{kg K})$	300–1033	0.0021	−1.8221	866.28
	1033–1184	0.0429	−99.038	57866
	1184–1809	0.0004	−0.86	1118.3
$r \times 10^8, \Omega \text{ m}$	300–800	9.57×10^{-5}	2.41×10^{-2}	24.41
	>800	—	2.45×10^{-2}	89.98

AWS70S-6 wire is taken [23, 26]

Fig. 2.3 The temperature distribution in the electrode extension at AS. 1 is accounted for heat transfer from the electrode; 2 is accounted for heat transfer from the surface of the electrode is not accounted



Currently, the use of core wires (CW) as an electrode material is expanding. However, they differ by the unevenness of thermal and physical properties of the cross-section, which drastically changes the nature of the thermal and physical-chemical processes at the end face of the electrode. Thus, for example, the study of the coating produced by CW spraying showed that the charge could be partially separated from the electrode end face in a non-fused state [28].

We introduced our model of heat distribution in CW from arc heating at AS. Obtained simulation data can be used independently for temperature prognoses of particles formed by melting of the wire, as well as a source of initial data for describing physical and chemical interactions. Analysis of ongoing processes at CW spraying will significantly reduce the volume of study, which is necessary to develop the new CW.

Heat distribution in the end face area of CW is considered as an axe-symmetrical task and described by the differential equation of thermal conductivity

$$\partial T / \partial t = (\lambda / c \cdot \lambda) \cdot (\partial^2 T / \partial x^2 + \partial^2 T / \partial y^2), \quad (2.16)$$

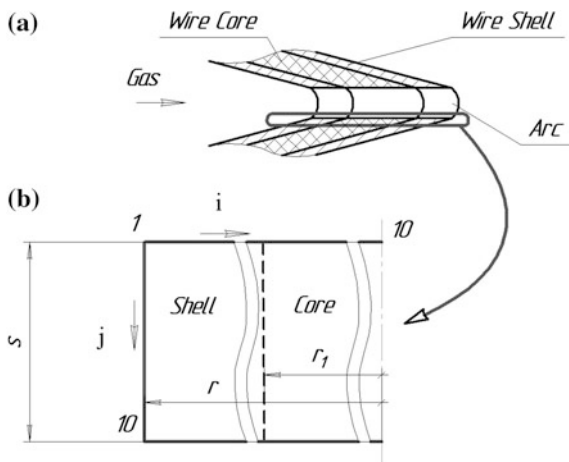
where T —temperature, K; t —time, s; λ —coefficient of thermal conductivity, W/(m K); c —specific thermal heat capacity, J/(kg K), γ —density, kg/m³; x, y —coordinates of cross-section and generating line of CW, respectively, m.

The task was solved using explicitly difference method. End face zone was divided on the radius (r) by coordinate lines $i = \text{const}$ with a step $\Delta r = r/10$, and to the depth (s) with coordinate lines $j = \text{const}$ with a step $\Delta s = s/10$ (see Fig. 2.4).

Within an element, the temperature is considered to be constant and refers to its middle. The following equation was adopted for the obtained elements of the mesh:

$$\partial T / \partial t \cong \Delta T / \Delta t = (T_{ij}^{k+1} - T_{ij}^k) / \Delta t \quad (2.17)$$

Fig. 2.4 End face electrode area model; *a* end face sketch at arc spraying; *b* scheme of heat propagation in electrode end face accepted



A differential, for instance on r , can be expressed as: $\partial T / \partial r \cong \Delta T / \Delta r$,

$$\begin{aligned} \partial^2 T / \partial r^2 &= \frac{\partial}{\partial r} \cdot \left(\frac{\partial T}{\partial r} \right) \cong \frac{\partial}{\partial r} \cdot \left(\frac{\Delta T}{\Delta r} \right) \cong \frac{1}{\Delta r} \left(\left(\frac{\Delta T}{\Delta r} \right)_+ - \left(\frac{\Delta T}{\Delta r} \right)_- \right) \\ &= \frac{1}{\Delta r} \cdot \frac{\left(T_{i+1,j}^k - T_{i,j}^k \right) - \left(T_{i,j}^k - T_{i-1,j}^k \right)}{\Delta r} = \left(T_{i+1,j}^k - 2T_{i,j}^k + T_{i-1,j}^k \right) / \Delta r^2 \end{aligned} \quad (2.18)$$

Differential with respect to s is expressed similarly. Then for the wire section under consideration, difference equation which approximates differential Eq. (2.16) can be represented as follows:

$$T_{i,j}^{k+1} = T_{i,j}^k + (\lambda / c \cdot \gamma) \cdot \Delta t \cdot (1 / \Delta r^2 \cdot B_1 + 1 / \Delta s^2 \cdot B_2), \quad (2.19)$$

where $B_1 = T_{i+1,j}^k - 2T_{i,j}^k + T_{i-1,j}^k$; $B_2 = T_{i,j+1}^k - 2T_{i,j}^k + T_{i,j-1}^k$; κ , Δt are a step number and time step. The time interval is divided into 10^4 steps.

When solving (2.19) the equation, a restriction arising from the convergence conditions for the selected method is imposed:

$$(\lambda / c \cdot \gamma) \cdot (1 / \Delta r^2 + 1 / \Delta s^2) < 1/2 \quad (2.20)$$

Since the conductivity of the charge is 1–2 orders lower than that of the shell, it is assumed that the active arc spots are located at the ends of the shells. The surface temperature of the shell at the end is assumed 2500 K. Overheating of the electrode end face to such temperature regarding the melting point (T_m) is typical feature to arc processes [4]. It is assumed that the surface of the charge receives the heat radiated from the arc column and heated up to 1300 K.

Heat distribution from the end face onto downward direction is affected by thermal exchange “shell-gas” and “shell-charge”. Heated by the arc, the shell gives off the heat to the streamline gas flow and the charge.

When calculating the heat “shell-gas”, the heat transfer coefficient of the surface is defined by the equation

$$\alpha_1 = Nu \cdot \lambda / 2r \quad (2.21)$$

Nusselt number is calculated according the dependency used for the cross-flow tubes calculation [7]:

$$Nu = 0.245 \cdot Re^{0.6} \quad (2.22)$$

According to the calculations, when using air and propane–air mixture as the carrier gas, the value of α_1 is in a range of 2000–4000 W/(m² K), depending on changes in transfer characteristics of the gas and its velocity. The heat transfer coefficient on the border “shell-charge” is adopted as $\alpha_2 = 1400$ W/(m² K) [29].

The change in temperature on the surface of the electrode due to the heat loss while electrode blow-off of by the carrier gas was calculated as following:

$$\lambda_1 \cdot \frac{(T_{ij}^k - T_{i-1,j}^k)}{\Delta r} = \alpha_1 \cdot (T_{i-1,j}^k - T_g) \quad (2.23)$$

The change in temperature at the border of the “shell-flux wire core” due to the heat loss calculated by the equation:

$$\lambda_1 \cdot \frac{(T_{ij}^k - T_{i-1,j}^k)}{\Delta r} = \alpha_1 \cdot (T_{i-1,j}^k - T_g) \quad (2.24)$$

where “1”, “2”—indices related to metal shell and core charge correspondently.

Thermo and physical properties of the charge are adopted in the composition range, typical for these CW components. Low carbon steel (0.08 % C) is used as the shell material. Necessary for calculating thermophysical characteristics as thermal conductivity (λ), density (ρ), heat capacity(c) are adopted according to [30, 31] and approximated by polynomials (Table 2.6).

In calculation, according to Eq. (2.16), the enthalpy is taken into account as follows. At each step, the cell temperature was compared with the melting temperature of the shell and the charge, respectively. On reaching the latter, the cell temperature remains constant for the following time. At all subsequent points of time, the heat accumulated in the cell was summed:

$$\sum_{k=1} q_{ij}^k = \sum_{k=1} c_{ij}^k \cdot (T_{ij}^k - T_{ij}^{k-1}) \quad (2.25)$$

This heat at each step was compared with the specific melting enthalpy of cell material ($h_{m\ell}$). Under the following condition: $\sum_{k=1} q_{ij}^k > h_{m\ell}$ for further calculation steps, the temperature rise of the cell was taken into account.

Table 2.6 Equations of thermal parameters of the approximation

Parameter	Temperature range, K	The polynomial coefficients $Y = A_1 T^3 + A_2 T^2 + A_3 T + A_4$			
		A_1	A_2	A_3	A_4
λ , W/(m K)	300–2500	-2×10^{-8}	0.0001	0.1742	123.89
	2500–3500	0	-3×10^{-6}	0.0175	22.42
ρ , kg/m ³	300–3500	0	8×10^{-5}	0.463	7971.1
c , J/(kg K)	323–1073	0	0.0003	0.0969	409.12
	1073–1273	0	0	−1.045	1987.8
	1273–2473	0	0	0.0658	575.03

Calculation of the layer thickness at the end of the electrode, melted by the heat of the active arc spot, was carried out according to the solution of the task to melt the cold body of limited sizes under conditions of ongoing removal of resulting melt taking into account the enthalpy of fusion [32]. Balance equation of heat in this case can be written as:

$$q_1 \cdot F \cdot d\tau = dQ \quad (2.26)$$

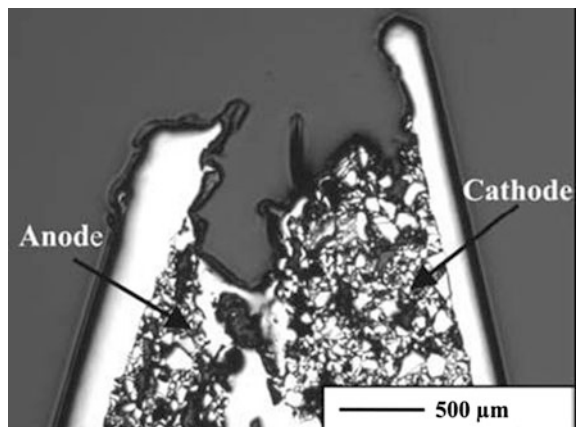
The left-hand side of the equation ($q_1 \cdot F \cdot d\tau$) is the heat input on the electrode end face, which is going on due to the heat of the active arc spot (q_1 is specific heat flow from the active arc W/m^2 ; F is the electrode end face area, m^2). Right-hand side of the equation (dQ) is heat discharge for melting the metal layer at the end and for the heating of the solid part of the electrode shell.

B. Tillman and et al. cite that in arc spraying using air as the atomizing gas leads to unevenness of CW melting. On combined macro thin section of anode and cathode is evident that non-melted components are less presented on anode-wire than on cathode-wire (Fig. 2.5).

Note that according to the illustrated macro thin section, the shell thickness varies for anode and cathode. This will adjust the process of wires melting. The asymmetric melting behavior of solid wires and core wires was observed in many studies [48, 103]. The reason is that the anode and cathode are heated differently. The same effect is observed at arc welding [2–4]. But Erokhin also noted that for the case of the arc welding in the air, the thermal power released in anode and cathode spots is approximately the same [4]. In case of AS, the asymmetric melting behavior is lowered due to the heat exchange between the active arc spots, located at a distance of 0.1–1.0 mm [22]. For this model, the share of thermal power of the arc, for each active spot is assumed nearly the same.

After substitution and integration, according to [32], Eq. (2.26) turns out into the equation regarding to the parameter (Ψ), in which the unknown is the thickness of the liquid layer (s), melting by the active arc spot:

Fig. 2.5 Combined macro-section molten CW $\varnothing 1.6$ mm, the composition of 28 % Cr, 1.4 % Si, 4.8 % C, <1 % B, Fe bal. [33]



$$2 \cdot a \cdot t / h^2 = (\Psi(N\varpi) / N^2), \quad (2.27)$$

where

$$\begin{aligned} \Psi(u) = & 9 \cdot u \cdot \sqrt{(u + 1/3)^2 + 8/9} / 16 + \sqrt{(u + 1/3)^2 + 8/9} / 16 \\ & + 2 \cdot \ln \left(6 \cdot \left(\sqrt{(u + 1/3)^2 + 8/9} + u + 1/3 \right) \right) / 12 - 3 \cdot u^2 / 16 - u/4; \\ N = & c \cdot \Delta T / (2 \cdot h_{m}); \quad \omega = (h - s) / h; \\ h = & 2 \cdot \lambda \cdot (T_n - T_c) / q_1; \quad a = \lambda / (c \cdot \rho); \end{aligned}$$

N —melting criteria, reflecting the effect of latent heat of melting; $\tau = 4 \times 10^{-4}$ s—a typical period of droplets removal from the end face of the electrode [20]; $T_s = 300$ K, the temperature of the unheated metal; $T_n = 2500$ K, the temperature of the surface melt at the end face of the electrode; a —thermal conductivity, m^2/s ; $h_{m} = 1,319,000$ J/kg, a melting enthalpy of liquid iron [4], h —heating zone longitude from the surface end downward to the depth of the electrode, m.

Results of calculation have shown in Fig. 2.6, in the case of CW, the thickness of the tearing off molten metal interlayer increases several times in comparison to solid wire.

Furthermore, with decreasing of shell thickness (δ) the thickness of liquid layer rises. Calculations based on Eq. (2.27) are done with reference to solid wire and CW. The correctness of the results was checked for the solid wire at calculation assuming the amount of layer and drops were equal. The droplets data are obtained due to oscilloscoping breakdown of droplets [20, 34] (these data are indicated in Fig. 2.6 as a solid, test).

In calculations of heat transfer, inverse problem was solved with respect to thermophysical parameters of the charge, for the blow round gas, the geometric

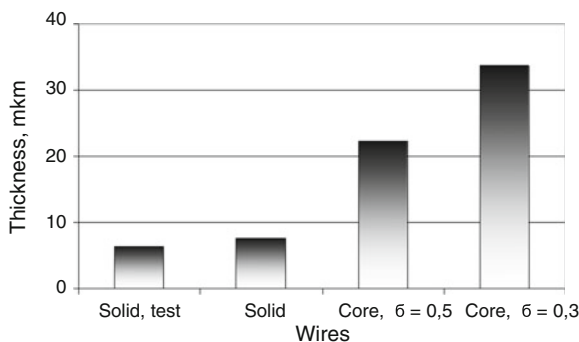


Fig. 2.6 The results of the calculation of the layer thickness of the liquid metal being broken away during AS for the solid wire and CW with different thickness of wire shell

Table 2.7 Parameters of corer wires and the modes used in calculations

Version number	r, mm	δ , mm	α , W/(m ² K)		Parameters of the gas		c_{charge} , J/kg K	λ_{charge} , W/m K	s, μm
			Cover	Charge	T, K	Type			
1	1	0.5	2070	1400	300	1	1600	20	22
2	+	+	+	+	+	+	+	5	22
3	+	0.3	+	+	+	+	+	+	33
4	+	0.3	+	+	+	+	+	5	33
5	+	+	3820	+	2100	2	+	+	22

Notes In the column “Version”, index 1 corresponds to the compressed air and the index 2—combustion products of propane-air mixture
Sign “+” in the cells corresponds to the variant 1

dimensions of the cross-section of CW. In the range of values of these parameters, the search of the area satisfying favorable conditions for drops formation of sprayed material was conducted. Table 2.7 shows the parameters of CW adopted for the calculation of the various options, differs from the basic option 1 by the thermal conductivity of the charge (variations 2, 4), the thickness of shell (variations 3, 4) and the conditions of heat transfer at the border “gas-wire” (variation 5).

- Analysis of the results of calculations (Figs. 2.7, 2.8, 2.9 and 2.10) showed:
- (1) CW shell throughout the thickness of the liquid interlayer is strongly overheated above the melting temperature (Fig. 2.7).
 - (2) The CW shell thickness along with the previously marked growth of the liquid layer height, results in decrease of its temperature (Fig. 2.7).
 - (3) The surface temperature of the shell is strongly depended on the heat-transfer conditions in system “gas-wire” (Fig. 2.8).
 - (4) The surface temperature of the shell is reduced in comparison with their inner-metal layers (Fig. 2.9) due to the heat exchange with cooler gas. It slows the droplets discharge.
 - (5) Some melting of the charge may be caused by the reduced heat-conductivity of the charge and the small thickness of the shell (Fig. 2.10).

Fig. 2.7 The temperature distribution over the cross-section of core wire

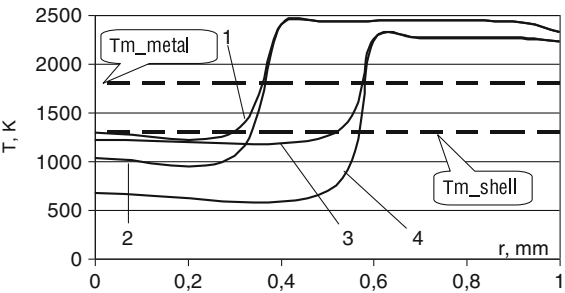


Fig. 2.8 Changes in the surface temperature of the shell according to the heat transfer conditions in the system “gas-wire”

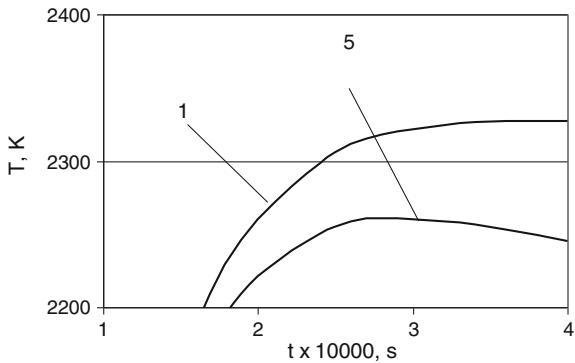


Fig. 2.9 Temperature changing in shell for the external and internal surfaces in variant 1

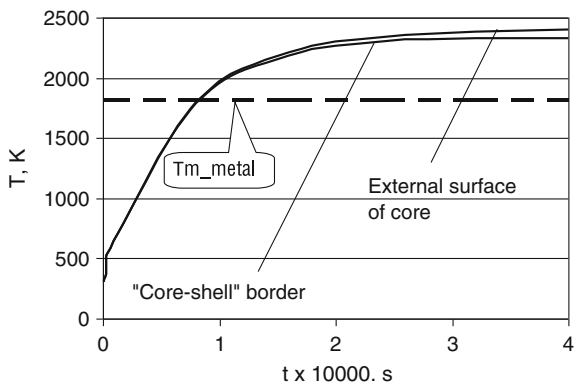
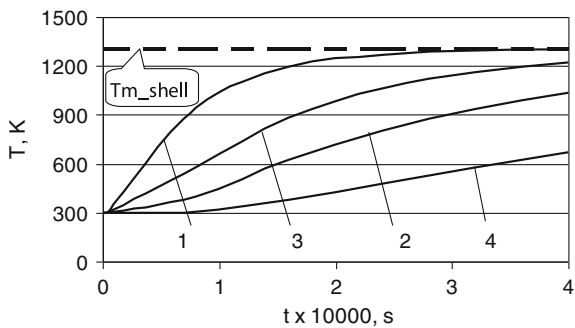


Fig. 2.10 Changing of the temperature in the center of the core for any combinations of thermal conductivity of the charge and the shell thickness



2.4 The Formation of Droplets of Liquid Metal

In the methods of powder thermal spraying, spherical particles flow gets into a stream of gas or plasma jet, where they are basically exposed to a drag force of the jet. This makes it easy to generate the spraying material jet. Other forces affected

the droplet apart from aerodynamic force have been neglected in calculations. In addition, it is assumed that they have a spherical form [11, 16, 17, 35].

The processes of formation of droplets at AS significantly differ from those indicated. Aside from gas-dynamic pressure, droplets are strongly affected by the electromagnetic forces generated when electrical current is passing through the wire electrodes, the liquid metal layer on the ends of the electrodes and the electric arc. Studying of droplet formation features enables to specify the initial conditions for the droplet motion for the subsequent step, on spraying distance.

For AS, the evaluation of droplets maximum size $d_{\max}$, broken away from the wire (diameter d_{np}) was done by Kudinov [15]. Condition that the surface tension and aerodynamic forces are equal was used.

$$d_{\max} = 1/|W_g - W_p| \cdot \sqrt{8 \cdot \sigma \cdot d_n / (C_d \cdot \rho_g)}, \quad (2.28)$$

where $S = \pi \cdot d_{\max}^2 / 4$ is the cross-section area of the droplet, m^2 ; C_d is drag coefficient; ρ is density, kg/m^3 ; W is velocity, m/s ; indices “ p ”, “ g ” refer to the droplets and gas, respectively.

Calculation according to Eq. (2.28) using $\sigma = 1.5\text{--}1.16 \text{ J/m}^2$ gives $d_{\max} = 600\text{--}900 \text{ }\mu\text{m}$.

The sizes of the discharged droplets can be obtained by welding arc oscillographic testing (Table 2.8). Based on the calculated and experimental data it can be stated that, in case of steel, the droplets that are breaking away from the end face of the electrodes corresponds to a midship section of a diameter of 550–900 μm .

A picture of the gas dynamic effect on the liquid surface is described by Levich [36]. Let us suppose that on the jet surface have occurred a small disturbance of any minimum size. The gas pressure above the roughness top will be reduced, whereas bottom pressure will be raised in comparison to the average. The reason for this is that the velocity of the gas passing over the roughness top is higher than at its bottom.

According to Bernoulli’s equation, the pressure is lower where velocity is higher. Thus, the roughness formed on the liquid surface that is moving in relation to the gas tends to increase. This effect is stronger, if the relative velocity of movement is higher. The amplitude rise ultimately leads to the separation of the

Table 2.8 Characteristics of the droplets discharge for AS according to oscillographic testing, 2 mm wire

	Steel [20]	Steel [20]	Steel [34]	Al [20]	Al [20]	Zn [20]	Zn [20]
f , Hz	900	1750	1740	600	1750	500	850
$t \times 10^4$, s	11.1	5.71	5.74	16.7	5.71	20.0	11.8
$G \times 10^3$, kg/s	4.0	8.0	2.2	1.1	4.0	3.3	11.1
$m_{\text{канли}} \times 10^6$, kg	2.22	2.29	0.63	0.93	1.14	3.33	6.54
ρ , kg/m^3	7200	7200	7200	2500	2500	6500	6500
$d_{\text{канли}} \times 10^6$, m	838	846	551	891	956	993	1240

droplet from the surface. Separation occurs in violation of the balance between gas-dynamic head of the gas jet and the surface tension force.

With regard to the above-mentioned AS scheme of metal removal from the face assumes an elongated form of droplets in the form of a tongue. Deviation from the spherical shape of the droplet increases the possibility of its separation. Deformation processes and a subsequent separation were described by Levich for the case of the falling droplet [36]. The review was done considering that the dynamic head of the jet equals to the capillary pressure in the droplet. For the critical droplet radius, r_{kp} , at which its disintegration occurs the equation is derived as:

$$r_{kp} = \sigma / \left(\rho_g \cdot (W_p - W_g)^2 \right) \cdot \sqrt[3]{6/C_d} \quad (2.29)$$

Further clarification of this equation was done taking into account dimensionless parameters like the Weber number (We), the Laplace (Lp) number, the Mach number (M), Bond (Bo) number and the, Reynolds (Re). Moreover, the degree of their influence on the destruction conditions is different [37]. Weber number is the most important among them.

$$We = d \cdot \rho_g \cdot (\bar{W}_p - \bar{W}_g)^2 / \sigma \quad (2.30)$$

Knowing We , for taken hydrodynamic conditions, the estimation of critical droplet diameter can be made.

In work [38], based on a studied spark photo of it was concluded that the separation of the droplet occurs at a critical value of the Weber number $We_{kp} = 5,406$. During the separation, the droplet shape is closer to the disk, with the ratio of the semi axes 3.75.

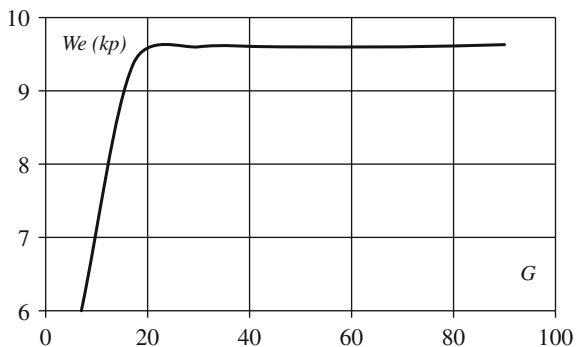
Thinning liquid narrow disk is unstable and separated under the influence of small perturbations by disruption of wave crests of increasing amplitude. The midship section area of the deformed drop cross-section was estimated as the average between the sphere area f_{iu} and ellipsoid area in a critical phase, then $f_m \approx f_{iu}$. In the critical stage, the droplet is closer to the ellipsoid with the semi axes ratio of 4–6.

During the experiments with water–glycerin droplets of diameter 3.5–5.3 mm in an air stream, the increased separation was studied [39]. To evaluate the nature of load change on the droplet of the aerodynamic forces, the G similarity number was introduced:

$$G = dWe / (d(\tau/\tau_k)) = 1.66 \cdot We^{0.5} \cdot \bar{Bo} \quad (2.31)$$

where, τ is current time, s; $\tau_k = 0.83 \cdot \left(\rho_p \cdot d_p^3 / \sigma \right)^{0.5}$ is the period of natural droplet fluctuation, s; $\bar{Bo} = \sqrt{\rho_g \cdot \rho_p} \cdot d_p^2 \cdot a / \sigma$ is modified Bond number; $a = du/d\tau$ is droplet acceleration, m/s^2 .

Fig. 2.11 Change of the critical Weber number (We_{kp}) depending on the parameter G



The result of the experimental data processing is presented in Fig. 2.11 and corresponds to the Laplace number

$$Lp = \sigma \cdot \rho_{\text{ж}} \cdot d / \eta^2 = 23200,$$

where $\rho_{\text{ж}}$ is liquid density, kg/m^3 ; d is diameter of droplets, m; η is coefficient of dynamic viscosity, kg/(m s) .

In the paper, it was concluded that the low rate of rise of aerodynamic forces which means more time of exposure, ($G < 15$), the value of We_{kp} can be nearly twice as different from the corresponding values for $G > 15$.

In the latter case, $We_{kp} \approx 10$ and the critical conditions for the separation are of self-similar with respect to G . At the point of reaching point of that value, the droplet is inevitably separated. It corresponds to the dumbbell shape of the droplets.

Research data [39] were used by Petrov et al. to evaluate the changes in the particle size of aluminum, tungsten, copper, nickel, and aluminum oxide at plasma spraying [40]. The main conclusion to be drawn from the above analysis is that at jet processes associated with the powder processing, the conditions of particles separation of different materials with a diameter of $>50 \mu\text{m}$ are realized at a velocity of 1500 m/s and temperatures between 3000–6000 K of the plasma jet. Unfortunately, the paper does not present any data on the surface tension of the materials that were used in the calculations.

In the case of AS, to evaluate the critical size of particles of various materials $We_{kp} = 12$ was taken [41]. According to the calculations, the critical particle diameter in microns is: Al–80, Zn–72, Fe–156, Ni–180, Cu–118. These calculations truthfully reflect the quality correlation of critical droplet diameters of different materials. Thus, experience shows that metal coatings of Al and Zn are denser and smoother than those from Fe under the same coating conditions. However, the reasoning for taking values in the calculation of the surface tension $\sigma = 0.6 \text{ J/m}^2$ is not provided.

Data that are more reliable are given in the works on the theory of metallurgical processes. According to Popel [42], when the temperature is slightly above the melting point, the changing of surface tension is described by the equation:

Table 2.9 Surface tension of liquid iron

T_{Me} , K	1809		2070		2500	2900	3148 ^a
Source	[43]	[42]	[43]	[42]	Equation (2.33)		
σ , J/m ²	1.85	1.85	1.65	1.76	1.50	1.29	1.16

^a $T_{kun} = 3148$ K, according to [26]

$$\sigma(T) = \sigma_{nn} + (d\sigma/dT) \cdot (T - T_{nn}), \quad (2.32)$$

where σ_{nn} is the surface tension at the melting point of the metal, J/m²; $d\sigma/dT = -0.35 \times 10^{-3} \text{ J}/(\text{m}^2 \text{ K})$.

According to the generalization of the various studies experimental data made by Lynchevskiy, the surface tension of iron varies in the range of $\sigma = 1.85\text{--}1.65 \text{ J/m}^2$ [43], with the temperature increase from 1809 to 2070 K, i.e., for the same area.

Approximate temperature dependence of the surface tension at higher temperatures between the melting and boiling points is expressed by equation [20]:

$$\sigma(T) = \sigma_{nn} - B(\rho_p/\mu)^{2/3} \cdot (T - T_n), \quad (2.33)$$

where ρ_p , μ is density, kg/m³ and a molecular weight, kg/mol, of the liquid metal; T , T_{nn} are current and melting temperatures, respectively, K;

$$B = 2 \cdot 10^{-7} \text{ kg m}^2/(\text{s}^2 \text{ K}).$$

The surface tension values for the intended temperature intervals for Fe droplets are summarized in Table 2.9 according to the cited studies. They are >2 times higher than those used in studies [41].

Dealing with arc processes, we can only speak about the approximate values of the metal surface tension. The temperature at the end faces of the electrodes is not the same due to the active arc spots wandering and various thermal contributions of cathode and anode. This leads to inhomogeneity of the surface tension at the end faces of the electrode. In addition, the presence of alloying elements and impurities in the metal influences the value of surface tension. In the case of liquid steel, for example, oxygen, nitrogen, sulfur, phosphorus, reduce the surface tension by 20–30 % [26].

We carried out the calculation of the critical diameter of the iron particle along the spraying distance for AS, Table 2.10. This analysis leads to the following conclusion: metal droplets of the above given diameter interval in the range of 550–900 μm (Table 2.8), can be separated near the nozzle cut. Herewith, the droplets with the diameter <165 μm are resistant to deformation and fracture. On the further movement along the distance, the droplets resistance increases. In the calculations for AS, it can be certainly assumed that the droplets have a sphere shape when the diameter is <300 μm .

However, the studied mechanism of droplets fragmentation under the gas-dynamic forces influence does not explain the appearance of droplets with a

Table 2.10 Calculating results of the critical diameter of droplets along the spraying distance (S) using Eq. (2.30)

S , mm	σ , J/m ²	We	W_g , m/s	W_p , m/s	ρ_g , kg/m ³	d_{kp} , μm
0	1.29	12	900	7	0.118	165
5	1.29	12	582	43	0.137	332
10	1.5	12	426	91	0.131	1002
100	1.85	12	119	177	0.415	11011

The values of the surface tension are taken from Table 2.9. The data on the hydrodynamic parameters of two-phase flow for AS are according to [13]

diameter smaller than the critical, $d_{kp} = 165 \mu\text{m}$. Measurements of particle diameters during wire metallization were made by Vadivasov [44], Kretzschmar [34], Pohmurskiy et al. [45], Borisova et al. [46], Vahalin et al. [20], as well as by the authors by trapping the droplets from flying jet with different methods for the subsequent grain-size analysis. Experimental data suggest that the diameters of droplets during the metallization process are in the range of 1–600 μm .

Moreover, the share of droplets with diameter less than the critical value (for example, for iron) is 20–80 %, depending on the experimental conditions. Although, according to calculations, the surface tension forces should be enough to deter the droplets of this diameter interval from separating by the gas jet. The hypothesis about the secondary separation of droplets under the influence of the gas-dynamic forces [20] is not convincing enough, because it does not justify the possibility of reducing the surface tension for iron to $\sigma = 0.6 \text{ J/m}^2$.

To resolve the contradictions in the results of the droplet sizes evaluation according to oscilloscoping and grain size analysis, the authors proposed another mechanism of droplets formation in the arc burning zone [47].

It is accepted that in the arc, the big diameter droplet separation from the electrode end face due to the gas-dynamic pressure of the gas occurs simultaneously with its fracture into smaller droplets under the action of electromagnetic forces.

In the works, where the melting and spraying process of the electrode material for the arc spraying is studied, the effect of electromagnetic forces on the droplet formation is not considered. Although, their presence is indicated. In addition, it is assumed that the gas-dynamic forces have a decisive effect on the character of the droplets formation [20, 48]. To assess the correctness of this conclusion, we performed a quantitative estimation of the main forces acting on the liquid metal in the combustion zone of the arc [98].

Under the influence of the gas-dynamic pressure, the metal is pushed away from the electrode end face and extends in a plane jet “tongue”. It is being held on the end face due to the surface tension forces. The balance of these forces can be expressed by the equation:

$$0.5 \cdot C_d \cdot S \cdot \rho_g \cdot (W_g - W_p)^2 = \pi \cdot d_n \cdot \sigma, \quad (2.34)$$

where C_d is drag coefficient, S is the area of the midship section, m^2 ; W_g , W_p are gas and particle velocities in m/s ; d_n is the diameter of the separation necks, m .

According to the previously described scheme of Levich, the fracture of the liquid metal jet occurs in the necks formed in the tongue under the gas pressure fluctuations at the local perturbation spots [36]. The presence of passing current flow running through the tongue and electric arc add to the constricting mechanism with more powerful destroying mechanism of neck's breakage. It was described by Sheyhaliev applying to steel casting in arc furnace [49].

Joule heating release at the constrictions is more intensive, when compare with the other parts of the conductor due to the increased ohmic resistance. Further breakage of the jet progresses due to the joint processes of liquid metal boiling and shock expansion of the gas at the necks.

Furthermore, in the arc burning zone the electromagnetic forces arise from the interaction of running current and the induced induction flows. Interaction "liquid metal-electric arc" is considered in the number of research works with respect to arc welding [2–4, 50]. However, they do not allow an adequate description of what is happening in the case of arc spraying for several reasons.

- (1) The difference of 1–2 orders in velocities and gas flow consumption leads to a decrease of 2–3 orders of the size and time of droplets fracture from the end faces of the electrodes. If for the arc welding the fracture period $= (10^{-1} - 10^{-2})$ with a diameter of 1–3 mm [2], then for AS $= (10^{-3} - 10^{-4})$ s (see Table 2.8) with a diameter of droplets 0.01–0.3 mm (see Table 2.10).
- (2) The direction and a value of an impact of the gas stream on the electric arc are changing. When Arc Welding, the gas flow is directed along the axis of the electrode and the gas consumption rate does not exceed $1.2 \text{ m}^3/\text{h}$ [23]. When the Arc Spraying direction of gas flow forms an angle $(15 - 30)^\circ$ to the electrode axis, and the gas flow rate is higher on average by 100 times. While welding, it is preferable to have a laminar flow of the gas stream. Transition to turbulent flow reduces the stability of the arc combustion. During AS, gas flow with a high kinetic energy of high turbulence is forcefully directed on the liquid metal in the arc burning zone.
- (3) For AS, the cathode and the anode acts like electrodes simultaneously. The metal droplets do not reach the other electrode after breaking from the one. Whereas, the angled co-relative position of the electrodes causes specific interaction of the magnetic and electric fields of current flow.

These differences change the parameters of the processes at the end faces of the electrodes in comparison with the Arc Welding.

Let us consider the electromagnetic forces in the burning zone of the arc for the Arc Welding. The scheme of arc movement in the gap between the electrodes while the latter are being blown by the cross-flow (Fig. 2.12) was offered by Bron and Sushkov. It was confirmed by the high-speed shooting data. According to this scheme, arc column retains a cylindrical shape under the influence of the cross-flow of gas. Whereas the plasma flows are tied to the electrode end faces and are demolished under the joint action of the Ampere force and gas-dynamic pressure.

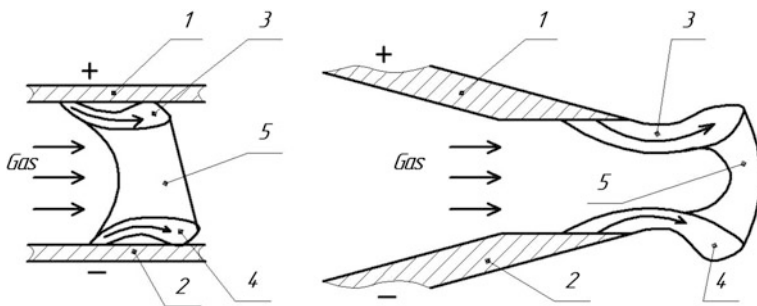


Fig. 2.12 The arc shape at the combined air and magnetic blast [51]. **a** In the gap between the electrodes; **b** In the extreme position at the end face of the electrodes (1, 2), plasma flows (3, 4), arc column (5)

For calculations, it is convenient to assume that the arc column has cylindrical shape and its axis is perpendicular to the end faces of the electrodes. However, in this case, the correctness of this assumption should be evaluated further due to the high velocity of the gas stream flowing around the end faces of the electrodes. Let us calculate the velocity of the arc and gas-plasma flows which is flowing around it.

Due to the pinch effect, i.e., the interaction proceeds in a current column with its own magnetic field, the compression of the arc column by plasma surrounding this column has occurred. Thus, there is a longitudinal pressure gradient, which in turn generates a flow of plasma from the spots of decreased cross-section. According to the decision of the Navier–Stokes equations for this case, the maximum velocity of the plasma in the flow can be determined from the equation [51]:

$$v_{\max} = \sqrt{\mu \cdot I \cdot j / (2 \cdot \pi \cdot \rho_g)}, \quad (2.35)$$

where $\mu = 4 \times 10^{-7}$ H/m is magnetic permeability of the medium corresponds to a vacuum; I is current, A; j is current density, A/m²; $\rho_g = 0.129$ kg/m³ is the density of the gas at $T = 2500$ K [30].

If we consider radius of the arc column to be equal to the radius of the electrode $r = 0.001$ m, then for a typical mode, $I = 200$ A, this corresponds to $j = 6.4 \times 10^7$ A/m². According to Eq. (2.35) for these parameters, the flow velocity of the plasma $v_{\max} = 140$ m/s. These calculation results are close to the experimental data obtained by Lenivkin and Dyatlov acquired with a help of drop movement record [3, 50].

The resulting value is in the interval between the flow velocity of the plasma in the arc column for the arc welding $v = 78$ m/c, where $j = 2.0 \times 10^7$ A/m² [2], and the flow rate of the plasma in the plasmatron $v = 393$ m/c, where $j = 5.0 \times 10^8$ A/m² [12]. From the structure of Eq. (2.35), we can see that an increase in the rate of the gas flow causes the compression of the plasma flows around the arc column, which leads to the increased stability of the arc in the inter electrode gap.

The velocity of the arc movement in the gap between the electrodes can be assessed by the frequency of droplets detachment. This follows from the physical representation about the mechanism of arcing between the end faces of the electrodes. First, the arc is bypassed in the most energetically favorable location—on the inner edges of the ends. Thereafter, the arc moves toward the front edges of the end faces. Here, the arc column bends and its length increases. While the potential difference across the cathode and anode is constant, it leads to the fracture of the arc. Then, the process begins again with the bypassing of the arc on the inside edges. When the diameter of the electrode is 2 mm, the angle of inclination toward the central axis is 15° . The breakdown period is 5.7×10^{-4} s and the velocity of the arc column is:

$$v_{nc} = 2.10^{-3} / (\sin 15^\circ \cdot 5.7 \times 10^{-4}) \approx 12 \text{ m/s} \quad (2.36)$$

The gas velocity in the gap between the electrodes is estimated from the liquid layer being teased from the end face of the electrode by the gas stream. From the Newton's equations for the case of thin liquid films flowing that are entrained in the gas stream, Levich derived the dependency of the viscous layer thickness (δ) from the gas velocity and the liquid metal properties [36]:

$$\delta = (\sigma \cdot v / \rho_p \cdot v_0^3)^{0.5}, \quad (2.37)$$

where v is the kinematic viscosity, m^2/s ; v_0 is the gas velocity in the gap above the boundary layer is m/s . We take parameter values as for the liquid iron under the temperature of 2500 K: $\rho_p = 7265 \text{ kg/m}^3$ [26], $\sigma = 1.5 \text{ J/m}^2$ [20], $v = 7 \times 10^{-7} \text{ m}^2/\text{s}$ [52].

Let us express the gas velocity (v_0) from the Eq. (2.37):

$$v_0 = (\sigma \cdot v / \rho_p \cdot \delta^2)^{1/3} \quad (2.38)$$

Thickness of the layer (δ) will be estimated based on the condition that the volume of separating layer and droplet according to oscilloscoping of droplets breakdown for Arc Welding. According to Table 2.8, the average value of breakdown period is 5.74×10^{-4} s. Then, at wire feed velocity 0.05 m/s the droplet mass is $m = 6.4 \times 10^{-7}$ kg. If we assume that the thickness of the separated layer over the end face is uniform, then its value will be:

$$\delta = m / (\rho_p \cdot S_{nos}) = 6.4 \cdot 10^{-6} \text{ m}, \quad (2.39)$$

where: $S_{nos} = 1.3 \times 10^{-5} \text{ m}^2$ is the surface area of the electrode end face, according to the scheme adopted for AS process (Fig. 1.3), where the gradient angle of the electrode to the axis of the gas jet is 15° and diameter is 2 mm.

The thickness of the layer at the end face of the electrode fusing by the heat of the near-electrode arc spot is $s = 7.6 \times 10^{-6} \text{ m}$. The ratio between this value and the thickness of the separated liquid layer calculated according to Eq. (2.39), $\delta = 6.4 \times 10^{-6} \text{ m}$ is qualitatively corresponds with the physical picture of the process, when the liquid metal layer is carried away by the gas flow.

Table 2.11 The velocities in the gap

Velocities	Calculated equation	Value, m/s
Plasma flow around the arc column	(2.35)	140
Arc column transition along the end faces	(2.36)	12
The carrier gas flow	(2.38)	1.5

The calculating results of the velocities of the arc column movement, gas, and plasma flows in the gap between the end faces of the electrodes are shown in Table 2.11.

It is evident that the velocity of carrier gas flow is smaller comparing to the velocity of the plasma that compressing the arc column and to the velocity of column moving within the gap. Therefore, we can conclude that the arc column will likely have a cylindrical shape with a vertical axis, and the electromagnetic forces are the main reason for its motion.

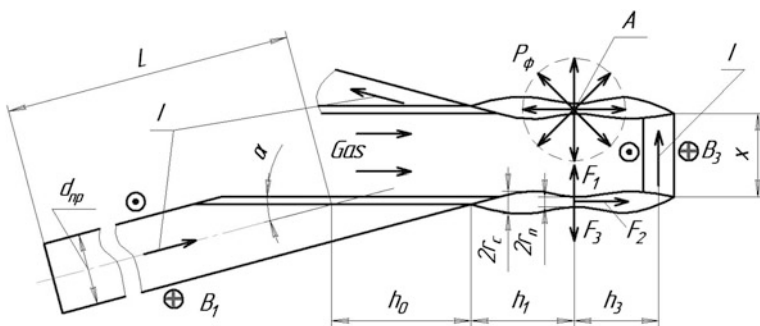
We are to estimate the magnitude and direction of forces acting on a single droplet when it is separated from the electrode end face. Assuming that the mutual vapor pressure of the metal at both end faces is leveled, then the affecting forces are the following:

- aerodynamic drag;
- impact wave pressure of the at the explosive expansion of the metal vapor and the gas at the neck;
- Amper: F_1 (the interaction of the magnetic field of electrode extension and the current in the neck), F_2 (pinch-effect), F_3 (the interaction of the magnetic field of the arc column and the current in the neck).

The direction of the currents flows, the location of magnetic fields and Ampere forces are shown in Fig. 2.13. Let us perform the calculation of these forces.

The aerodynamic drag force can be calculated using Eq. (2.34):

$$F_{ac} = \pi C_d (v_g - v_p)^2 y_g d_k^2 / 8 \quad (2.40)$$

**Fig. 2.13** Electromagnetic forces in the arc combustion zone

We will consider the following:

$C_d = 0.4$, as the self-similarity mode [53];

$(v_g - v_p) = 800$ m/s;

$\gamma_e = 0.168$ kg/m³ (gas temperature is 2100 K);

$d_\kappa = 10$ μ m, corresponds to an average droplet size of the smallest fraction interval according to the sieve analysis data obtained by authors.

The force value F_ϕ from the impact wave on the surface of the neck of radius r_n and length Δh will be:

$$F_\phi = P_\phi \cdot 2\pi \cdot r_n \cdot \Delta h, \quad (2.41)$$

where P_ϕ is the pressure at the front of the impact wave, MPa.

According to the mechanism of the “tongue” breakage which was suggested by us, the current running through the neck drastically enhances the heat release due to the increased ohm resistance. This all initiates the “tongue” breakage in the neck zones. Moreover, it stimulates the appearance of micro arcs at the breakage zones that leads to the explosive expansion of the vapors metal and gas. During the gas expansion, there is a sharp increase in the volume of the heated gas and spreading of the impact wave, followed by the energy release. According to [54], the pressure on the front of the impact wave can be expressed by the dependency:

$$P_\phi = 2 \cdot \gamma_e^0 \cdot v_\phi^2 / (\kappa + 1), \quad (2.42)$$

where γ_e^0 is the initial gas density, kg/m³; κ is the adiabatic constant; v_ϕ is the front velocity of the expanding gas, m/s.

v_ϕ can be estimated from the equation of gas-dynamic pressure forces and capillary pressure in the bridge, which prevents its destruction [49]:

$$v_\phi = \sqrt{(16 \cdot C_d \cdot \sigma) / (d_{nep} \cdot \gamma_e)} = 2857 \text{ m/s} \quad (2.43)$$

Neck diameter d_{nep} is assumed to be the thickness of the separated layer (δ) to be calculated by the Eq. (2.39).

Aside from the gas-dynamic forces, the neck is exposed to electromagnetic forces caused by the current flow of in the tongue and in the electrodes. There are three main directions of current flow regarding to the axis of the gas flow: the section of extension of length (L) at an angle (α); along the layer of the liquid metal; along the arc column of length (x), Fig. 2.14. Magnetic induction B_1 of the electrode departure at the neck center (A) is calculated according to the equation of the magnetic field of linear conductor [55]:

$$B_1 = \mu \cdot I \cdot (\cos \varphi_2 - \cos \varphi_1) / (4 \cdot \pi_1), \quad (2.44)$$

where according to Fig. 2.14: r_1 is the shortest distance from point A (the neck center) to the electrode; φ_1 , φ_2 are the angles between the vector of the current

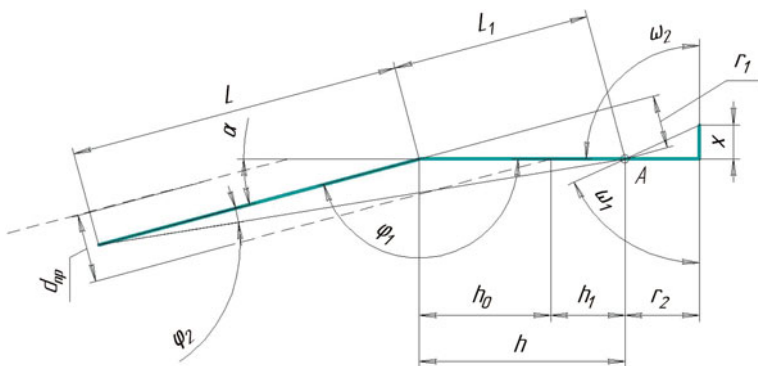


Fig. 2.14 The calculating scheme of electromagnetic forces in the arc burning zone. Equation (2.44) takes the form

density in the conductor and radius—vectors which are drawn to the point A from the beginning and the end of the conductor; μ —is the magnetic constant. Angles φ_1 , φ_2 can be determined by the inclination angle of the electrode (α):

$$\cos \varphi_1 = \cos(180 - \alpha) = -\cos \alpha, \quad r_1 = h \cdot \sin \alpha, \quad (2.45)$$

$$\cos \varphi_2 = (L + h \cdot \cos \alpha) / \sqrt{(h \cdot \sin \alpha)^2 + (L + h \cdot \cos \alpha)^2}, \quad (2.46)$$

where $h = d_3 / (2 \cdot \sin \alpha) + h_1$,

$$B_1 = \frac{\mu \cdot I}{4 \cdot \pi \cdot h \cdot \sin \alpha} \cdot \left(\frac{L + h \cdot \cos \alpha}{\sqrt{(h \cdot \sin \alpha)^2 + (L + h \cdot \cos \alpha)^2}} + \cos \alpha \right) \quad (2.47)$$

The force F_1 that occurs from on the interaction of the magnetic field B_1 and the current flowing through the bridge will influence the neck of the length Δh :

$$F_1 = I \cdot B_1 \cdot \Delta h, \quad (2.48)$$

We are to take the length of the neck $\Delta h = 1 \times 10^{-6}$ m.

Due to the local narrowing of the conductor at the neck, the force F_2 is acting due to the pinch effect which caused by the interaction of current flow and the magnetic field (induction B_2):

$$F_2 = \mu \cdot I^2 \cdot \ln(r_c/r_n) / (4 \cdot \pi), \quad (2.49)$$

We are to consider: $2r_s = 7 \times 10^{-6}$ m, $2r_n = 6.5 \times 10^{-6}$ m are transverse dimensions of the “tongue” and the neck (Fig. 2.12).

Table 2.12 The estimated forces acting in the arc burning zone on the liquid metal

Forces	Equation	Estimated value, $\times 10^5$ N
Aerodynamic drag	(2.40)	16.9
The impact wave pressure	(2.41)	2.7
Ampere F1 (electrode extension magnetic field-the current in the neck)	(2.48)	5.2
Ampere F2 (pinch-effect)	(2.49)	9.4
Ampere F3 (the magnetic field of the arc column-current in the neck)	(2.51)	0.03

The numerical value of the magnetic induction B_3 at point (A) is similar to Eq. (2.44) and is going to be:

$$B_3 = \frac{\mu \cdot I}{4 \cdot \pi \cdot r_2} \cdot (\cos \omega_2 - \cos \omega_1) = \frac{\mu \cdot I}{4 \cdot \pi \cdot h_2} \cdot \left(1 - \frac{x}{h_2}\right), \quad (2.50)$$

where ω_1 , ω_2 are the angles between the vector of the current density in the arc column and radius—vectors that are drawn to the point A from the beginning and the end of the column; $\omega_2 = 90^\circ$ due to the assumption that the position with respect to the axis of the arc column of the gas stream is vertical.

The interaction of the magnetic field of the arc column with the current in the neck gives the force F_3 :

$$F_3 = I \cdot B_3 \cdot \Delta h \quad (2.51)$$

The results of calculation of the forces separating droplets from the end faces of the electrodes are shown in Table 2.12.

These results are evaluative due to the lack of experimental data. Moreover, the calculation shows that the force of an electromagnetic nature, has the same magnitude with aerodynamic force. However, the direction of their actions differs from the desired motion for droplets. Thus, our assumption about the possibility of the droplets fracture in the burning zone of the arc under the influence of not only aerodynamic but also electro-dynamic forces is rather significantly accurate.

The accuracy of mechanism proposed by us of processes at the end face of the electrode at AS was confirmed later in experimental data obtained by Hussary and Heberlein [48] dealing with the high-speed shooting of the droplets formation. BP400 (Praxair) spraying gun was used during this experiment. Nitrogen was used as a carrying gas, the carbon wire $\varnothing 1.6$ mm (0.37–0.44 C, wt%) was a feedstock, $U = 30$ V, $I = 100$ A.

On the anode (+) active spot heats a large portion of the electrode end face surface. This causes the formation of molten metal on the surface (Fig. 2.15). Carrier gas flow affects the layer, creating a wave which propels a metal toward the edge of the electrode end face, creating the anode metal strip. Anode spot tends to

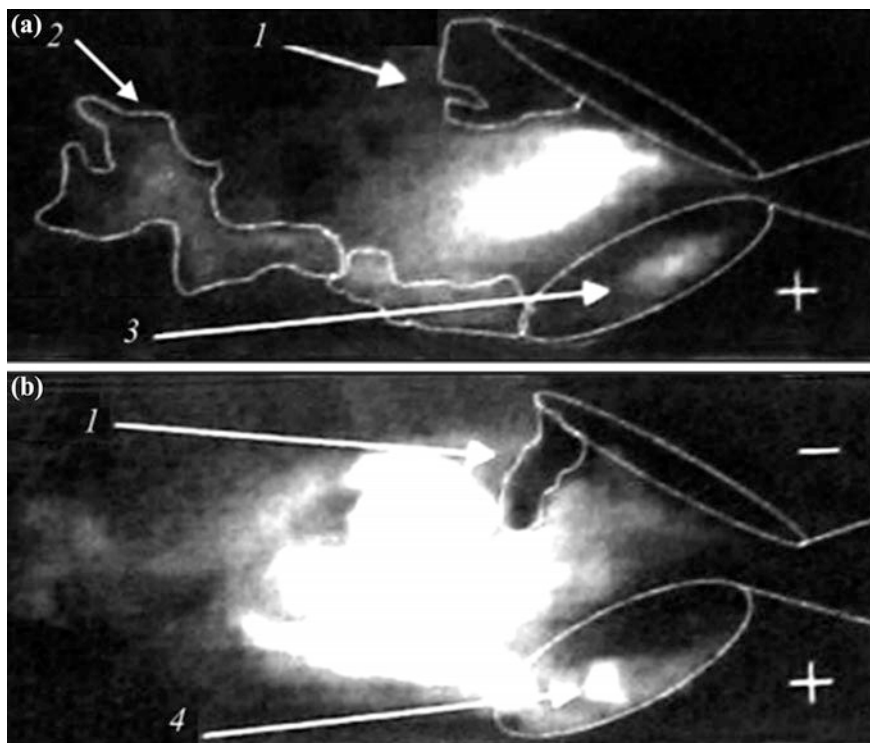


Fig. 2.15 Formation of the liquid metal droplets, $f = 13,500$ frames/s, $P = 138$ kPa. **a** $t = 0$; **b** $t = 0.28$ ms. 1 Tabs on the edge of the cathode spot and the cathode tongue; 2 anode tongue and its fracture; 3 wave on the anode surface; 4 wave motion on the anode surface

move in the direction with the gas flow, settling on the surface of the anode tongue (sheet according to Hussary and Heberlein). When the anode tongue breaks away, the arc is cut off and reignited in the shortest distance between the electrodes.

The cyclical behavior of the arc corresponds to the similar voltage fluctuations. Also, the arc fracture without abandoning the anode tongue was observed, but only under the influence of aerodynamic forces.

There are obviously the three mechanisms (Fig. 2.16) of metal separation occurred at the cathode (-).

- (1) Ejection of the molten metal in the droplets form into the cathode stream. Due to the current change flowing through the cross-section of the arc (the arc compression at the cathode due to the pinch effect), the arc interaction with its own magnetic field leads to the ejecting step and separation of the molten metal in the arc column.
- (2) The formation of small projections on the perimeter of the cathode end face and their separation before the formation of the tongue. Often, these projections are part of an extended ring (or jacket) of the molten metal on the surface

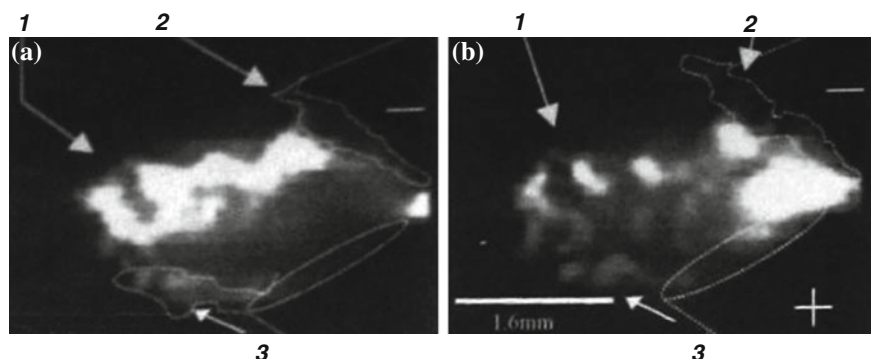


Fig. 2.16 Droplets separation from the cathode into the arc column, the two consecutive frames. $f = 13,500$ frames/s, $P = 138$ kPa. 1 Separated droplets; 2 cathode projections; 3 anode tongue

of the wire. The displacement of the binding cathode spot over its surface has occurred. The pressure gradient at the end face surface of the wire force the molten metal to shift toward end face perimeter creating the metal projections. The gradient occurs due to the impact of ion flux on the cathode spot.

- (3) Small projections are grouped to form a cathode tongue on the front edge of the wire. It is similar to the anode, but usually is smaller in size.

The influence of atomizing gas pressure on the nature of the metal melting on the anode and the cathode was investigated in the range of $P = 138\text{--}414$ kPa, $f = 27,000$ frames/s. With increasing pressure all the effects were clearly observed (Fig. 2.17):

- (1) According to the oscillation, the frequency of the voltage has increased. As the consequence, the velocity of the arc transferring has also increased. It was witnessed from the high-speed shooting data. There has been a reduction of time of the arc being in the stable and the rise in the number of resignations.
- (2) The nature of the anode and cathode melting has changed. A bulbous metal formation has occurred on the anode near the end face of the electrode. They are increasing in accordance with a continuous displacement of the molten metal from the liquid layer at the end face of the electrode by the gas. Upon reaching a critical mass of bubbles, the molten metal is drawn into a long tongue and is separated (Fig. 2.17b, right).

However, at a certain point, when the forces of surface tension in the tongue exceed the aerodynamic resistance, the tongue pulled toward the end face of the electrode and the formation of bubbles begins again. This effect is most clearly observed at the intermediate pressure (345 kPa).

When the pressure increases to 414 kPa, the anode bubble is reduced in size, and the contraction of the anode tongue to the end face of the electrode eliminates (Fig. 2.17). At the cathode, the extrusions are increased in size by the end face perimeter forming a cathode tongue which is not formed at a low pressure (Fig. 2.17a).

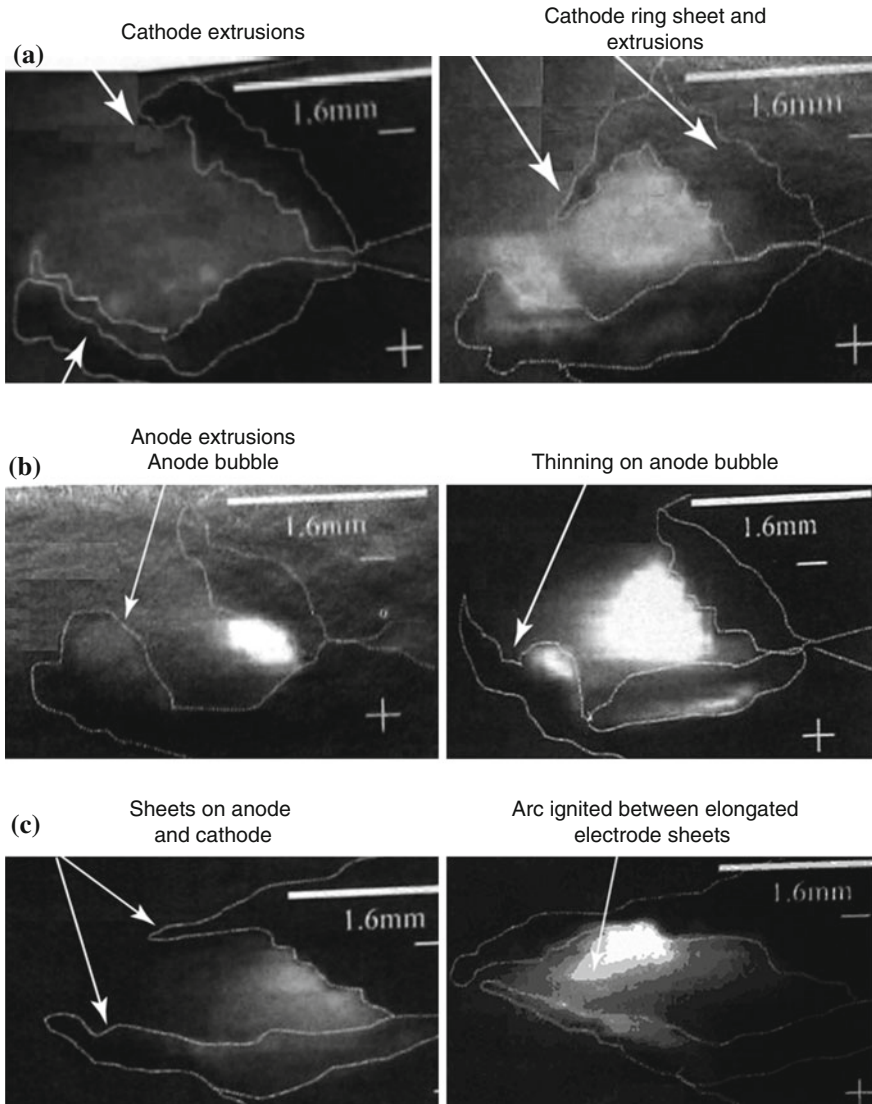


Fig. 2.17 The nature of the metal melting at AS **a** $P = 207$ kPa; **b** $P = 345$ kPa **a** $P = 414$ kPa

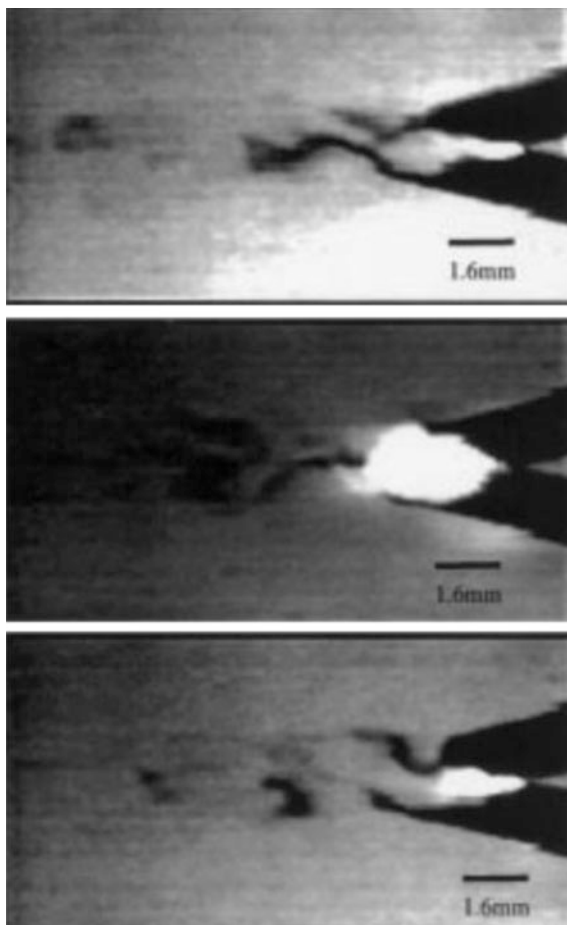
It is noted that the shape and size of metal droplets that are separated from the anode and cathode can be the same, despite the differences in the mechanisms of their formation. This is due to the influence of high pressure of gas. Probably, an additional effect can be caused by heat transferring from the anode to the cathode, where the heat input is higher (Fig. 2.18).

Large whirling was marked on the anode and cathode tongues (Fig. 2.19). Although these pictures are not consistent, they demonstrate the process of tongue

Fig. 2.18 The structure of the spraying metal.

$f = 27,000$ frames/s,

$P = 276$ kPa



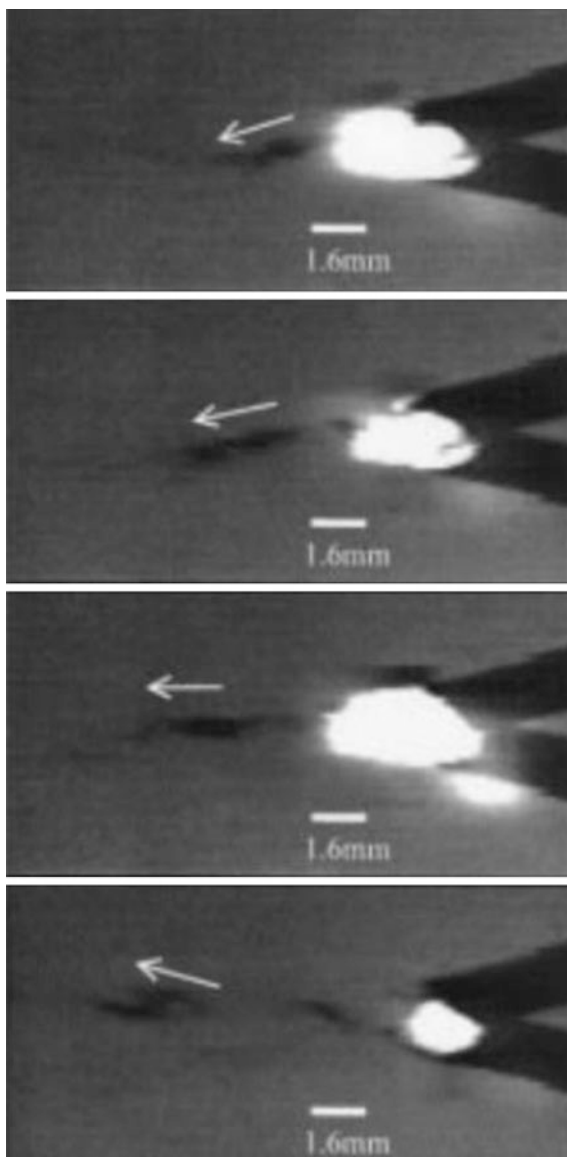
formation. As soon as the tongues begin to exceed a certain critical length, the influence of aerodynamic forces gets stronger. This is reflected in their curves. These curves are increasing along the flow and changing its structure into a whirling form as on anode and on cathode.

At the critical moment, when the surface tension forces cannot keep the tongue of the curved shape, the formation of the droplets shower begins. Thus the breakage the tongues, the droplets may fly in directions other than axial (shown by arrows in Fig. 2.19).

From the calculated and the experimental data, it was derived the following:

- (1) During the formation process, the metal droplets are subjected to the influence of the significant forces in directions other than axial;

Fig. 2.19 Droplets formation cycle. $f = 27,000$ frames/s, $P = 276$ kPa



- (2) The value and direction of the forces in the burning zone of the arc is sufficient to explain the possibility of small-diameter droplets;
- (3) The metal droplets have different surface area of in the process of forming;
- (4) Due to the additional heat release in the necks, the droplets temperature at this stage increases comparing to the end face temperature.

These features cause difficulties in obtaining high-quality coatings at AS:

- (1) Combination of a large specific surface area of the droplets with their high temperature stimulates the undesirable input of the gases into the liquid metal.
- (2) To design the nozzle subassembly we need to take special measures to prevent the deflection of droplet's way from the jet axis.

2.5 Description of the Geometry and the Velocity of Two-Phase Flow

In general, the motion of the particles in the two-phase flow can be considered as the motion of a viscous incompressible liquid, which is described by the Navier—Stokes equation:

$$d\overline{W}/dt = -\text{grad}p/\rho + \mu \cdot \nabla^2 \cdot \overline{W}/\rho + \overline{f}, \quad (2.52)$$

where ρ is density; $\overline{W}$ is the velocity vector of the particles; p is pressure; μ is dynamic viscosity of the fluid; $\nabla^2 = (\partial/\partial x^2 + \partial/\partial y^2 + \partial/\partial z^2)$ —Laplace operator; $\overline{f}$ is the resulting volume forces affecting an elementary volume of fluid.

With regard to the coating process, there are forces that caused an impact on moving particles [35]:

- aerodynamic resistance force;
- Basset force, owing to non-stationary processes and depending on the nature of the particle motion in the previous time span;
- Magnus force, occurred due to the rotation of the particles by gradient of velocity of an ambient around flow;
- the force caused by the pressure gradient in the flow;
- thermophoresis force caused by the temperature gradient in the flow;
- the gravity of the particle;
- the force, reflecting the acceleration of the gas layers adjusted to the particle;
- the force caused by the inertia of the gas volume displaced by the particle.

Besides the above mentioned, the motion of the particles must be also influenced by the electrostatic forces, the forces of light pressure, the forces caused by pulsations, and the diffusion of the gas components.

It was estimated that for the hydrogen plasma jet of with the temperature of 3000 K and the particle size of 50 μm , the density of 10^4 kg/m^3 , and the gas velocity of 1000 m/s, the aerodynamic resistance force value is approximately $5 \times 10^{-2} \text{ N}$; Basset and Magnus forces are $0.3 \times 10^{-2} \text{ H}$ and $0.2 \times 10^{-2} \text{ N}$, respectively, other forces are at $10^{-6} - 10^{-8} \text{ N}$ [35]. At AS, the two-phase flow parameters are at the same level, which makes reasonable to calculate the aerodynamic resistance force only, as is done in modern studies of plasma spraying [16].

Other forces are noticeable only in the case of an external conditions change, such as, for instance, the impact against the substrate. The force caused by the pressure gradient when moving in a viscous medium is Magnus force, and so on.

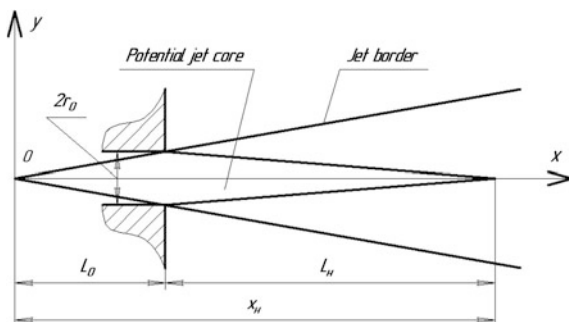
A closed scheme was chosen for calculations (see Fig. 1.4a). Among nozzle designs used for AS, it is the most general and simple for calculations. It has a submerged gas jet, symmetric spraying axis. Other schemes of interaction between the gas and the particles can be reduced to the scheme under consideration. For the carrier gas composition, the variant in which the combustion products of propane–air mixture are supplied into the spray zone was used. All of the main components of the gas mixture are represented: nitrogen, hydrogen, oxygen, carbon, and their compositions. It can be reduced to other nozzle schemes by simplifying.

According to the AS scheme (see Fig. 1.3), the gas jet can be represented as a axisymmetric, flowing out into the motionless space. It is distinguished by low viscosity and high velocity. According to the Reynolds assessment for gas flowing out from the nozzle, at spraying distance is in the range of $Re = (5000–12,000)$. It is several times higher than the critical value, constituting $Re = (2100–2300)$ [56]. This suggests that the motion of the gas is in the turbulent regime.

Based on understanding the physical nature of gas flow, turbulent jets were described by Abramovich [53]. He proposed advanced mathematical apparatus describing the distribution of velocity, temperature, and concentration of the gases adjusted from the atmosphere in any point the jet. The following specific regulations and assumptions were made:

1. Thickening of the jet boundary layer, consisting of a involved particle of the environment and broken particles of the jet itself, leads, on the one hand, to a cross-section increase. On the other hand, it causes a gradual reduction of the potential core of the jet. It is the region lying between the inner rims of the boundary layer (Fig. 2.20). Part of the jet containing the potential core flow is the initial part with the length of X_n .
2. One of the properties of such jet is the constant static pressure over the flow region, whereby the potential velocity in the potential core remains constant.
3. Its common feature is the low value of the transverse velocity components in comparison with the longitudinal velocity. It is usually neglected in engineering calculations.

Fig. 2.20 Turbulent jet schemes



4. The simplified scheme of the jet is used frequently, assuming that there is no transition part between the initial and basic areas.
5. Jet spreading outside the initial section is expressed not only by its cross-section increasing but also by the velocity temperature changes along the axis. In the boundary layer, the parameters asymptotically approach the parameters of the surrounding space.
6. A distribution of excessive concentrations of involved particles and temperature of the jet is similar to the jet velocity kind of dependence.
7. During supersonic jet flow, its geometry is changing.
8. Axisymmetric turbulent jet behaves as if it emanates from a point source—pole (O), where it is convenient to place the beginning of coordinates (see Fig. 2.20). Pole is located inside the nozzle at a relative distance from its face:

$$a \cdot L_0/r_0 = a \cdot (x_H - L_H)/r_0 = 0.29, \quad (2.53)$$

where L_0 is the distance from the pole to the jet nozzle face L_H is the distance from the nozzle face to the cross-section of the initial area end; r_0 is the radius of the nozzle, a is an empirical constant proportional to the length of the mean path of molecules before the collision. It is one of the features of turbulence jet. With uniform initial velocity field, the good agreement with experiment is reached for $a = 0.066$.

Within the framework of the theory of Prandtl, the model of involved particles influence on the turbulent structure of the jet is proposed. According to this model micro volume of the jet in its motion carries involved particles and is slowed by the accumulative force of aerodynamic resistance. In this case, the Navier–Stokes Eq. (2.52) takes the form [35]:

$$m \cdot \frac{dW_p}{dt} = 0.5 \cdot C_d \cdot \rho_g \cdot (W_p - W_g) \cdot |W_p - W_g| \cdot S \quad (2.54)$$

The correlation between gas velocities and particles is also determined by the equation of momentum:

$$dW_g/dx + (G_p/G_g) \cdot (dW_p/dx) = 0, \quad (2.55)$$

where m , S are weight and midship section area of the particle, respectively; C_d is aerodynamic resistance coefficient; ρ is density; G is mass flow; W is velocity; t is time; x is coordinate; indices “g”, “p” refer to the gas and particle, respectively.

Its analytical solution is impossible to perform because of the complex dependencies on the movement coordinate of almost all the terms of the equation. We therefore use a numerical solution method for which we divide the transferring particles interval into sufficiently small segments Δx . considering constant values of the terms within its limits. Then, instead of (2.54), (2.55), we obtain:

$$W_p^{i+1} = W_p^i + 0.75 \cdot \frac{\rho_g^i}{\rho_p} \cdot \frac{C_d^i}{d_p} \cdot \frac{(W_g^i - W_p^i) \cdot |W_g^i - W_p^i|}{W_p^i} \cdot \Delta x, \quad (2.56)$$

$$W_g^{i+1} = W_g^i - \frac{G_p}{G_g} \cdot (W_p^{i+1} - W_p^i) \quad (2.57)$$

Scheme of the jet for calculations is shown in Fig. 2.21.

The confining nozzle of diameter ($2r_0$) is set in the middle section of the atomizing point of wires 1. The nozzle centers the gas stream that separating the drops of the molten metal from the end faces of the electrodes. The combustion products of propane–air mixture, which is generated in the closed space of the protective housing 3 are used as the carrier gas. Protective housing 3 with an installed nozzle 2 prevents the suction of ambient air into the jet prior to its ejection from the nozzle. An expansion of a gas jet at an angle of (2α) takes place at the ejection of the nozzle. The jet has a current width of (2δ) at a distance (x) from the pole of the jet. The length of the initial section (x_H) consists of the distance from the jet pole to the nozzle face (L_0) and the distance from the nozzle face to the end point of the initial zone (L_n).

The initial parameters of the Eqs. (2.56) and (2.57) for $i = 1$, i.e., when $x = 0$ (at the nozzle face) and their change in the length and cross-section of the jet are selected as follows.

For particles ejected at an angle to the axis of the jet, according to the assumptions made, the particle velocity toward the x direction is calculated according to Eq. (2.56), whereas with the parameters related to the point with coordinate y_i , is defined by the equation:

$$y_j = x_i \cdot \operatorname{tg} \alpha_j, \quad (2.58)$$

where α_j is the angle at which the particle flew out from the spray point (see Fig. 2.21).

This scheme assumes that there is no dynamic effect of the gas on the particles at y -axis. Therefore, the initial impulse that defined its movement at an angle to the x -axis was obtained at the point ($x = 0, y = 0$) due to the arc electro-dynamic impact and the expansion of the compressed gas at the exit of the confining nozzle. Half-angle of the jet spread α is divided into equal intervals. Thus, the position and micro volume characteristics the two-phase jet is determined by its coordinates x_i and y_j .

Geometric and velocity characteristics are connected by the following dependencies [53]:

$$\begin{aligned} x_H &= 12.4 \cdot r_0, & \beta_W &= 12.4, \\ \text{If } M_0 < 1.0 : & \delta = 0.22 \cdot x & \text{at } x \geq x_H, \\ & \delta = 0.27 \cdot x & \text{at } x < x_H \end{aligned} \quad (2.59)$$

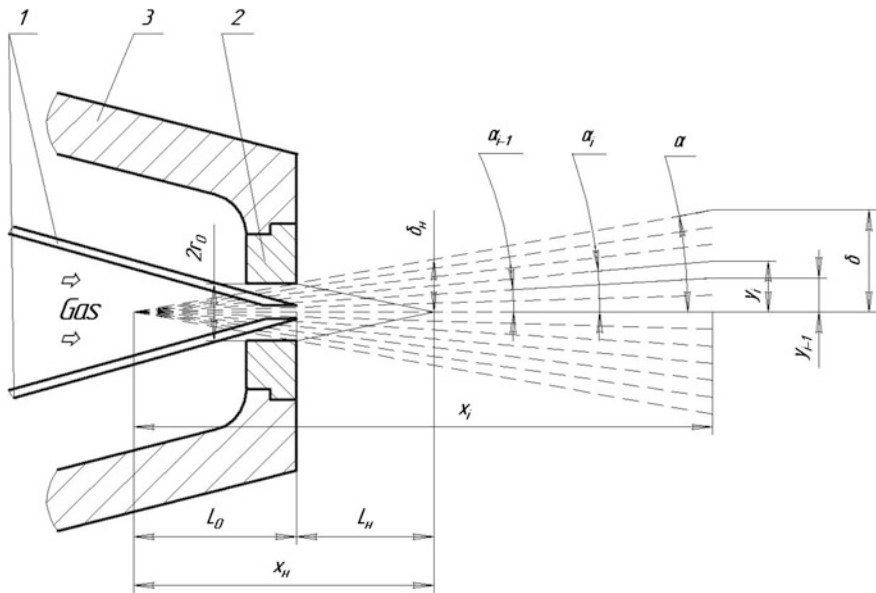


Fig. 2.21 Scheme of the jet for the calculations

$$\text{If } 1 \leq M_0 \leq 1.6 : \quad \begin{aligned} x_H &= (6.4 + 2.2 \cdot M_0^2) \cdot r_0, & \beta_W &= 14, \\ \delta &= (0.165 + 0.045 \cdot M_0^2) \cdot x, \end{aligned} \quad (2.60)$$

where x, y are the coordinates of the point; δ is the radius of the jet for the present coordinate x ; β_W is the coefficient of the velocity change along the axis of the jet; M_0 is the initial Mach number.

The velocity change of the gas jet along the axis (W_{gx}) and along the cross-section (W_{gy}) depends on the initial velocity of the gas (W_{g0}) and its distance from the pole of the jet:

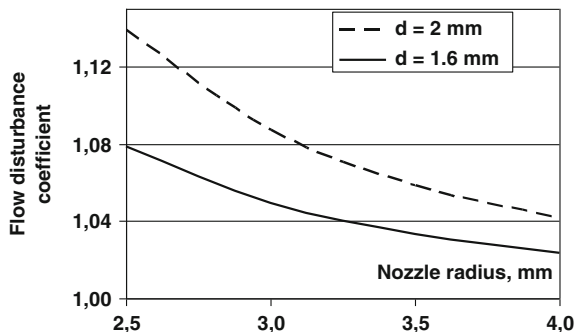
$$W_g = W_{g0} \quad \text{if} \quad x < x_H \quad (2.61)$$

$$W_{gx} = (W_{g0} \cdot \beta_W \cdot r_0) / x \quad \text{if} \quad x \geq x_H \quad (2.62)$$

$$W_{gy} = W_{gx} \cdot \left[1 - (y/\delta)^{1.5} \right]^2 \quad (2.63)$$

The wires—electrodes positioned in the nozzle exit cause an additional turbulence of the gas jet. The high accuracy of the calculations can be reached by using the finite element analysis. Experiments show [57] sufficient for practice calculations accuracy can be achieved while using a coefficient of the flow disturbance ($k_{\text{возб}}$), which reflects an influence of the wires (placed in the nozzle) on the width and

Fig. 2.22 The dependency of the flow disturbances ratio on the radius of the nozzle and diameter of the input wires



velocity of the jet: of the flow disturbance:

$$\kappa_{\text{EO3M}} = ((0,4 \cdot d_{np} / (2 \cdot r_0))^{2,56} + a) / a, \quad (2.64)$$

where $a = 0.066$ is empirical constant.

The radius of the jet is directly, and its velocity is inversely to κ_{EO3M} :

$$\delta \cdot \kappa_{\text{EO3M}}, \quad (2.65)$$

$$W'_{gx} = W_{gx} / \kappa_{\text{EO3M}} \quad (2.66)$$

Calculation according to the Eq. (2.65) was done for typical intervals (wire diameter $d_{np} = 1.6\text{--}2.0$ mm, radius of the emanate nozzle $r_0 = 2\text{--}4$ mm). It shows that disturbance ratio is changing by 2–13 % at wire placing in the nozzle, Fig. 2.22). Proportional to it according to Eq. (2.66) is the spreading radius increase, whereas the velocity of the jet is decreased. It can be seen that the influence on the jet parameters caused by wires is reduced by increasing the wire nozzle diameter. For the limit case of the maximum wire diameter and the minimum radius of the nozzle, the velocity of the gas is reduced by 14 % when you input the wires into the nozzle. For the maximum radius of the nozzle wires input will not have a strong impact, as the deviation of parameters is ~ 3 %.

Aerodynamic resistance coefficient C_d in the formulas (2.54, 2.56) is determined by the Reynolds number:

$$Re = |W_g - W_p| \cdot d_p \cdot \rho_p / \eta_g, \quad (2.67)$$

where η_g is a coefficient of the dynamic viscosity of the gas, kg/(ms).

For a single non-deformable spherical particle, which is moving with a constant velocity in a stationary isothermal and incompressible fluid of infinite extension at $Re = 0.05\text{--}1.0$ the Stokes formula shall be used [35]:

$$S_d = 24 / Re, \quad (2.68)$$

Consideration of various factors amends this formula as the follows:

$$C_d = 0.5 + 29.2 / \sqrt{\text{Ar}} + 430 / \text{Ar} \quad [58], \quad (2.69)$$

If $2 < Re < 10^3$, the free flow regime is

$$C_d = 24 \cdot Re^{-1} + 3.6 \cdot Re^{-0.317}, 0.2 < Re < 4.0 \quad [35], \quad (2.70)$$

$$C_d = 24 \cdot Re^{-1} + 4 \cdot Re^{-0.333}, 4 < Re < 400 \quad [35], \quad (2.71)$$

$$C_d = 1.1, 400 < Re < 5500, f = 1.15 - 1.2 \quad [59], \quad (2.72)$$

$$C_d = 1.8, 300 < Re < 500, f = 1.4 - 1.5 \quad [59], \quad (2.73)$$

$$C_d = 37.8 \cdot Re^{-0.6}, 30 < Re < 400 \quad [59], \quad (2.74)$$

where $f = (S/S_M)_{V=\text{idem}}$ is a geometric shape factor, $\text{Ar} = \rho_0 \cdot W_0^2 / (\rho \cdot g \cdot d_0)$ is a criterion of Archimedes at the nozzle outlet.

It is accepted as it is stated above, that the gas jet is a submerged axisymmetric turbulent jet out flowing directly into immobile space as shown in Fig. 2.20.

In calculations of the geometric characteristics of the gas jet: the length of the initial section (x_n), the jet radius (δ) for the coordinates (x); the dependencies for the jet zone, consisting of two wires to determine the gas velocity and the jet radius, the flow disturbance coefficient is used as in the Eq. (2.65).

The spreading angle of the jet is determined experimentally based on the spray spot size of the spray jet at a working distance and from the photographs of the jet (Fig. 2.23). Although, on the photographs, it can be seen that the spreading angle of the jet varies in distance, the calculations has been assumed that: $2\alpha = 10^\circ$, which corresponds to a straight boundary line of the jet and the absence of the transition area.

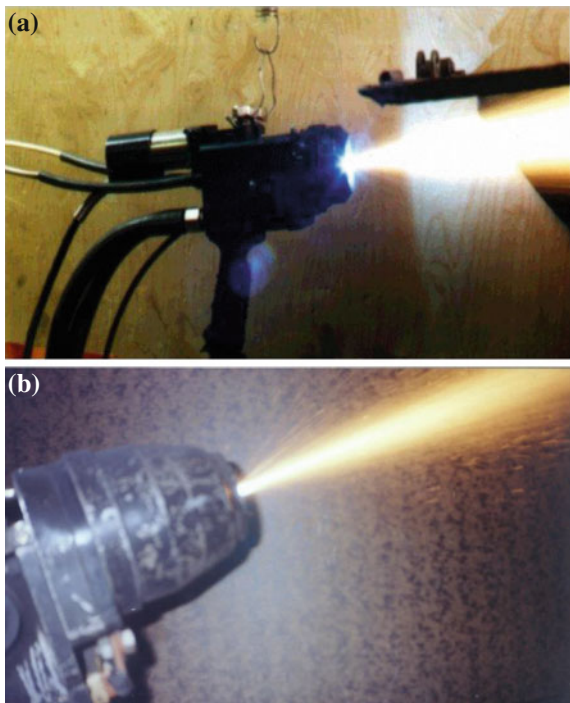
Initial particle velocity was equated to the wire feeding velocity.

Let us estimate the velocity of the elementary volume of metal movement in the tongue. According to Sect. 2.4, “tongue” is a portion of the liquid metal that was drawn off from the electrode face end, but has being retained at the expense of the gas-dynamic pressure balance and surface tension. Its thickness is $2r_s = 7 \times 10^{-6}$ m, where m is the mass of drawn off metal portion; $m = 6.4 \times 10^{-7}$ kg. Let us assume that transverse dimension of the “tongue” will be equal to the diameter of the wire: $b = 0.002$ m, the period of breakage of the liquid metal is $t = 5.74 \times 10^{-4}$ s (Table 2.8). In this case:

$$s = m / (\rho_p \times 2r_s \times b) = 6.3 \cdot 10^{-3} \text{M}; \quad v = s/t = 11.7 \text{M/S},$$

where s is the length of the “tongue”; v is the velocity of movement of the elementary volume in the “tongue”.

Fig. 2.23 AS guns that were adopted as the basic equipment for calculating the parameters of two-phase jet. Steel spraying [8]. **a** Gun with combustion chamber for pre-burning of propane-air mixer of carrier gas. **b** Gun where propane-air mixer of carrier gas is used without pre-burning



The estimated value of the “tongue” length is comparable with the speed cinema shooting data [20, 21]. In the “tongue” the specific surface area is gradually increased comparing with the electrode face end surface. Consequently, there is a further increase in temperature due to the additional heat release in necks.

Velocity changing of the gas in this sector occurs as in the momentum Eq. (2.55). Gas engages with the teared-off portion of the molten metal which mass is similar to 550 μm droplet in diameter. This droplet size is adopted according to the oscillocoping records data according to Table 2.8, [34].

How it was shown above, the portion of the metal in the form of “tongue” in gas velocity calculations was replaced by the scheme shown in Fig. 2.24. In this scheme, the “tongue” is substituted by the droplets of spherical shape. Its diameter is equal to the smallest droplet size $d_p = 10 \times 10^{-6}$ m. At the end of this sector, droplet size corresponds to its distribution according to sieve analysis, (Table 2.15.)

Assume that density, diameter, and overall consumption of particles remain unchanged in distance. At the same time, adopted density is the same as for the liquid iron $\rho_p = 7265 \text{ kg/m}^3$ [27]. The initial velocity of the gas is equal to the outflow velocity of the jet escaping from the combustion chamber taking into account the influence of the wires—electrodes according to the Eq. (2.64) Along the distance and the y-section of the jet with the stipulated calculating step, the gas velocity is changing according to the momentum Eq. (2.55). It is followed by its correction according to the equation of velocity change on the axis, and on the section by

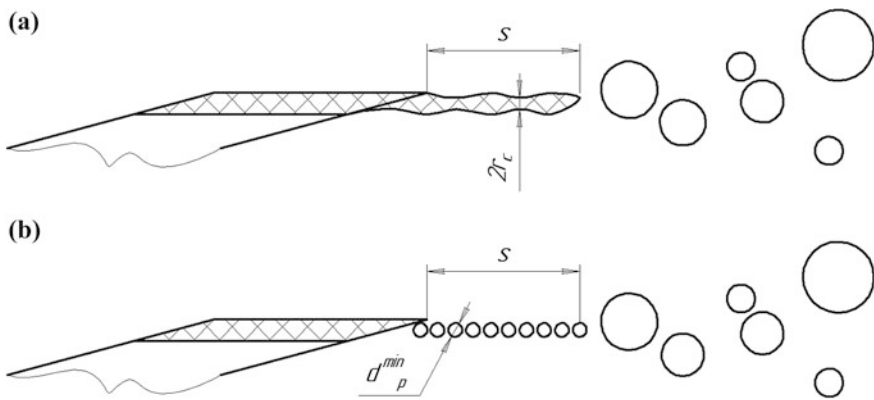


Fig. 2.24 Estimated traffic scheme of drops movement when they are being teared off from the electrode face-end

Eqs. (2.54, 2.61–2.63). It is considered that the gas velocity is equal to the velocity of the environment at the boundary of the jet: $W_g = W_s = 0$ m/s. Gas density is changing in temperature at each step of the calculation.

Aerodynamic resistance coefficient (C_d) was determined according to the calculation of Reynolds equation computed at the previous calculating step (2.70, 2.71, 2.72, 2.74). All other equations were rejected by the following reasons. Stokes formula (2.68) and the equation for the free flow regime (2.69) do not meet the requirements of turbulent nature of the jet. The results of calculations on the dependency differ from the experimental data that were made by Abramovich for large values of Re [53]. Furthermore, the surface tension stands up against a strong deformation of the particles ($f = 1.4–1.5$) as it is shown in paragraph 2.4. When $Re < 0.2$ aerodynamic resistance coefficient becomes unrealistically large. To avoid this, an assumption was made in the calculations. It was assumed that if $Re \leq 0.2$ then $C_d = 126$. This corresponds to $Re = 0, 2$ according to the Eq. (2.70).

Due to the turbulent nature of the jet, let us assume that the distribution of the specific gas consumption on the cross-section is close to uniform. In this case, it is proportional to the square of the j-circular cross-section

$$G_{gj} = G_g \cdot \frac{(y_{j+1}^2 - y_j^2)}{\delta^2} \quad (2.75)$$

Let us assume that the total mass flow rate corresponds to the maximum for nozzle (position 2, Fig. 2.21) which is expressed in terms of the gas-dynamic functions according to [14]:

$$G_g = \frac{m \cdot P \cdot F \cdot y(\lambda) \cdot 3600}{\sqrt{T_g \cdot \tau(\lambda)}}, \quad (2.76)$$

where $P = 0.981 \times 10^5 \text{ N/m}^2$; is the static pressure in the flow,

$$m = \sqrt{k \cdot \left(\frac{2}{k+1}\right)^{\frac{k+1}{k-1}}} \cdot \sqrt{\frac{g}{R}} = 0.035 \text{ m}^{-1} \text{ s K}^{0.5},$$

where λ , $y(\lambda)$, $\tau(\lambda)$ are the gas-dynamic functions:

$$\lambda^2 = \left(\frac{k+1}{2} \cdot M\right) / \left(1 + \frac{k-1}{k+1} \cdot M^2\right), \quad (2.77)$$

$$y(\lambda) = \left(\frac{k+1}{2}\right)^{\frac{1}{k-1}} \cdot \lambda / \left(1 - \frac{k-1}{k+1} \cdot \lambda^2\right), \quad (2.79)$$

$$\tau(\lambda) = 1 - \frac{k-1}{k+1} \cdot \lambda^2, \quad (2.80)$$

where $k = c_p/c_v$ is the adiabatic index, taken as $k = 1.262$ for air at $T = 2100 \text{ K}$ [9].

Mach number (M) was determined experimentally (see below).

In the calculations of cross-sectional area of the nozzle, the presence of a two wires in it with diameter d_{np} merging at cross-sectional angle α_{np} was taken into account, see Fig. 2.21.

$$F = F_c - F_{np} = \pi \cdot \left[r_0^2 - 2 \cdot \left(\frac{d_{np}}{2 \cdot \cos \alpha_{np}} \right)^2 \right], \quad (2.81)$$

where F_c is the total area of the nozzle; F_{np} is the cross-sectional area of the wire.

To determine the specific ($\text{kg/m}^2/\text{s}$) consumption of the particles flow over the cross-section g_j , we assume that it is distributed according to normal—curricular law

$$g_j = g_0 \cdot e^{-k_c \cdot y_j^2}, \quad (2.82)$$

where g_0 is a specific consumption on the jet axis.

For the maximum radius of the spraying material pattern (r), we take the radius of the jet. According to the experimental data, nearly 10 % of the particles are flown off the jet borders of angle (α). At the experiment, the coating on the disk was weighed. The disk of the radius (δ) is placed on the characteristic spraying distance. It allows calculating the concentration coefficient k_c in each section of the annular section of the jet:

$$0.1 \cdot g_0 = g_0 \cdot e^{-K_c \cdot r^2}$$

$$K_c = -\ln 0.1 / r^2 = 2.303 / r^2 \quad (2.83)$$

Integrating of the specific discharge on the cross-sectional area (F) will give the total mass flow of the particles:

$$G_p = \int_F g dF \quad (2.84)$$

Let us expand the integrand:

$$G_p = \int_F g dF = \int_0^\infty g \cdot 2 \cdot \pi \cdot r \cdot dr = \int_0^\infty g_0 \cdot e^{-k_c \cdot r^2} \cdot 2 \cdot \pi \cdot r \cdot dr = \frac{\pi}{k_c} \cdot g_0$$

Accordingly, the fuel particles consumption for each interval Δx_j in each section x_i is:

$$G_{pj} = \int_{y_j}^{y_{j+1}} g_0 \cdot e^{-k_c \cdot r^2} \cdot 2 \cdot \pi \cdot r \cdot dr = G_p \cdot \left(e^{-k_c \cdot y_j^2} - e^{-k_c \cdot y_{j+1}^2} \right) \quad (2.85)$$

The nature of the Eq. (2.85) shows that the largest consumption occurs at the cross-sections which are distanced from the jet axis on the relative distances: $y/\delta \approx 0.4$ (Fig. 2.25).

To select the initial data for calculations and confirm its accuracy, some of the parameters have been determined experimentally. The following are the measurement of the initial velocity of the gas stream, the gas distribution, particle velocity, granulometric size of the spraying jet.

Measurement of the initial velocity of the gas stream flow was carried out using pneumometric head. Its shape was designed for supersonic flow (Fig. 2.26). The initial velocity of a supersonic flow was determined by the Mach number (M) and the sound velocity (a_{3g})

$$W_g^0 = M \cdot a_{3g} \quad (2.86)$$

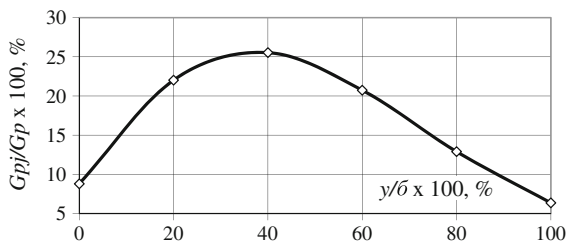


Fig. 2.25 Distribution of relative consumption of particles (G_{pj}/G_p) on the cross-sections of the jet

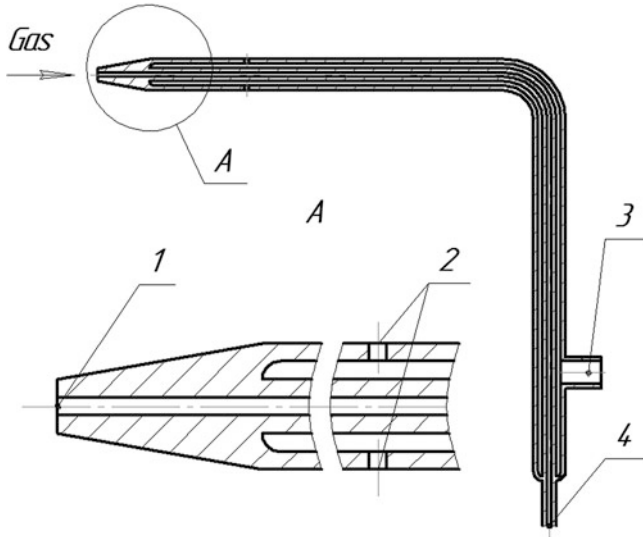


Fig. 2.26 Scheme of pneumometric head for measuring the gas flow rate [14, 60]. 1 Inlet orifice; 2 side orifice; 3 outlet tube for static pressure measuring; 4 outlet tube for total pressure measuring

The Mach number is calculated by the method of successive approximations on the Rayleigh formula [14, 60]:

$$\frac{P_1^*}{P_H} = \left(\frac{k+1}{2} \right)^{\frac{k+1}{k-1}} \cdot \left(\frac{2}{k-1} \right)^{\frac{1}{k-1}} \cdot (M^{\frac{2k}{k-1}}) / \left(\left(\frac{2 \cdot k}{k-1} \cdot M^2 - 1 \right)^{\frac{1}{k-1}} \right) \quad (2.87)$$

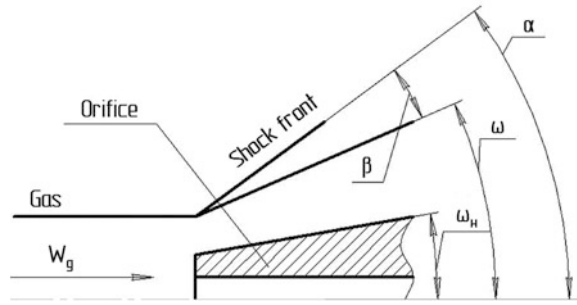
where P_1^* , P_H are total and static pressure of the incoming flow, MPa. When calculating the absolute sound velocity according to the Eq. (2.13), the combustion temperature of propane–air mixture was taken for the temperature of the stream: $T_g = 2112$ K [5]. Adiabatic index of combustion products $k = 1,262$ is taken as for air at $T = 2100$ K [9].

To eliminate the influence of the nozzle design on the results of measurements of the opening half-angle of receiving hole (ω_n) is made than limit angle of the gas flow (ω), (Fig. 2.27):

$$\omega_H < \omega \quad (2.88)$$

Neglecting the difference between bevel front-section in the form of a head and clinch-section which is taken into account when calculating the velocity with the Rayleigh formula (2.87), we can assume that the oblique shock is occurred when supersonic gas flowing around the nozzle [53]. Velocity vector $\vec{W}_g$ and the bevel bow shock front form an angle α before jet with the head reach their meeting point.

Fig. 2.27 The scheme of a pneumometric head for gas flow velocity measurement



Surface of a cone with an apex angle α is the envelope of spherical waves. The angle α is defined by:

$$\sin \alpha = 1/M \quad (2.89)$$

The deflection angle of the jet after crossing the front of the oblique shock ω is:

$$\omega = \alpha - \beta \quad (2.90)$$

According to [53], the angle between the velocity vector and the shock front β is determined by the equation:

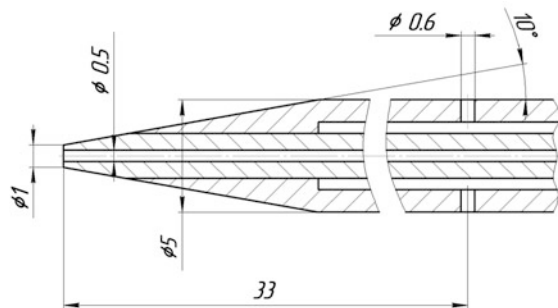
$$\operatorname{tg} \beta = \frac{k-1}{k+1} \cdot \left(1 + \frac{2}{k-1} \cdot \frac{1}{M^2 \cdot \sin^2 \alpha} \right) \cdot \operatorname{tg} \alpha \quad (2.91)$$

Expected Mach number is in the range of 1–1.5. On the basis of the calculations by Eqs. (2.88–2.91), it was assumed that $\omega_H = 10^\circ$.

Characteristic dimensions of the head (Fig. 2.28) are selected from the following conditions [60]:

- side holes 2 are positioned at a distance of not <4–6 head diameters from the front edge. In this case, the pressure P_2 in the tube 3 equals to the static pressure of the oncoming stream P_∞ . Since for the head with $\omega_H < \omega$, the slowing down of

Fig. 2.28 View of the working part of the head



the gases jet takes place without friction and eddies, we can assume that in the tube 4, pressure P_1 is formed. The latter is approximately equal to the total pressure of the incoming flow P_1^* . Thus, the pressures P_H и P_1^* are measured by pressure gauges attached to the head output tubes 3 and 4 respectively;

- the minimum size of the central hole 1 is made of in order to reduce measurement errors.

In this case, the following relation is correct

$$P_1^*/P_H = P_1/P_2 \quad (2.92)$$

According to the measurement and calculation results for the Eqs. (2.86 and 2.87), the initial velocity of supersonic was 950 m/s, when the combustion products of propane–air mixture were flowing out of the nozzle.

Float-type meters were used to determine the gas flow rates (Table 2.13).

Table 2.13 The results of the consumption of compressed air and propane being fed into the combustion chamber

Excess oxidizer coefficient, α_{OK}		0.8	1.0	1.2	
Combustion chamber pressure, MPa		0.32	0.30	0.29	
Air	Pressure, MPa	0.42	0.42	0.42	
	Density, kg/m ³ [62]	4.94	4.94	4.94	
	Consumption	Volume, m ³ /h	1.6	1.6	1.7
		Mass, kg/h	7.9	8.1	8.2
Propane	Pressure, MPa	0.40	0.38	0.35	
	Density, kg/m ³ [9]	7.67	7.28 ^a	6.70 ^a	
	Consumption	Volume, m ³ /h	0.085	0.069	0.057
		Weight, kg/h	0.65	0.50	0.38
Gas mixture consumption, kg/h		8.57	8.57	8.58	

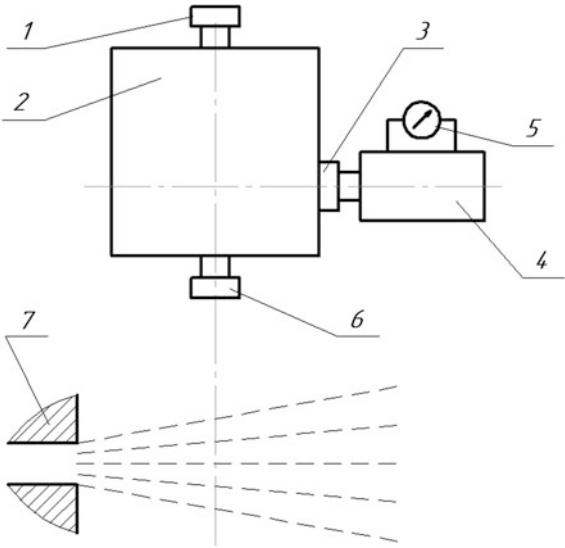
^aObtained by the linear approximation of table data

Determination of the particles velocity was carried out using the velocity rate meter for the luminous objects ИССО-1. Device operation (Fig. 2.29) is based on the principle of a rotating mirror. Mirror rotation axis is parallel to the axis of the gas jet. In this case, a perpendicular component that is proportional to the frequency of the mirror rotation applied to the projection of the particle velocity. In the case of the particle motion at a constant velocity, its track is a straight line. The values of the particle velocity are defined as:

$$W_p = V \times ctg\alpha, \quad (2.93)$$

where V is the liner rotating velocity of the mirror cylinder, m/s, α is an angle between the jet axis and the particle track.

Fig. 2.29 Operating scheme of the device “ИССО-1” [61].
1 is an eyepiece; 2 is a registration module; 3 is a mirrored cylinder; 4 is an engine tachometer; 5 is an indicator of the rotational speed; 6 is lens; 7 is a nozzle



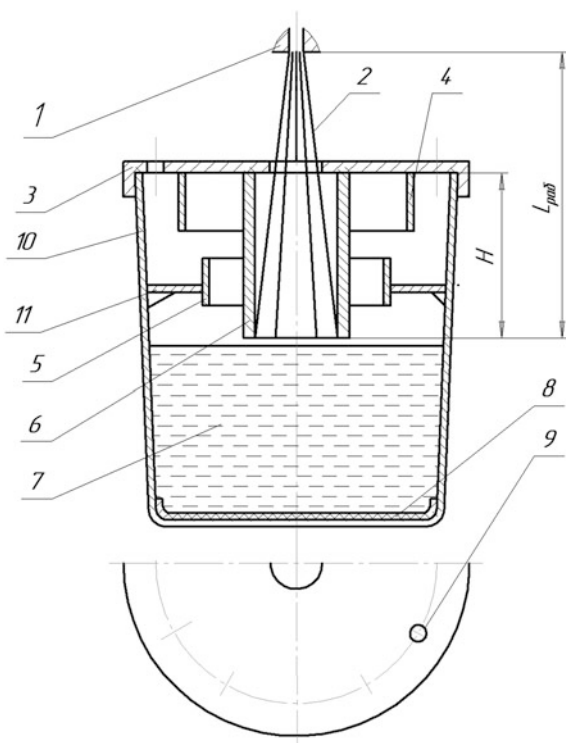
By varying the rotation speed of the mirror cylinder in the device, we can achieve the parallelism of luminous particles tracks and control lines with fixed angles, visible through the eyepiece of the instrument. Then, the magnitude of the velocity is counted on the dial indicator. Particle’s velocity measurement was performed for the two models of arc spraying guns: with and without combustion chamber that is installed in the nozzle area. Modes: $I_d = 210$ A; $U_d = 30$ V; combustion chamber pressure equals 0.32 MPa; the pressure in the spraying head is between 0.22 and 0.23 MPa; wire material is steel: X20Cr13Ø1, 6 mm. The measurement results are shown in Table 2.14. The scatter of these values is related to the difference in velocities of the particles of different diameters.

Particle size distribution at AS with steel solid wires was done according to the spraying into the water scheme. The developed device for collecting the particles (Fig. 2.30) comprises a vessel 10 into which the water 7 is poured. Two-phase “particles-gas” jet 2 from the spraying gun 1 passes through the central hole of the

Table 2.14 The particles velocity measurements for arc spraying, m/s

The distance from the nozzle face, mm		50	100	150
Spraying head with a combustion chamber	Velocity at the upper border of the interval, m/s	134	125	120
	Velocity at the lower border of the interval, m/s	110	100	100
Spraying head without a combustion chamber	Velocity at upper border of the interval, m/s	62	75	70
	Velocity at the lower border of the interval, m/s	42	52	46

Fig. 2.30 The droplets collecting device for measuring its fractional composition. Explanations are followed in the text



cover 3, and then through the tube 6 it gets into the water. Sufficient diameter of the hole ensures achieving water all of the sprayed metal droplets in water. Height (H) of tube 6 is chosen so that the nozzle face of the spraying gun is at spraying working distance ($L_{paδ}$) from the water surface.

In order to prevent the splashing out of water under the influence of air jet, the inside cover 11 with the sleeve 5 and the sleeve 4 were installed. They are forming a system of labyrinths to extinguish the kinetic energy of water. To release excessive air pressure inside the container 10 on the cover 3 there are special holes 9. When falling into the water, the particles cool down and settle on the tray 8 which is made from stainless steel. Thus, under the influence of air jet, the water is suspended inside the container 10 and therefore the metal droplets are subjected to a slight deformation due to their contact with it, while the conditions of heat dissipation into water are improved.

The obtained powder that was collected on the tray/pallet during spraying, is being dried for 2 h at a temperature of 200 °S. Fractional composition of the particles was determined by sieve analysis method by weight share based on GOST 18318-73 (Table 2.15).

In the diameter range 20–100 μm , the pitch mesh sizes was 10–15 μm . Particles of larger size fractions of 100–630 μm have been sown through sieves with a

bigger step. No evidence of particles with a diameter bigger than 315 μm has been found. The experiments were performed using AS gun with the combustion chamber, $I_d = 200\text{ A}$, $U_d = 30\text{ V}$. The spraying distance was 100 mm and the steel wire X20Cr13 was used as a feedstock.

Calculations were made according to the accepted model of AS processes for the average diameters at each fraction interval as in Table 2.15. Comparison of experimental data on the velocity of the particles with the calculated ones is presented in Fig. 2.31. The curves of the experimental data correspond to the points listed in Table 2.13 when using a spray head with a combustion chamber. The diameters of the particles with a particular velocity changes rates were used for calculations. The graph shows the average calculated cross-section velocities of particles. Average velocities of the particles decrease with an increase of their

Table 2.15 Fractioned composition of particles according to experiments

Particle sizes intervals, microns											
0–20	20–35	35–40	40–50	50–63	63–71	71–80	80–100	100–125	125–160	160–200	200–315
Fraction weight content, %											
7.0	6.6	6.6	8.2	7.6	10.0	11.8	12.0	10.0	7.8	6.0	6.4
Area share of fraction, %											
38.9	10.4	3.9	6.6	7.7	4.8	4.9	8.1	6.5	4.1	2.3	1.9

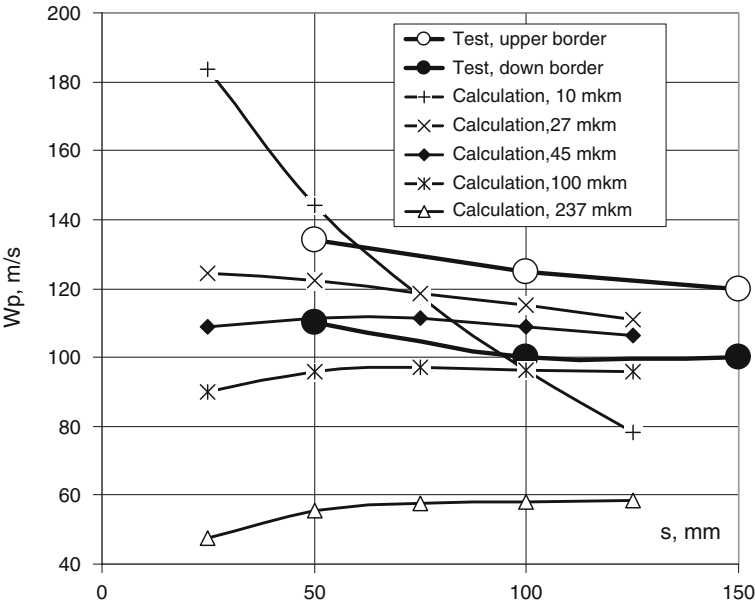


Fig. 2.31 Comparison of experimental data on the velocity of the particles with the calculated ones

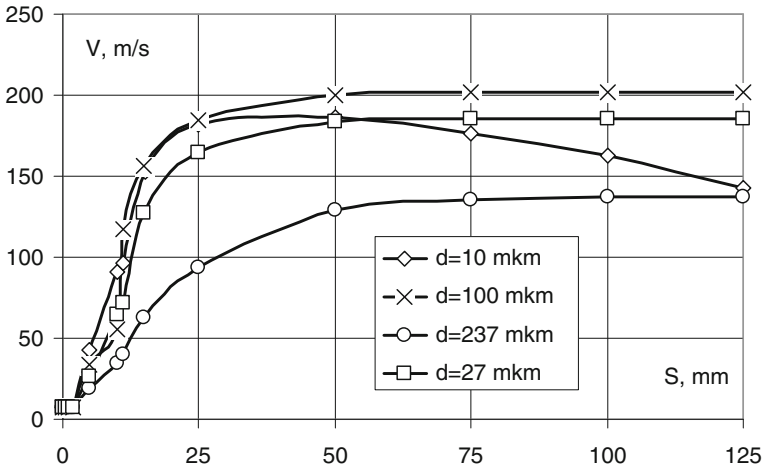


Fig. 2.32 Changing of velocity particles with different diameters on spraying distance. The axis of the jet

diameters. For particles at the diameter range of 20–125 μm , and the proportion of which accounts for about 75 wt%, a satisfactory agreement between the calculated velocities with the experimental was noted. Deviation is $<10\%$. The calculated velocities of the particles of larger diameter differ from the experimental ones by 20–50 %. This probably is related to the experimental procedure. Gas stream energy may not be sufficient to attract particles of a larger diameter, which are scattering across the main direction of the jet when breaking the “tongue.” This leads to the fact that the tracks of those particles do not enter the objective lens of the device and therefore are not recorded therein.

Particle velocities on the jet axis are approaching the maximum value at the distance of about 50 mm, (Fig. 2.32). At a working distance of 100–125 mm, velocity varies slightly for the most fractions. Only the particles $d_p = 10 \mu\text{m}$ reach the maximum velocity at the distance of ~ 25 mm, and the velocity decrease later. In the range of particle diameters in between 27 and 100 μm , the ratio of the weight to the particle cross-section is such, that the velocity of larger diameter particles exceeds the numbers for the smaller ones. Hereafter, for the range of diameters in between 100 and 237 μm , the dependence is reversed. The velocity of bigger particles (in diameter) is lower due to its greater inertia. In the cross-section of the cone corresponding to $y/\delta > 0.8$, the velocity of particles is in the range of 50–80 m/s, but on the jet axis the velocity of particles is 160–200 m/s.

Gas and particle velocities on the jet axis are compared at a distance of 75–100 mm. Subsequently, the gas velocity becomes lower (Fig. 2.33).

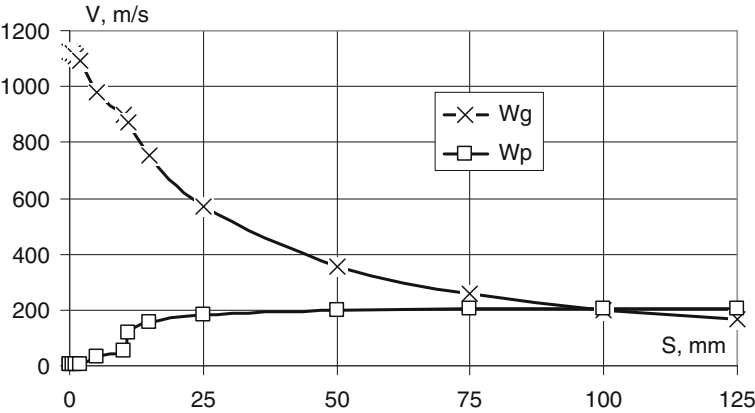


Fig. 2.33 Changing of gas and metal droplets velocities for the spraying distance. Axis of the jet

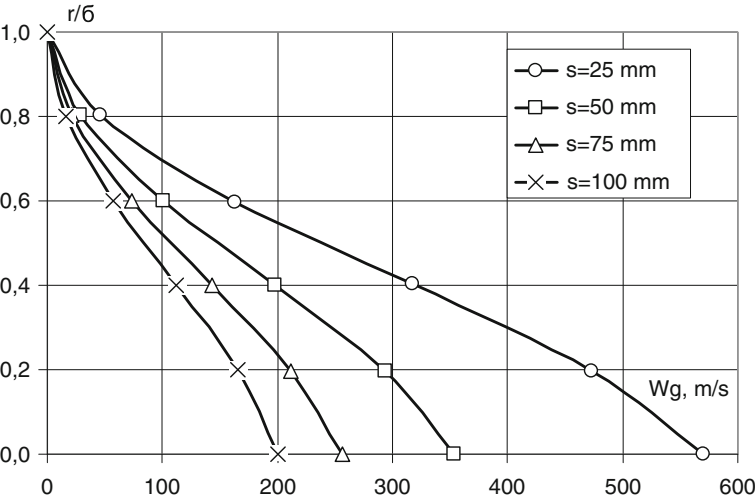


Fig. 2.34 Changing the gas velocity on the jet axis at a various distance of spraying when the diameter of particles is 100 μ m

On the jet cross-section, the velocity of droplets and gas decreases drastically, whereas the gradient of the velocity drop goes down when the distance from the nozzle gets bigger (Figs. 2.34 and 2.35).

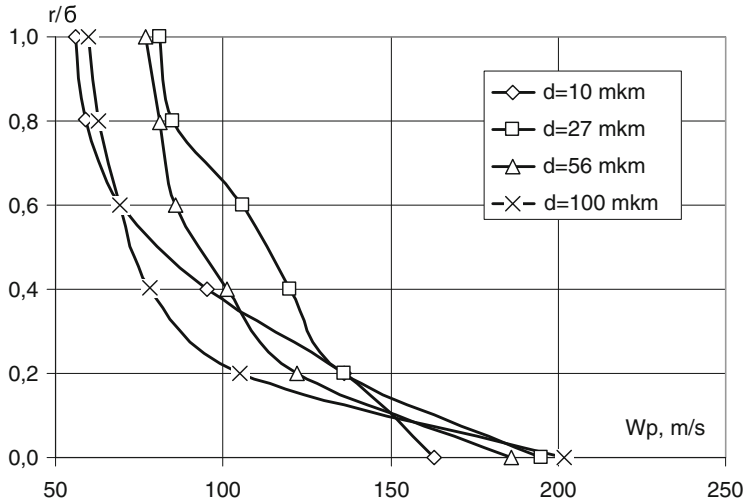


Fig. 2.35 Changing the particles velocity on the cross-section of the jet at a distance of 100 mm

2.6 Temperature Calculation of the Two-Phase Flow

The heat balance of sprayed droplets for AS can be represented as a typical scheme adopted for the TS processes (Table 2.16).

The choice of the heat exchange limiting stage “gas-drop” is made by the criterion Bio:

$$Bi = \alpha_T \cdot d_p / \lambda_p, \tag{2.94}$$

where α_m is the heat transfer coefficient, W/(m² K); d_p is the particle diameter, m; λ is the thermal conductivity of the particle material, W/(m K).

When $Bi > 1$ the heat transmission is limited by heat transferring into the depth of the particle (materials with low thermal conductivity). From the data presented by Kudinov (Table 2.17), it is obvious that for metallic materials used in AS, $Bi \ll 1$ and the loss on the internal heat transferring is negligible. Studies made by other authors can confirm this statement [11, 35, 59].

As plasma flows at a temperature about 10⁴ K refractory materials lose significant amount of its heat. In the plasma process models, these losses are always taken

Table 2.16 The heat balance of sprayed droplets for TS [16, 63]

Gas		Metal droplets		
Heat transfer to the environment	Heat exchange with the particles	Heat exchange with the gas	Irradiation	Evaporation

Table 2.17 Bio criteria in plasma of various gases while heating the particles of 100 μm in diameter [63]

Material		ZrO ₂	Al ₂ O ₃	MgO	BN
λ , W/(m K)		2.39	5.86	5.86	9.2
<i>Bi</i>	Ar	0.18	0.07	0.07	0.05
	N ₂	0.63	0.26	0.26	0.16
	NH ₃	3.5	1.4	1.4	0.9
	H ₂	5.5	2.2	2.2	1.4
Material		SiC	Ni	Fe	W
λ , W/(m K)		41.9	18	40	100
<i>Bi</i>	Ar	0.01	0.02	0.01	0.004
	N ₂	0.04	0.08	0.04	0.015
	NH ₃	0.2	0.5	0.2	0.08
	H ₂	0.3	0.7	0.3	0.1

In the calculations the following values were taken: $Nu = 2$; the mass-average plasma temperature: for argon it is 10,000 K; for nitrogen it is 5000 K; for hydrogen and ammonia it is 3500 K. The thermal conductivity coefficient of the particles is taken for the temperature equals the half value of the melting point of its material

into account [16, 17, 35]. However, the temperature level for AS is lower, than for plasma spraying. Therefore, we are to estimate the necessity to account the irradiation losses.

Let us calculate these losses, based on the methodology proposed by Erokhin for the droplets transfer in arc welding [4], using the following system of differential equations:

for droplets thermal conductivity:

$$dQ_p = -c \cdot m_p \cdot dT \quad (2.95)$$

for irradiation losses

$$dQ_p = \xi \cdot \sigma_0 \cdot F \cdot (T_p^4 - T_s^4) \cdot dt \quad (2.96)$$

It is more convenient from the point of integration to solve the equation for T beforehand:

$$c \cdot m_p \cdot dT = -\xi \cdot \sigma_0 \cdot F \cdot T_p^4 \cdot dt,$$

where ξ is the emittance coefficient; σ_0 is a constant value of irradiation; T is temperature; F , t refer to the surface and the time of irradiation; indices “ p ”, “ s ” refer to the particles and the environment.

To estimate the maximum heat losses, it was assumed that the surface temperature $T_s = 0$. Separating the variables we obtain:

$$dT/T^4 = -A \cdot dt \quad \text{где } A = \xi \cdot \sigma_0 \cdot F / (c \cdot m_p)$$

$$\int_{T_0}^T T^{-4} \cdot dT = -A \cdot \int_0^t dt,$$

$$1/T^3 - 1/T_0^3 = 3At,$$

$$T = T_0 \cdot (1 + 3 \cdot A \cdot T_0^3 \cdot t)^{-1/3},$$

Let us substitute the value of T into the Eq. (2.96):

$$dQ_p = -\xi \cdot \sigma_0 \cdot F \cdot T_0^4 \cdot (1 + 3 \cdot A \cdot T_0^3 \cdot t)^{-4/3} \cdot dt,$$

$$Q_p = -\xi \cdot \sigma_0 \cdot F \cdot T_0^4 \cdot \int_0^t (1 + 3 \cdot A \cdot T_0^3 \cdot t)^{-4/3} \cdot dt,$$

Let us bring the expression to the following form:

$$Q_p = \xi \cdot \sigma_0 \cdot F \cdot T_0^4 \cdot (1/3 \cdot A \cdot T_0^3) \cdot \int_0^t (3 \cdot A \cdot T_0^3) \cdot (1 + 3 \cdot A \cdot T_0^3 \cdot t)^{-4/3} \cdot dt$$

According to the theorem on the invariance of integration and taking into account that

$$(3 \cdot A \cdot T_0^3) \cdot dt = d(1 + 3 \cdot A \cdot T_0^3 \cdot t),$$

let us integrate:

$$\begin{aligned} \int_0^t (3 \cdot A \cdot T_0^3) \cdot (1 + 3 \cdot A \cdot T_0^3 \cdot t)^{-4/3} \cdot dt &= \int_0^t (1 + 3 \cdot A \cdot T_0^3 \cdot t)^{-4/3} \cdot d(3 \cdot A \cdot T_0^3 \cdot t) \\ &= -3 \cdot (1 + 3 \cdot A \cdot T_0^3 \cdot t)^{-1/3} \Big|_0^t \\ &= -3 \cdot \left[(1 + 3 \cdot A \cdot T_0^3 \cdot t)^{-1/3} - 1 \right] \\ &= 3 \cdot \left[1 - (1 + 3 \cdot A \cdot T_0^3 \cdot t)^{-1/3} \right] \end{aligned}$$

Substituting the value of the integral, we obtain:

$$Q_p = (\xi \cdot \sigma_0 \cdot F \cdot T_0 / A) \cdot \left[1 - (1 + 3 \cdot \xi \cdot \sigma_0 \cdot F \cdot T_0^3 \cdot t / (c \cdot m_p))^{-1/3} \right]$$

or

$$Q_p / m_p = c \cdot T_0 \cdot \left[1 - (1 + 3 \cdot \xi \cdot \sigma_0 \cdot F \cdot T_0^3 \cdot t / (c \cdot m_p))^{-1/3} \right] \quad (2.97)$$

The irradiation losses χ_p represent the fraction of the heat coming on to the irradiation from the enthalpy of droplets:

$$\chi_p = (Q_p / m_p) / \Delta h_{\kappa} \quad (2.98)$$

Let us compare the value of these losses for AS with arc welding. For the processes, the values of the droplets specific surface area and the emission time (t) differ in order of 1–3 steps. The parameters are taken based on the following:

- The value of t is assumed to be equal to the frequency of the droplets discharge in the combustion zone of the arc, where $T_0 = 2500$ K; and the droplets flying time at a distance, when $T_0 = 1809$ K;
- According to [4], $\xi = 0.4$ for the clean steel surface (combustion arc zone), $\xi = 0.8$ for droplets coated by slag (flight at a distance);
- The droplets diameter in the burning zone of the arc for AS is taken according to the idea of “tongue” formation. The latter is blown away from the electrode end face, the thickness of which is equal to the minimum size of the particles;
- For the flying of droplets at a distance, the average droplet size is taken according to the particle size analysis (Table 2.15).
- Specific enthalpy of the molten iron $\Delta h_{\kappa c}$ is calculated according to [4]

$$\Delta h_{\kappa} = 1319 + 0.84 \cdot (T_p - 1807).$$

- The values of the specific heating capacity (s_p) for the combustion zone of the arc and for the arc welding are taken according to [4]; for the flight distance the value is taken according to the data [13].
- The calculation of the droplets number (n) and the total droplets surface area (F) is made for a conventional productivity of spraying/welding of 8 kg/h.

From the presented data calculations, there are the following conclusions (Table 2.18):

- (1) The main share of the irradiation losses, >80 %, occurs in the burning zone of the arc.
- (2) Due to the small droplet lifetime at AS, the losses are lower than in arc welding, although the total irradiating surface of the droplets is significantly higher.

Table 2.18 Loses by radiation in arc welding and AS

Parameters	AS		Arc welding [4]	
	Arc burning zone	Droplet flight at a distance	Metal drop transfer	Fine spray metal transfer
Emissivity, ζ	0.4	0.8	0.4	0.4
Stefan-Boltzman constant, σ_0 , W/(kg K)	5.67×10^{-8}			
Specific heat capacity, c_p , W/(kg K)	840	740	840	840
Initial metal temperature, T_0 , K	2900	1809	2500	2500
Droplet separation period, t, s	5.73×10^{-4}	1.70×10^{-3}	0.18	0.018
Droplet diameter, $d_p \times 10^6$, m	10	100	1740	420
Metal density, ρ , kg/m ³	7200	7200	7200	7200
Droplet mass, m_p , kg	3.8×10^{-12}	3.8×10^{-9}	2.0×10^{-5}	2.8×10^{-7}
Droplet surface, s , m ²	3.1×10^{-10}	3.1×10^{-8}	9.6×10^{-6}	5.6×10^{-7}
f/m , m ² /kg	83	8.3	0.48	2.0
Number of droplets, pcs.	2.1×10^{12}	2.1×10^9	4.0×10^5	2.8×10^7
Total surface area, F , m ²	660	66	3.8	16
Q_p/m , kJ/kg	41	6.9	97	42
Specific heat enthalpy of iron, Δh , kJ/kg	1901	1321	2069	2069
Irradiation loses, χ_p , %	3.3	0.5	3.8	1.6

(3) The value of the irradiation losses for AS is 0.5–3.3 % of the droplets enthalpy.

As a result of the high temperatures at plasma spraying, the major role in the heat balance of metal droplets belongs to evaporation. For instance in plasma spraying, the spraying radius of tungsten particles in the plasma jet is reduced by 78 % due to evaporation (diameter of 20 μm) and by 9 % (diameter of 80 μm) at a temperature of about 10,000 K [64].

The metal loss evaluation due to evaporation for AS was conducted in the research [65]. From the energy balance equation for the melting process of the electrodes with respect to one cycle of the arc, it was derived that for evaporated metal, the χ

$$\chi = (E - M \cdot H(T)) / (M \cdot (H_u - H(T))), \quad (2.99)$$

where E is the energy released in the active spots of the electrodes within one splash/discharge; $H(T)$ is the enthalpy change from room temperature to T_{mT} during heating, whereas phase transitions is considered; H_u is the enthalpy change during

evaporation; $M = \pi \cdot d_p^2 \cdot v \cdot \rho_p / (2 \cdot \tau)$ is the amount of molted metal breaking away from two electrodes at a drop separation frequency τ , and wire feeding velocity v .

The calculation of the average mass temperature T was conducted taking into consideration the derived equation of temperature distribution at the end of the electrode.

$$T(l) = T_{\text{kun}} \cdot L \cdot [\exp(a \cdot l/L) - 1] / (a \cdot l), \quad (2.100)$$

where $a = \ln(T_{\text{nn}}/T_{\text{kun}})$, L is the thickness of the liquid layer at the end of the electrode, l is the thickness of the evacuated part of the liquid metal at the end of the electrode.

The estimated share of the evaporated metal for the close to characteristic modes ($I = 270$ A, $U = 25$ V) is in the range of 0–8 % depending on the assumed value of fractured layer (l). The further increase of the arc power according to the estimation from [65], leads to evaporation losses up to 20 %.

This is comparable with the systemized by Erokhin data when applying to arc welding. While using bare the estimation from [65] leads to evaporation losses up to 6–25 % [4]. However, the metal heat losses on evaporation are not determined by the total amount of evaporated metal, but only by that part which was not condensed in the spray screen due to deviation of the vapor stream.

Erohin has analyzed the experiments conducted by Potapevskiy at impulse welding, [66] together with his own data at MIG welding. Besides, he used calculation results about share of the evaporated metal from the electrodes ИМЕТ–4 due to the amount of metal which has passed into the slag in the form of oxides.

Based on these data, he has concluded that the metal loss during evaporation can take ≤ 0.5 %. A similar value of the evaporation loss is given for the iron arc melting by Zamoruev [67].

Accordingly, for the arc spraying that is similar to the arc welding process it was assumed that the heat losses of the flying particles on evaporation can be neglected in the subsequent calculations.

Since it is assumed, that the gas temperature decrease in the presence of particles is due to the heat transfer from gas to particles, the equation of heat exchange “gas-metal droplets” can be written as follows:

$$d(c_p^p \cdot m_p \cdot T_p) / dx = -d(m_g \cdot h_g) / dx, \quad (2.101)$$

where, $\alpha_T = Nu \cdot \lambda_g \cdot d_p$ is the heat transfer coefficient, W/(m²K); h_g is the gas enthalpy, J/kg.

Gas heat exchange with the environment was studied by Abramovich [53]. It was assumed that the turbulent jets of the dimensionless temperature field in a gas jet are proportional to the curves of the dimensionless velocity values:

$$(T_{gx} - T_{gs}) / (T_{g0} - T_{gs}) = 0.86 \cdot W_{gx} / W_{g0}, \quad (2.102)$$

$$((T_{gy} - T_{gs})) / ((T_{gx} - T_{gs}))^{1/Pr_T} = W_{gy} / W_{gx}, \quad (2.103)$$

where, T_g is the temperature of the gas jet, K; additional indexes “0”, “x”, “y”, “s” refer to the parameters of the initial section: on the axis, at the distance y from the axis and in the surrounding space, respectively; $Pr_T = \nu/a$ is the turbulent Prandtl number; ν is the kinematic viscosity coefficient, m^2/s ; $a = \lambda / (c_p \cdot \rho)$ is the coefficient of tubular temperature conductivity; m^2/s ; c_p is the specific heat capacity of the metal droplets at a constant pressure, $J/(kg \cdot K)$.

Note that the volume distribution of the two-phase flow parameters is not usually considered in the calculations for AS. Hence, it reduces the accuracy of the calculations. Thus, Kretschmar, Vadvivasov considered parameters for the jet axis [34, 44]. Lyalyakin et al. calculated the parameters of the flying particles “... for the values of the gas parameters on the axis of the gas jet.” [19].

Convective heat exchange, which is responsible for determination of the heat transferring in a two-phase jet, is estimated by the Nusselt number (heat):

$$Nu = q_n \cdot d_p / (\lambda_g \cdot (T_g - T_p)), \quad (2.104)$$

where, q_n is the specific heat stream onto the sphere, W/m^2 .

For the spherical particles, the formulas like $Nu = f(Re, Pr)$ are used. During the study of individual fixed spherical particles in low temperature pressures conditions with constant thermal properties the dependencies that are mentioned by Klubnikin [11] were obtained:

$$Nu = 2 + 0.6 \cdot Re^{0.5} \cdot Pr^{0.333}, \quad \text{at } 0 < Re < 200, \quad (2.105)$$

$$Nu = 2 + 0.459 \cdot Re^{0.55} \cdot Pr^{0.333}, \quad \text{at } 0.1 < Re < 2 \times 10^5, \quad (2.106)$$

Tsvetkov proposed the following expression for Nu [35]:

$$Nu = 2 \cdot \lambda_{en} / \lambda_e + 0.6 \cdot Re^{0.5} \cdot Pr^{0.333} \cdot (\rho_g \cdot \mu_g / (\rho_{en} \cdot \mu_{en}))^{0.2}, \quad (2.107)$$

where μ is the coefficient of dynamic viscosity, $Pa \cdot s$; index “en” refers to the properties of the gas at the particle surface temperature; index “g” refers to properties of the gas that has no contact with the particle.

Byrov reported that the experimental data about heat exchange between the particles moving in the stream can be determined by the formula [68]:

$$Nu = 0.186 \cdot Re^{0.8} \quad (2.108)$$

From the stated above dependencies, the most preferable for determination of the Nusselt number (2.105–2.108), is the equation of Tsvetkov (2.107). In Eq. (2.108) the

deviation from the ideal gas properties (Prandtl number) is not considered. In comparison to the Eqs. (2.104–2.106), where a correction due to heat transfer conditions depending on the parameters of the boundary layer on the particle is added.

So due to above mentioned analysis lost on irradiation and evaporation of the droplets should be neglected. In the heat exchange equation of the balance of the sprayed droplet at AS, we will take into account only the thermal conductivity:

$$\frac{dT_p}{dx} = \frac{6 \cdot \alpha_T}{\rho_p \cdot c_p^p \cdot d_p} \cdot \frac{(T_g - T_p)}{W_p} \quad (2.109)$$

The other balance equation relates to the heat exchange “gas-metal droplets”:

$$\frac{d(c_p^p \cdot m_p \cdot T_p)}{dx} = - \frac{d(m_g \cdot H_g)}{dx} \quad (2.110)$$

$$dH_g = c_p^g \cdot dT_g \text{ and } m_p/m_g = G_p/G_g$$

Let us do the same as in Sect. 2.5 and use the numerical method for solving the differential Eqs. (2.109 and 2.110). In this case, within the step limits (Δx), the values c_p^p and m_p may be considered as constant. Since $dH_g = c_p^g \cdot dT_g$ and $m_p/m_g = G_p/G_g$ the Eq. (2.110) should be transformed as follows:

$$\frac{dT_g}{dx} = - \frac{c_p^p}{c_p^g} \cdot \frac{G_p}{G_g} \cdot \frac{dT_p}{dx}, \quad (2.111)$$

where, c_p^p , c_p^g is the metal heat exchange of the particles and gas at the constant pressure; G_p , G_g are the mass consumption/flow of particles and gas.

Equations (2.109, 2.110) take the form:

$$T_p^{i+1} = T_p^i + \frac{6 \cdot \alpha_T^i}{\rho_p \cdot c_p^{pi} \cdot d_p} \cdot \frac{(T_g^i - T_p^i)}{W_p^i} \cdot \Delta x \quad (2.112)$$

$$T_g^{i+1} = T_g^i - \frac{c_p^{pi}}{c_p^{gi}} \cdot \frac{G_p}{G_g} \cdot (T_p^{i+1} - T_p^i) \quad (2.113)$$

To calculate the gas heat exchange with the surrounding space, let us use tubular jet dependencies (2.102, 2.103).

Thus, to determine the temperature in the selected section at each step the calculation is made successively to the Eqs. (2.112, 2.113, 2.102 and 2.103).

We assume that the temperature at the end of the electrode 2500 K, which corresponds to the values defined Erokhin and Pokhodnya at arc welding [4, 69]. According to our proposed scheme, in the “tongue” there is a further increase in temperature due to the appearance of necks. To evaluate the processes in the arc

burning, it is assumed that in this zone the initial temperature of droplets is 2900 K with subsequent changes because of heat transfer due to “gas-droplet”.

We assume that the droplets temperature after lowering to the melting point remains constant until the complete solidification of metal (the super cooling of the liquid metal is neglected). In the calculations, it is treated as follows. At each step of the calculation, the droplet temperature is compared with the temperature of its melting point (T_m). Upon reaching the later, the droplet temperature at each successive point remains constant. Moreover, the heat lost by the particle at all subsequent points is summed.

$$\sum_{i=1} Q^i = \sum_{i=1} c_p^i \cdot m_p \cdot (T^{i+1} - T^i) \quad (2.114)$$

This heat at each step is compared with a melting heat of the particles:

$$Q_{nn} = \Delta h_{nn} \cdot m_p, \quad (2.115)$$

where, Δh_{nn} is a specific enthalpy of melting, J/kg.

When the following condition $\sum_{i=1} Q^i > Q_{nn}$ is met, the temperature fall of the solid particle is taken into account on the further steps of calculation.

Strictly speaking, the calculated temperature of the particle is mass average and is not constant over the cross-section of the droplets. However, the criteria $Bi \approx 0.04 \ll 1$ taking into account the large value of the thermal conductivity of the sprayed material (steel, $\lambda \approx 40$ W/(m K)) and the small diameter of particles. Therefore, the internal heat transfer, core and periphery temperature of droplets should be neglected in calculation.

The analysis carried out by the proposed calculation model revealed the following. On the axis of the jet, metal droplet temperature decreases to the melting point at a distance of 15–25 mm, (Fig. 2.36.) In the droplets of the majority of fractions, the melting heat Q_{nn} is high enough to make this the temperature remained unchanged throughout the spraying distance and to keep the droplets in the liquid state. Only for fractions of 10 μ m or less quantity of heat release exceeds Q_{nn} at a distance of 100 mm. After that the temperature begins to drop below the melting point. Hence, the droplets solidify.

Gas and droplets temperatures are compared at a distance of 15–25 mm. Subsequently, droplets temperatures exceed the gas temperature, Fig. 2.37.

To analyze the interaction of droplets with the substrate, it is necessary to assess the state of the drop when approaching the sprayed surface. Let us introduce the concept of the solidification degree R_{ome} , representing the ratio of the total heat loss of the droplet $\sum_{i=1} Q^i$ to the heat of droplet melting Q_{nn} :

$$R_{ome} = \left(\sum_{i=1} Q^i \cdot 100 \right) / Q_{nn} \quad (2.116)$$

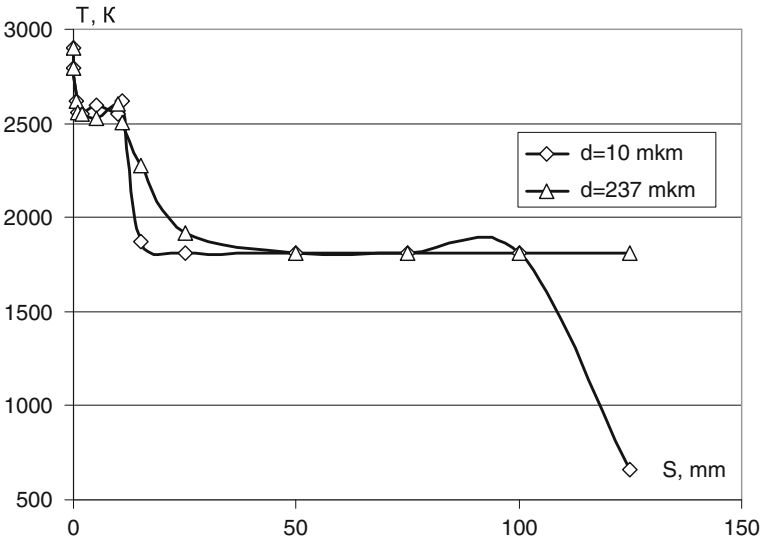


Fig. 2.36 Changing of the metal droplets temperature of different diameters along the spraying distance. The axis of the jet is considered

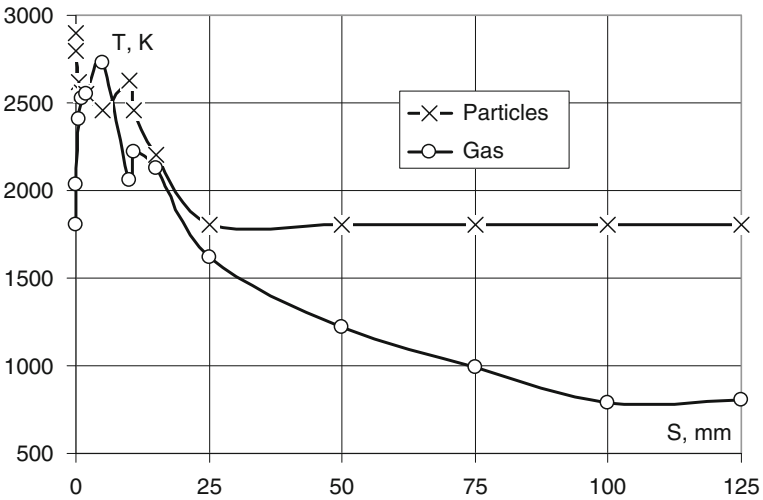


Fig. 2.37 The changing of the gas and metal temperatures along the spraying distance. The axis of the jet is considered. The diameter of droplets is 100 μm

Depending on the solidification degree, we can determine the droplet state. From the structure of the formula, it is clear that when $R_{ome} \leq 100 \%$ the droplet is in a solid-liquid state. If heat losses exceed the heat of the droplet melting point,

i.e., $R_{ome} \geq 100 \%$, the droplet is in the solid state, and its temperature begins to drop below T_{nr} . Calculation of the solidification degree for the characteristic droplet diameters are shown in Table 2.19.

The solidification degree at the jet axis for the majority of the fractions is $<20 \%$ and varies in inverse proportion to the diameter of the droplets (see Fig. 2.38). Although, only the fraction of $10 \mu\text{m}$ is solidified completely at a distance of 100 mm . At the jets boundary, the solidification degree is higher. Fraction drops of $10 \mu\text{m}$ solidify at the distance of $\sim 35 \text{ mm}$. The rest of the factions reach the solidification degree of $5\text{--}75 \%$, at a distance of 125 mm , Fig. 2.39.

Due to changes in the temperature and composition of the gas jet at the spraying distance is changed, the changes of thermophysical properties of the gas: density, viscosity, coefficient of diffusion, and others have occurred. On the axis of the jet

Table 2.19 Droplets solidification degree at the axis and the jet boundary

Droplets diameter	Cross-section point	s, mm				
		25	50	75	100	125
$d_p = 10 \mu\text{m}$	Axis	2.37	19.67	45.95	79.14	101.61
	Boundary	52.12	120.38	125.00	125.11	147.43
$d_p = 27.5 \mu\text{m}$	Axis	0.27	3.22	6.43	10.85	16.18
	Boundary	3.65	18.38	34.78	53.06	73.25
$d_p = 56.5 \mu\text{m}$	Axis	0.12	1.57	3.07	4.12	5.52
	Boundary	1.97	7.16	13.20	19.66	26.64
$d_p = 100 \mu\text{m}$	Axis	0.01	0.44	0.93	1.31	1.70
	Boundary	0.70	2.62	4.81	7.26	9.82

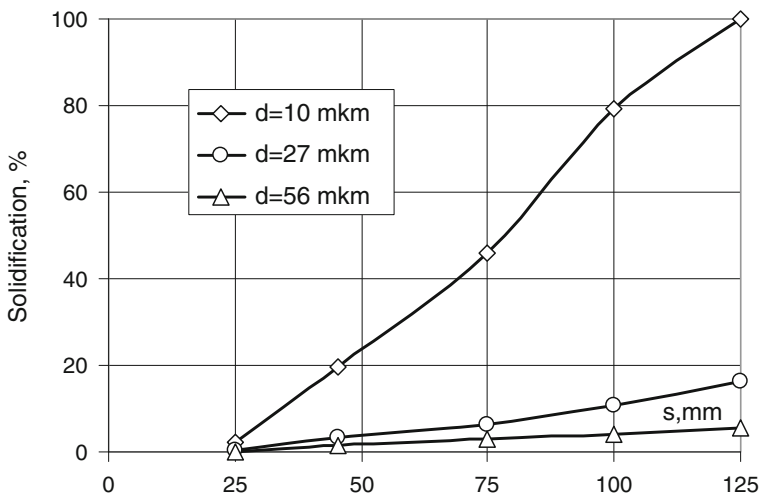


Fig. 2.38 The variation of the solidification degree of droplets of different diameters on the jet axis

Fig. 2.39 The variation of the solidification degree of droplets of different diameters on the jet border

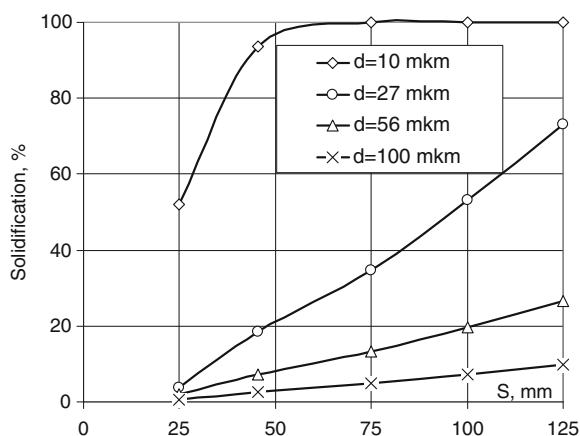
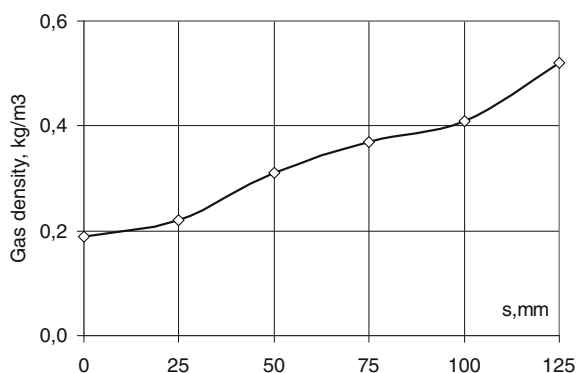


Fig. 2.40 Changing in the density of the gas on the jet axis



gas density increases by two times at a distance of 100 mm if compared to the nozzle face, Fig. 2.40.

The density of the gas is even higher along the cross-section of the jet. At the boundary of the jet, the gas density is 3–6 times higher than at the axis depending on the spraying distance, Fig. 2.41. This affects the angle of the velocity changing curves.

Changing of the number of characteristics is evaluated by the criterion dependences. The features of the flow are determined by the Reynolds number. For the case of external flowing around the flow is transferred to the turbulent regime at $Re_{\kappa} = 20\text{--}30$ [72].

According to calculations, the smaller diameter particles move in the laminar regime within the spraying distance on the jet axis. Particle having diameter of 45 μm and further up to the distance 50 mm move in the turbulent regime. Moreover, the turbulence is higher, the bigger the diameter is.

At the end of the initial section of the jet, where a sharp drop in the gas velocity begins, a turbulence peak for all diameters occurs. With the further increase of the

Fig. 2.41 Changing of the gas density at the cross-section of the jet at a different spraying distance

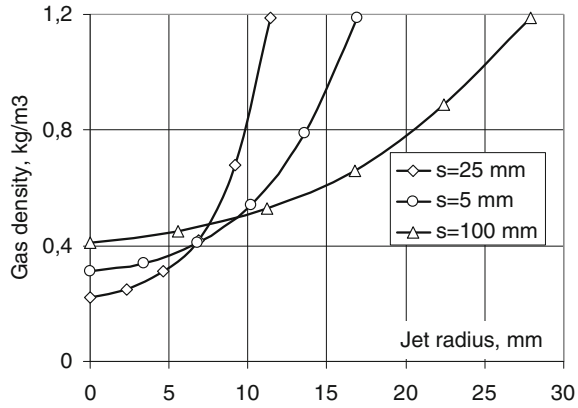
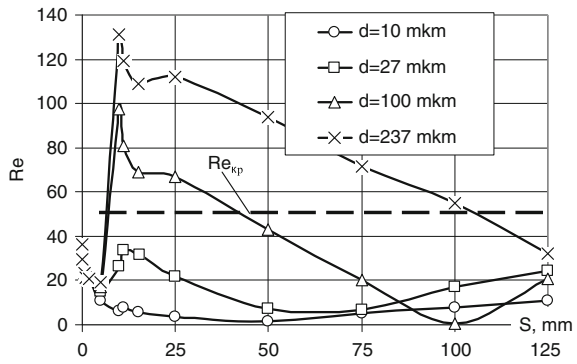


Fig. 2.42 Changing of the Reynolds number for the different diameters droplets at a spraying distance. The axis of the jet is considered



distance up to 125 mm on the jet axis, the flow regime of all the droplets is coming closer to laminar, Fig. 2.42. While distancing from the axis, the changing of Re has a complicated character that is determined by the value of the relative velocity of the droplets. Although, its value is greater than the critical $Re_k = 20-30$.

Nusselt number characterizing the heat and the mass transferring is related to the Reynolds number that is reflecting the dynamic parameters of the gas and to Prandtl number characterizing the physical properties of the gas.

Calculation shows that $Pr = 0.7-0.8$. This corresponds to the data of Abramovich, who is taking the value of $Pr_T = Pr_D = 0.75$ for axisymmetric turbulent jet [53]. Figure 2.43 shows that the curve of the Nusselt number repeats the changes Re . However, these changes are expressed less drastically. Note also that $8 > Nu > 2$ is true for all factions and along the whole distance. Hence, it shows the deviation from the ideal case of Langmuir (heat and mass transfer toward the ball in the absence of air flow, $Nu = 2$).

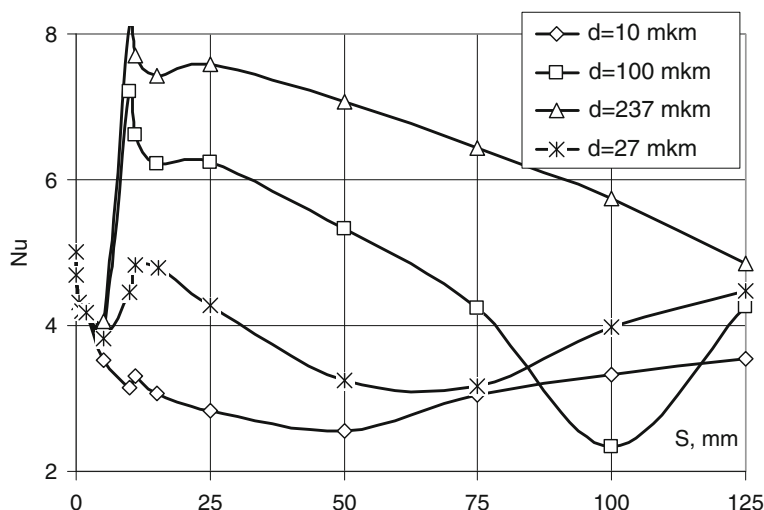


Fig. 2.43 Changing of the Nusselt number for the different diameters droplets at a spraying distance. The axis of the jet is considered

2.7 Chemical Composition of the Gas Jet

Despite the difference in the nature of high-temperature metallurgical processes and substances participating in gaseous atmosphere formation, the composition of the latter turns out to be of the same type. At temperatures below 2500 K, gas includes three groups of components: products resulting from the complete interaction with oxygen (CO_2 , $\text{H}_2\text{O}_{(\text{steam})}$, SO_3); product resulting from incomplete interaction with oxygen, from dissociation, from carbon elimination and gas removal from the metal (CO , H_2 , CH_4 , SO_2 , O_2 , N_2); inactive components (Ar and others.) [18]. The composition of such a gaseous atmosphere can be described on the basis of thermodynamic analysis of a few chemical reactions.

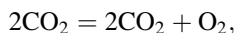
Arc spraying scheme use either compressed air or neutral and reducing gases as carrier medium. Compressed air spraying provides a stable atomization of the melting metal; nevertheless, the quality of the coating is not very high due to a large number of pores and oxides [57]. The use of noble gases for thermal spraying [70] is not technologically efficient because of their high price. Promising is the use of atmosphere “compressed air-carbohydrates”. That will bring additional heat when the mixture is burning; and will decrease the oxygen penetration in metal drops (particles) in case of reducing gas jet. Following this variant, the use of methane mixed with air is suggested [71]. VNIITUVID “Remdetal” suggests the use of kerosene as carbohydrates [19]. Baranovskiy suggests the use of products resulting from the combustion of propane–air mixture as the carrier gas [72]. However, these papers either completely miss any quantitative calculations [70, 71] or contain some approximate ones [19, 72]. It does not allow the realization of the advantages of the selected scheme fully.

Equipment and technology improving requires more comprehensive understanding of the delivery of gases and the nature of their interaction. When investigating gas and plasma jets used for coating application, there is usually introduced an assumption regarding the consistency of the gas jet composition [73–75]. Such calculations are characterized by a low precision. In order to increase their reliability, it is desirable to calculate the composition of the gas in each point of the jet.

The local composition of gases in each point of the jet at every moment of time can be taken as equilibrium one due to the high rate of chemical reactions. The legitimacy of this assumption has been experimentally approved for fast-running processes in metallurgy [76], rocket engine design [77]. In this case, the composition of the gas mixture is determined on the basis of the balance of chemical reaction components taking into account blending of atmospheric oxygen and nitrogen to the jet beyond the initial part.

The necessity of calculation of the equilibrium gas composition when spraying was pointed out by Tsvetkov [35], nevertheless, any ad hoc solution was not given. Karp with colleagues made such a calculation for the process of plasma spraying in their work using the minimum of thermodynamic potential with additional logarithm of the system energy equation [78]. As at temperatures 4000–12,000 K typical for plasma processes, high gas ionization is highly possible, these calculations take into account atom dissociation.

At arc spraying, according to the estimation (2.2), the dissociation is thermodynamically unlikely. In respect to arc spraying using combustible mixtures, the calculation of equilibrium composition of combustion gases of current mixtures “combustible gas-air” was given by Lialiakin [19]. However, it does not take into account the decomposition of CO_2 and H_2O . It does not match, for example, the thermodynamic analysis of carbon dioxide dissociation according to reaction



which states the dissociation degree of 6.6 % at temperature 2000 K [43].

Besides changing of the concentration of components resulting from chemical reactions, the composition of the jet changes due to continuous blending of air from the atmosphere. In order to estimate oxygen blending into the plasma jet, Gershenzon used the regression dependence [79] based on partial experimental data [80], which narrows the application area of the calculation results. The analytical account of atmospheric gases blending into the gas jet in papers considering process simulation in two-phase jet at arc spraying does not occur.

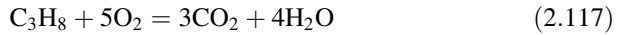
This is the law of mass action that is used to determine the gas composition at arc spraying. In contrast to similar calculations for plasma spraying [78], the equations of equilibrium constants are considered as the closure equations instead of the energy equation. Simplifying assumptions are to be taken:

1. Main products resulting from the combustion of propane–air mixture are CO , CO_2 , H_2 , H_2O , N_2 , O_2 . Atom dissociation of O, N, H is to be neglected due to not very high temperatures (<2300 K). The presence of carbohydrates, which

completely dissolve at temperature above 1000 °C [81], can also be neglected. Pohodnia analyzed the possibility of NO formation at welding [69]. According to different citations, at temperature 2500 K the equilibrium concentration of NO is 2–6 mol%. However, due to the high energy of activation of NO formation reaction (527 kJ/mole), the equilibrium concentration time (τ_{NO}) goes up sharply along with decrease in temperature from 2.2×10^{-3} s at $T_g = 2600$ K to 0.27 s at $T_g = 2100$ K. Gas residence time in the arc burning zone during arc spraying is an order of magnitude less and NO formation is unlikely.

2. The composition of gas at the nozzle exit is close to the equilibrium composition corresponding to the temperature at this point. This assumption is not absolute because combustion products also contain over-equilibrium quantities of H, OH, and other radicals due to the chain mechanism of combustion [76].

In the combustion chamber propane burns according to the summary reaction



If volume ratio of oxidant (air) and propane is L_B , then air is assumed to contain 79 % of nitrogen and 21 % of oxygen, and stoichiometric coefficients of the reaction (2.117) is equal 23.8. Combustion completeness is characterized by excess oxidant ratio:

$$\alpha_{ox} = V_O / V_g \cdot L_B^{st}, \quad (2.118)$$

where V_O —oxidant volume in mixture; V_g —combustible gas volume in mixture.

Oxidant (air) amounts required for propane combustion at different degrees of oxidation are given in Table 2.20. Where n —ratio of oxygen and propane number of moles. It is necessary to take into account that in practice, due to imperfection of mixers, the complete combustion is achieved when $\alpha_{ox} \geq 1.05$ [82].

To improve the quality by decrease of the degree of oxidation of particles when spraying, it is desirable to neutralize the action of air oxygen. Nevertheless, in case of considerable decrease of α_{ox} , a part of heat from combustion is used for heating of the excessive, according to stoichiometry, component. It leads to a decrease in temperature of combustion products. Besides the decrease in thermal contribution of the gas jet, it reduces the velocity of the jet passing through the supersonic nozzle because the sound velocity (a_{3B}) is related to the gas temperature by the dependence:

$$a_{3B} = \sqrt{k \cdot g \cdot R \cdot T_g} \quad (2.13)$$

Table 2.20 Relative air volume required for propane combustion at different degrees of oxidation

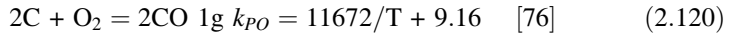
α_{ox}	0.6	0.7	0.8	0.9	1.0	1.05	1.1	1.2
n	3	3.5	4	4.5	5	5.25	5.5	6
L_B	14.28	16.66	19.04	21.42	23.8	24.99	26.18	28.76

The velocity of particles goes down, respectively, and the degree of their oxidation can increase due to a longer residence in the gas jet even despite lower oxygen content in it. The summary effect can be determined only as the result of detailed calculations.

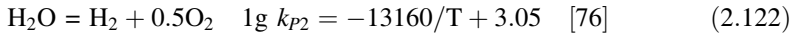
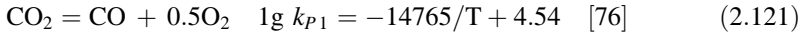
When the reducing mixture is supplied, the initial oxygen content is lower than according to stoichiometry (2.117) and at outlet from the nozzle there will be products of propane decomposition according to the reaction:



When there is free carbon, oxygen content in the gas phase will be determined by the equilibrium of the reaction:



When there is no carbon, it is sufficient to consider the equilibrium of two reactions only:



Let us consider, first, the method of solving of this relatively simpler problem. The reaction equilibrium constants (2.121), (2.122) can be noted as:

$$k_{P1} = \left(P_{\text{CO}} \cdot P_{\text{O}_2}^{0.5} \right) / P_{\text{CO}_2} \quad (2.123)$$

$$k_{P2} = \left(P_{\text{H}_2} \cdot P_{\text{O}_2}^{0.5} \right) / P_{\text{H}_2\text{O}} \quad (2.124)$$

The summary number of gas moles is equal to:

$$\sum n_i = n_{\text{CO}_2} + n_{\text{CO}} + n_{\text{O}_2} + n_{\text{H}_2\text{O}} + n_{\text{H}_2} + n_{\text{N}_2} \quad (2.125)$$

The material balance equations take the form of:

$$n_C'' = n_{\text{CO}_2}'' + n_{\text{CO}}'' = n_{\text{CO}_2} + n_{\text{CO}} \quad (2.126)$$

$$n_H'' = 2 \cdot n_{\text{H}_2\text{O}}'' + 2 \cdot n_{\text{H}_2}'' = 2 \cdot n_{\text{H}_2\text{O}} + 2 \cdot n_{\text{H}_2}, \quad (2.127)$$

$$\begin{aligned} n_O'' &= 2 \cdot n_{\text{O}_2}'' + 2 \cdot n_{\text{H}_2}'' = 2 \cdot n_{\text{H}_2\text{O}} + 2 \cdot n_{\text{H}_2}, \\ &= 2 \cdot n_{\text{O}_2} + 2 \cdot n_{\text{CO}_2} + n_{\text{CO}} + n_{\text{H}_2\text{O}}, \end{aligned} \quad (2.128)$$

$$n_{\text{N}_2}'' = n_{\text{N}_2}, \quad (2.129)$$

where $n_{\text{N}_2}^u, n_{\text{C}}^u, n_{\text{H}}^u, n_{\text{O}}^u$ are initial numbers of nitrogen moles, atomic carbon, hydrogen, and oxygen, respectively. From (2.125–2.129), we have

$$\sum n_i = n_{\text{C}}^u + 0.5 \cdot n_{\text{H}}^u + n_{\text{N}_2} + n_{\text{O}_2}, \quad (2.130)$$

$$n_{\text{O}}^u = 2 \cdot n_{\text{O}_2} + n_{\text{C}}^u + n_{\text{CO}_2} + n_{\text{H}_2\text{O}}, \quad (2.131)$$

The partial pressures of the components included in the Eqs. (2.123) and (2.124) are equal to:

$$P_{\text{CO}} = P \cdot n_{\text{CO}} / \sum n_i = P \cdot (n_{\text{C}}^u - n_{\text{CO}_2}) / \sum n_i = P \cdot (n_{\text{C}}^u - n_{\text{CO}_2}) / \sum n_i, \quad (2.132)$$

$$P_{\text{CO}_2} = P \cdot n_{\text{CO}_2} / \sum n_i, \quad (2.133)$$

$$P_{\text{O}_2} = P \cdot n_{\text{O}_2} / \sum n_i, \quad (2.134)$$

$$P_{\text{H}_2} = P \cdot n_{\text{H}_2} / \sum n_i = 0.5 \cdot P \cdot (n_{\text{H}}^u - n_{\text{H}_2\text{O}}) / \sum n_i, \quad (2.135)$$

$$P_{\text{H}_2\text{O}} = P \cdot n_{\text{H}_2\text{O}} / \sum n_i. \quad (2.136)$$

Inserting (2.123–2.136) into (2.123), (2.124) and eliminating the unknowns taking into account (2.125–2.131) we have:

$$n_{\text{CO}_2} = \frac{n_{\text{C}}^u \cdot B}{k_{p1} + B}; \quad n_{\text{H}_2\text{O}} = \frac{n_{\text{H}}^u \cdot B}{2 \cdot (k_{p2} + B)}; \quad n_{\text{O}_2} = \frac{B^2 \cdot A}{P - B^2} \quad (2.137)$$

$$-n_{\text{O}}^u + n_{\text{C}}^u + \frac{n_{\text{C}}^u \cdot B}{k_{p1} + B} + \frac{n_{\text{C}}^u \cdot B}{2 \cdot (k_{p2} + B)} + \frac{2 \cdot B^2 \cdot A}{P - B^2} = 0, \quad (2.138)$$

where

$$B^2 = P \cdot n_{\text{O}_2} / \sum n_i, \quad (2.139)$$

$$A = n_{\text{C}}^u + n_{\text{N}_2}^u + 0.5 \cdot n_{\text{H}}^u \quad (2.140)$$

Having solved the Eq. (2.138) for B , n_{O_2} can be found, then, from (2.125–2.131), mole fractions of the other gas mixture components and the mixture composition can be determined consequently. The Eq. (2.138) is a polynomial of degree four, which is solved by the method of successive approximations with magnitude B constraints resulting from the physical content of the Eq. (2.139): $B \leq P_{\text{O}_2}^{0.5}$.

Calculations beyond the initial part of the jet, take into account the blending of atmospheric oxygen and nitrogen into the jet. Fields of dimensionless concentrations and temperature by the Eqs. (2.102 and 2.103) are proportional to the curves of dimensionless values of velocity [53]:

$$\gamma_x = (C_x - C_s)/(C_0 - C_s) = 0.86 \cdot W_{gx}/W_{go}, \quad (2.141)$$

$$\gamma_y = (C_y - C_s)/(C_x - C_s) = (W_{gy}/W_{gx})^{Pr_D}, \quad (2.142)$$

where $C = G_g/(G_g + G_{air})$ —the additives concentration, which is the ratio of additives weight to the total weight of the mixture in the same volume in an arbitrary point of the jet per unit time. In particular, for the case of the gas flow through the nozzle into pure immobile air; $Pr_D = \nu/D$ is the Prandtl diffusion number characterizing the physical properties of the gas; D is the coefficient of eddy diffusion of the gas.

If we denote the quantity of blended air moles by (z) and the quantity of gas moles by $(\sum n_i)$ we can express dimensionless values of velocities as follows:

$$\gamma = \sum n_i / \left(\sum n_i + z \right), \quad (2.143)$$

where $\gamma = \gamma_x \cdot \gamma_y$.

One can see from the Eq. (2.143) that:

$$z = \sum n_i \cdot (1 - \gamma)/\gamma \quad (2.144)$$

Consequently:

$n_{N_2, \text{blended}} = 0.79 \cdot z$ is the quantity of blended nitrogen moles

$n_{O_2, \text{blended}} = 0.21 \cdot z$ is the quantity of blended oxygen moles

If we divide the jet into intervals Δx and each jet section—into intervals Δy , inside which the quantity of blended air moles can be considered as constant, we have the following equations for any section Δx and Δy :

$$n_{N_2}^j = n_{N_2}^{j-1} + 0.79 \cdot \sum n_i^{j-1} \cdot (1 - \gamma)/\gamma \quad (2.145)$$

$$n_{O_2}^j = n_{O_2}^{j-1} + 0.21 \cdot \sum n_i^{j-1} \cdot (1 - \gamma)/\gamma \quad (2.146)$$

$$n_C^j = n_C^{j-1} \quad (2.147)$$

$$n_H^j = n_H^{j-1} \quad (2.148)$$

Let us consider the case when the coefficient of oxidant excess $\alpha_{ox} < 1$, that is, the atmosphere is reducing. As noted above, when propane is completely

decomposed, the gas will contain elemental carbon, for the reason it is necessary to consider the equilibrium of the reaction (2.120). Besides that, the balance equation of carbon in the gas (2.126) will change as follows:

$$n_C^u = n_{CO_2} + n_{CO} + n_C \quad (2.149)$$

where n_C —is the quantity of elemental carbon moles.

It complicates the calculation because it requires establishing of a new more complex system of equations. However, specific conditions of the problem under solution allow the use of a simpler method. The calculation starts by the maximum values $\alpha_{ox} > 1$, when the gas is a priori free from carbon. The calculation continues by steady decrease in α_{ox} with checking of carbon occurrence conditions in each cycle, which results from the equilibrium of reaction (2.120):

$$P_{CO}^2/P_{O_2} \geq k_{P0} \quad (2.150)$$

when free carbon occurs, due to large values of equilibrium constants of its interaction with O_2 , H_2O , CO_2 (10^5 – 10^8 , according to [52]), the gas in equilibrium will contain almost only H_2 , CO , and N_2 . In this case:

$$n_{CO} = n_O^u, n_{N_2} = n_{N_2}^u, \quad n_{H_2} = 4 \cdot n_{C_3H_8}^u \quad (2.151)$$

Consequently

$$\sum n_i = n_O^u + n_{N_2}^u + 4 \cdot n_{C_3H_8}^u \quad (2.152)$$

and

$$P_{CO} = P \cdot n_O^u / \sum n_i; \quad P_{H_2} = P \cdot 4 \cdot n_{C_3H_8}^u / \sum n_i \quad (2.153)$$

Partial pressures of H_2O and CO_2 are found from the Eqs. (2.123) and (2.124).

If necessary, the calculation can be repeated until the required precision of values n_{H_2} and n_{CO} is achieved by the Eqs. (2.126 and 2.127) calculating the equilibrium constants by the Eqs. (2.120–2.122). The initial quantity of moles of components in terms of reactions of propane combustion and decomposition is equal to:

$$n_i^u = G_g^i \cdot 10^3 / M_i, \text{ mol/h} \quad (2.154)$$

where G_g^i , M_i is the consumption and the molecular weight of the i —component.

For specific components of the gas mixture the temperature dependences of components properties were obtained by the regression analysis of the reference data (Table 2.21).

In order to account the influence of components on the properties of the gas mixture there are used different methods. Thus, while determining the heating of

Table 2.21 Temperature Dependences of the thermophysical properties of mixture components, $T = 298\text{--}2500\text{ K}$, $P = 0.1\text{ MPa}$ [9, 62, 83]

	CO	CO ₂	H ₂	H ₂ O	N ₂	O ₂
Heat capacity $c_p = A + B_1 \cdot T + B_2 \cdot T^2 + B_3 \cdot T^3 + B_4 \cdot T^4$, kJ/(kg K)						
A	0.309727	0.401553	17.364959	0.370223	0.985015	0.800472
B ₁	0.001003	1.639687	−0.006933	5.513101	0.000142	0.000416
B ₂	0	−0.595574	0.000004	−5.250974	0	0
B ₃	0	−0.502355	0	1.690904	0	0
B ₄	0	0.288831	0	0	0	0
Dynamic viscosity $\eta = A + B_1 \cdot T + B_2 \cdot T^2$, kg/(m s)						
A	53.470470	90.821075	32.467186	−30.86506	74.161049	83.962339
B ₁	0.452274	0.345442	0.204200	0.407145	0.388818	0.470983
B ₂	−0.000110	−0.000042	−0.000035	0	−0.000066	−0.000072
Thermal conductivity, $\lambda = A + B_1 \cdot T + B_2 \cdot T^2$, W/(m K)						
A	5.0661668	−10.05457	91.495468	15.147091	11.160771	3.392105
B ₁	0.070140	0.092531	0.325269	0.001003	0.058519	0.077837
B ₂	−0.000009	−0.000014	0.000034	0.000084	−0.000003	−0.000010
Density, $\rho = A/T$, kg/m ³						
A	347	530	24.1	216	348	376

particles in the plasma jet, first calculated is nitrogen as plasma-forming gas, then the results are transferred on other gases [75]. In other cases, there are used average parameters and additive dependences are taken for them proportionally to mole fractions of components [73, 84]:

$$g_m = \sum g_i \cdot x_i \quad (2.155)$$

where g_m is a mixture property; g_i , x_i are a property and mole fraction of component, respectively.

This is correct for calculations of density and heat capacity of gas mixtures. Nevertheless, the additive determination of kinetic characteristics of gas mixtures—thermal conductivity, viscosity, diffusion coefficient—gives significant inaccuracy due to the occurrence of the potential interaction between molecules besides the kinetic one. Regarding the gas mixture at plasma spraying, Strongin accounts the thermal conductivity depending on the interaction potential, at the same time, he takes the viscosity of the gas mixture as a constant value, which does not depend on the temperature [85].

To account the influence of intermolecular forces, the interaction integral Ω is calculated according to the value of the potential interaction energy $\Psi(r)$. The value Ω is equal to 1 if the intermolecular attraction does not exist. $\Psi(r)$ represents the potential molecule interaction energy at the distance r from each other. The ratio of Ψ and r is expressed as the interaction potential. The most often used interaction potential is Lennard–Jones potential

$$\Psi(r) = 4 \cdot \varepsilon \cdot \left[(\sigma/r)^{12} - (\sigma/r)^6 \right], \quad (2.156)$$

where σ is the collision diameter, the distance between molecules when $\Psi(r) = 0$; ε is the characteristic energy corresponding to the minimum of the interaction curve.

The available data regarding the mutual influence of gas components are systematized in the monograph [86]. They allow the calculation of kinetic properties of a gas mixture.

The mixture viscosity is determined according to the Chapman-Enskog kinetic theory through the equation:

$$\eta = 5/16 \cdot (\pi \cdot M \cdot R \cdot T)^{0.5} / ((\pi \cdot \sigma^2) \cdot \Omega) \quad (2.157)$$

The computational solution of this equation is approximated by the series

$$\eta_{\text{CM}} = \sum_{i=1}^n x_i \cdot \eta_i / \sum_{i=1}^n (x_j \cdot \Phi_{ij}), \quad (2.158)$$

where Φ -parameter is determined by Vilke approximation:

$$\Phi_{ij} = \left[1 + (\eta_i/\eta_j)^{0.5} \cdot (M_j/M_i)^{0.25} \right]^2 / \left[8 \cdot (1 + M_i/M_j) \right]^{0.5}, \quad (2.159)$$

$$\Phi_{ji} = \eta_j \cdot M_i \cdot \Phi_{ij} / (\eta_i \cdot M_j) \quad (2.160)$$

For example, for the binary system the Eqs. (2.158–2.160) will be as follows:

$$\begin{aligned} \eta_{\text{CM}} &= x_1 \cdot \eta_1 / (x_1 + x_2 \cdot \Phi_{12}) + x_2 \cdot \eta_2 / (x_2 + x_1 \cdot \Phi_{21}), \\ \Phi_{12} &= \left[1 + (\eta_1/\eta_2)^{0.5} \cdot (M_2/M_1)^{0.25} \right]^2 / \left[8 \cdot (1 + M_1/M_2) \right]^{0.5}, \\ \Phi_{21} &= \eta_2 \cdot M_1 \cdot \Phi / (\eta_1 \cdot M_2) \end{aligned}$$

According to Vilke, while making calculations according to the Eqs. (2.158–2.160), the average deviation of η_{sm} from experimental data is <1 %.

The thermal conductivity of the mixture is not usually the linear function of the composition as well. If the molecules of components have very different polarities, the thermal conductivity of the mixture is higher than the values obtained following the rule of additivity. For non-polar molecules, the trend is converse. Similar to the theoretical ratio for the viscosity, the thermal conductivity of the gas mixture is expressed in the form of the equation of Vasilieva:

$$\lambda_{\text{CM}} = \sum_{i=1}^n x_i \cdot \lambda_i \Big/ \sum_{j=1}^n (x_j \cdot A_{ij}) \quad (2.161)$$

For A-parameter, there is used the Lindsay-Bromley modification obtained on the basis of the Sutherland gas model

$$A_{ij} = \frac{1}{4} \cdot \left\{ 1 + \left[\frac{\eta_i}{\eta_j} \cdot \left(\frac{M_j}{M_i} \right)^{3/4} \cdot \frac{T + S_i}{T + S_j} \right]^{1/2} \right\} \cdot \frac{T + S_{ij}}{T + S_i}, \quad (2.162)$$

$$A_{ji} = \frac{1}{4} \cdot \left\{ 1 + \left[\frac{\eta_j}{\eta_i} \cdot \left(\frac{M_i}{M_j} \right)^{3/4} \cdot \frac{T + S_j}{T + S_i} \right]^{1/2} \right\} \cdot \frac{T + S_{ji}}{T + S_j}, \quad (2.163)$$

where S_i , S_j are the Sutherland constants found by the concordance of the Sutherland equation with experimental data, dependence $S_i = 1.5T_{\text{boil}}$ (except He, H_2 , Ne, for which $S_i = 79 \text{ K}$) is taken empirically; $S_{ij} = 0.73 \cdot (S_i \cdot S_j)^{0.5}$ is the Sutherland interaction constant.

The values of the boiling point (T_{boil}) are given in Table 2.22 [9]. An average calculation inaccuracy in the Eq. (2.161) is 1.9 %.

The component diffusion coefficients can be calculated like for a binary mixture if one of the components is taken as the main one. To calculate the diffusion coefficient for the binary mixture “A–B” the following equation is used:

$$D_{AB} = 1,858 \cdot 10^{-7} \cdot T_g^{1.5} \cdot [(M_A + M_B)/(M_A \cdot M_B)]^{0.5} \Big/ (P \cdot \sigma_{AB}^2 \cdot \Omega_D), \quad (2.164)$$

where

$$\sigma_{AB} = (\sigma_A + \sigma_B)/2 \quad (2.165)$$

Table 2.22 Auxiliary parameters for calculation of the thermal conductivity and diffusion coefficient in the gas mixture (carbohydrate-air)

	Mixture components						
	CO	CO ₂	H ₂	H ₂ O	N ₂	O ₂	
T_{boil} , K	81.50	194.65	20.40	637.00	77.35	90.18	
σ , Å	3.690	3.941	2.827	2.641	3.798	3.467	
$\varepsilon_{\text{AB}}/k$, K	91.7	195.2	59.7	809.1	71.4	106.7	
Equation (2.166) coefficients							
A_1	A_2	A_3	A_4	A_5	A_6	A_7	A_8
1.06036	0.15610	0.19300	0.47635	1.03587	1.52996	1.76474	3.89411

is the characteristic distance,

$$\Omega_D = \frac{A_1}{T^{*A_2}} + \frac{A_3}{\exp A_4 \cdot T^*} + \frac{A_5}{\exp A_6 \cdot T^*} + \frac{A_7}{\exp A_8 \cdot T^*} \quad (2.166)$$

is the collision integral for the diffusion,

$T^* = k \cdot T_g / \varepsilon_{AB}$ is the dimensionless temperature,

$$\varepsilon_{AB} = (\varepsilon_A \cdot \varepsilon_B)^{0.5}$$

The values of auxiliary parameters are given in Table 2.22. The calculation inaccuracy in the Eq. (2.164) is 7–12 %.

Based on data found in the scientific literature, the density, heat capacity of gas mixtures were determined proportionally to the mole fraction of components according to the additive dependences. The kinetic characteristics of the gas mixture—thermal conductivity, viscosity, diffusion coefficient—were found taking into account the mutual influence of the components according to the additive dependences, Eqs. (2.158, 2.161 and 2.164).

The calculation results were analyzed for the jet core, where the influence of the initial content of carrier gas is maximal. Figure 2.44 shows the change in the content of oxygen-containing components of the gas along the distance when the jet is initially reducing. It is seen that due to intense atmospheric air blending beyond the initial part ($x > 25$ mm), the jet has almost the same composition as the air. When the initial gas compositions are different, oxidizing or reducing, sharp

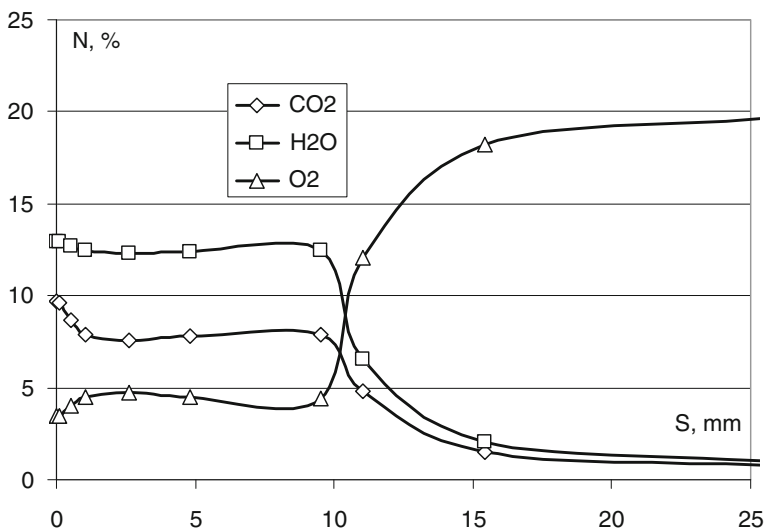


Fig. 2.44 Oxidant content in gas. Jet Core. $\alpha_{ox} = 0.8$

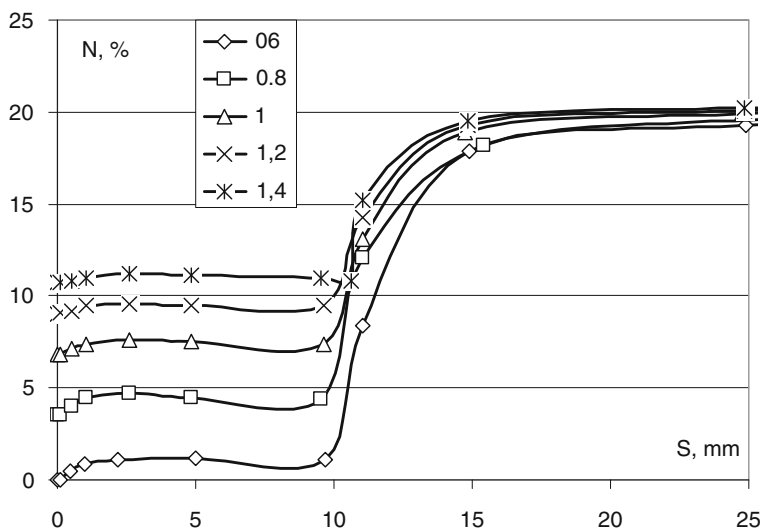


Fig. 2.45 Oxygen content in gas with different initial coefficients of oxidant excess. Jet Core

differences in oxidant content from the surrounding atmosphere are only in the initial part of the jet. The behavior of one of the oxidants—oxygen—is shown in Fig. 2.45. The calculation does not show any significant difference of jet composition in the rest of the distance.

2.8 Kinetic of Interaction of the Sprayed Metal with Oxygen

The quality of coatings obtained by arc spraying depends, in particular, on the quantity of oxygen solved in the metal of particles and contained in oxides. The process of oxygen delivery to the metal drops is important from the point of view of the final properties of coating. The principals of this process allow the prediction of properties of the coatings obtained depending on the modes of spraying or, by solving the converse problem, the selection of wire composition in order to obtain a coating of the required composition.

2.8.1 Physical Pattern of the Process

Based on the idea of the character of the interaction between melted metal and gas at arc spraying, as well as taking into account experimental data about slag behavior

on metal drops within the gas jet, the physical pattern of oxygen delivery to the sprayed metal can be presented as follows.

First, all oxygen delivered is solved in the liquid metal until the saturation point. Then, slag forms on “metal-gas” interface. This process involves not only the oxygen coming from the gas phase but also the oxygen discharged by the super-saturated solution as the result of metal cooling from the overheat temperature, 2500–2900 K at the end face of the electrode and in the arc burning zone, to the melting point when drops are flying.

The reaction surface at the end face of the electrode is renewed in cycles due to the removal of liquid metal film with the frequency of drop discharge.

After film removal, the liquid metal surface opens. Oxygen content in it is equal to the initial solid metal, which is significantly below the saturation point at the temperature on the end face.

Experimental data show that the surface of flying drops is irregularly covered with slag.

In the paper of Borisova and colleagues, there are microphotos of particles obtained during the arc spraying of wire 65Г (like steel ASTM 1566) [46]. It is noted that all particles, as a rule, are covered with a layer of iron oxides, which can crack or partially pitted. There occur particles representing a complete oxide casing submitted to a partial destruction, Fig. 2.46.

In case of crucibleless melting of metal in high-frequency magnetic field, Yavorskiy and his colleagues described their observation of metal drops formed as the result of melting of specimens in the inductor [87]. The drops were blown with inert gas flow. Then, they added oxygen to the gas blowing a drop during a certain period of time. The observation showed that when carbon content is $C < 1$ wt%, there are formed particles of oxide phase on the drop surface, which are visible due to their different, compared to the metal, color, and brightness. The slag film does not cover the entire metal surface but floats on it in the form of tiny islands, which flow to the lower part of the specimen, where they gather into a larger structure, which breaks away from the metal drop. It was often observed that the metal stirred by electromagnetic field caught these slag islands, and their reverse descending on the surface.

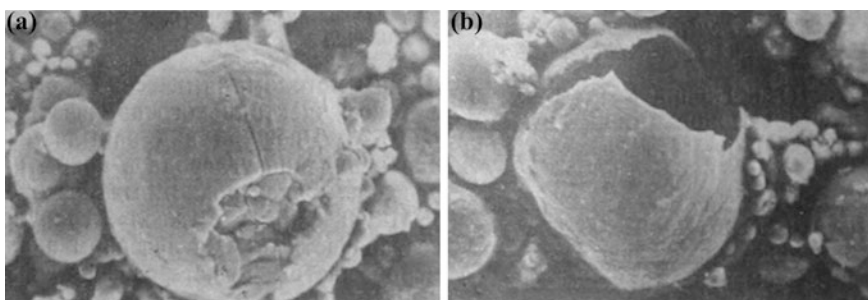


Fig. 2.46 View of particle of steel 65Г caught from spraying jet. **a** Cracking and partial pitting of the oxide casing on a particle, x550; **b** partially destroyed empty oxide casing, x600

Our investigations of the structure and properties of coatings have shown the presence of individual layers in the coating consisting of iron oxides [88].

In order to give a quantitative description of processes taking place, the idea of the continuous change of the area covered with slag is to be replaced by a model, according to which the spherical particle is filmed with slag. The area of the slag film is constant or changes under a certain law. The slag film is pushed aside under the influence of the gas flow. According to the calculations made before, at the most part of the spraying distance the gas velocity is higher than the velocity of the drops that is why the rear side of the drop surface is mainly clear of the slag film. The value of the surface share clear of slag can be assessed by the parameter (L):

$$L = f_s/f_d, \quad (2.167)$$

where f_s , f_d are slag covered area and total area of the drop surface, respectively.

It is most likely that the value of the area clear of slag is continuously changing due to changes in drop velocity relative to the gas and existence of drop rotation because of the turbulence. The calculations assumed a conventional scheme, according to which the share of the surface clear of slag remains the same during all the life time of a drop. Just opposing the calculations with the measures of oxygen content in the coating—the end point of drop flight—it is possible to take an average value L .

It is found that nitrogen in the presence of an arc discharge can pass into liquid metal in quantities several times larger than its equilibrium solubility under the similar conditions in the absence of an arc discharge [89]. However, the formation of supersaturated solution is less likely for oxygen. More likely, when the temperature is going down, oxygen will discharge from the solution and interact with iron forming FeO.

Thus, there are two types of reaction surfaces on a drop, which show different behavior while interacting with oxygen:

- Surface clear of slag. Oxygen from the gas phase comes on this surface and interacts with liquid metal forming slag. The latter, under the influence of the gas flow, is pushed aside from this surface opening the metal surface again for the interaction with the gas phase.
- Surface filmed with slag. Interdependent reactions between the components of the liquid metal and slag take place on the interphase interface “metal-slag”.

The total quantity of oxygen (m_O) having come into the drop of the i —fraction will be equal to the sum of oxygen solved in the liquid metal and contained in the slag:

$$m_O = m_{Me} \frac{[O]}{100} + m_w \left(\sum_i \frac{(\Xi_m O_n)_i \cdot M_O}{100 \cdot M_{(\Xi_m O_n)}} \right), \quad (2.168)$$

The degree of oxidation of particles in this fraction r_i will be:

$$r_i = (m_O \cdot 100) / (m_{Me} + m_w) \quad (2.169)$$

The general degree of oxidation R is summed according to the distribution of drops by the weight fraction:

$$R = \left(\sum (r_i \cdot dm_i) \right) \cdot 100, \quad (2.170)$$

where for the drops of the i —fraction: m_{Me} is the weight of the liquid metal, kg; m_w is the weight of slag, kg; dm_i is the weight fraction of a component; M is the molecular weight of a component, kg/mole; $(\sum_m O_n)_i$ are concentrations of the i —component of slag, wt%.

2.8.2 Principles of Thermodynamic and Kinetic Calculations of Oxygen Delivery

Let us consider the features of oxygen delivery to the clear and slag-covered surfaces.

2.8.2.1 Free of Slag Surface

Macrokinetics divides the conditions of reactions into three areas: kinetic, diffusion, and transition ones.

A reaction takes place in the kinetic area if it is limited by the rate of the chemical transformation itself. In this case, the rate of the chemical transformation is proportional to the concentration of the reacting substances according to the law of mass action:

$$w = kc_1^{v_1} c_2^{v_2}, \quad (2.171)$$

where k is the constant of the reaction rate depending on the temperature in accordance with the Arrhenius equation; v_1, v_2 are stoichiometric coefficients;

$$k = Ze^{-\frac{E}{RT}}, \quad (2.172)$$

Z is the factor, which reflects the probability of the transformation; T is the temperature; R is the gas constant; E is the activation energy (the level of energy when a molecule or another particle becomes reactive).

Along with the temperature growth, the rate of the chemical transformation goes up quickly, whereas the velocity of delivery and taking off chemical agents in liquid

media changes much more slowly. Consequently, under high-temperature conditions inherent to arc welding processes, gas passes into metal in the diffusion area; that is, the rate of the reaction between metal and gas is limited by transport agent (molecular or convection diffusion) [4, 69].

Let us introduce the basic concepts of the mass transfer—processes of chemical agent transfer to the phase interface (reaction zone) and reaction product take off from this interface [4, 56, 90].

The specific diffusion flow j , $\text{kg}/(\text{m}^2 \text{ s})$, which express the velocity of mass transfer, is equal to the amount of the substance transferred through a unit of surface per unit time:

$$j = \beta(c_{o\delta} - c_{no\delta}), \quad (2.173)$$

where β is the coefficient of mass transfer, m/s ; $c_{o\delta}$, $c_{no\delta}$ are the concentrations of a substance in the liquid or gas volume and on the phase interface, respectively, kg/m^3 .

If a heterogeneous reaction takes place in the diffusion area, there should be supported an equilibrium on the phase interface: $c_{no\delta} = s_p$. In this case the Eq. (2.173) can be written as:

$$j = \beta(c_{o\delta} - c_{no\delta}) = \beta\Delta c \quad (2.174)$$

In physical–chemical hydrodynamics, it is assumed that a substance in the liquid or gas volume is distributed uniformly in consequence of convective stirring and all changes in concentration related to its transfer take place in the thin interface, where the main role is played by molecular diffusion. The thickness of this layer δ is conventionally represented as the diffusion path. Then, in compliance with the first Fick's law, when the linear change in interface is admitted, the diffusion flow is equal to:

$$j = D \cdot \Delta c / \delta \quad (2.175)$$

Comparison of the Eqs. (2.174) and (2.175) gives:

$$\beta = D / \delta \quad (2.176)$$

The transfer of the substance in the interface is also influenced by convection diffusion, thus, δ must be considered as a nominal value determined by the relation (2.176).

At different spraying process stages, the relation of values of the diffusion flows in gas and metal can change. So, in Gershenzon's paper [79] concerning plasma spraying, the delivery of oxygen into drops following the mechanism of external diffusion was explained without taking into account the kinetics of interaction in the system “metal-slag”. According to the calculation made in this paper, the amount of oxygen delivered to a particle will be:

$$m_O = \int_0^{\tau} j \cdot F \cdot d\tau = \pi \cdot d^2 \int_0^l \frac{j}{V_p} dl, \quad (2.177)$$

where l —spraying distance, m; d , F , V_p —diameter, m, surface area, m^2 , and velocity of a particle, m/s; j —specific diffusion flow, $kg/(m^2 \text{ s})$.

Taking that the oxygen concentration in a particle is equal to 0, it was found:

$$j = \beta_g \cdot c_O = Nu \cdot D \cdot c_O / d, \quad (2.178)$$

where β_g —coefficient of mass transfer in gas, m/s; c_O —concentration of oxygen in gas, kg/m^3 ; D —coefficient of diffusion in gas, m^2/s .

Considering the diffusion in gas only without considering the diffusion in metal like it was done by Gershenzon leads to the fact that the calculated degree of drop oxidation is insensitive to the chemical composition of the starting materials. This differs from the data obtained by Novozhilov for the similar, as far as the temperature level is concerned, welding process. His experiments showed that the amount of oxygen having reacted with metal during welding of austenitic steel 12X18H9T (like steel ASTM 302) was about two times larger than the amount of oxygen having reacted during the welding of pearlitic steels 30ХГСА, Ст.3, and Ст.3кп (like steels 30CrMnSiA and A57036, correspondingly) [91]. Besides that, Gershenzon did not take into account the eventual oxygen saturation of the liquid metal.

Correspondingly, the following slag formation and the kinetics of slag interaction with metal were not taken into account as well. This is in contradiction with the results of the experimental studies. Besides that, according to the data obtained by vacuum melting, the oxygen content in the coating in case of arc spraying is 2–6 % [88, 92], which is an order of magnitude higher than the oxygen saturation limit in liquid iron at the melting point, $[O]_{Fe}^{sat} \approx 0.17 \%$. This also confirms the presence of slag in the coating and, consequently, in drops forming the coating.

To choose the limiting stage for arc spraying, let us estimate possible limiting diffusion flows in gas J_g^{limit} and in metal J_{Me}^{limit} :

$$J_g^n = \beta_g \cdot c_g = \beta_g \cdot P_{O_2} / (2 \cdot R \cdot T), \quad (2.179)$$

$$J_{Me}^{npe\partial} = \beta_{Me} \cdot c_{Me} = \beta_{Me} \cdot \rho_{Me} \cdot [O]_{Fe}^{Hac} / (100 \cdot M_O) \quad (2.180)$$

where β_g , β_{Me} —coefficients of mass transfer in gas and metal, m/s; s —oxygen concentration, wt%; P_{O_2} —partial pressure of oxygen, MPa; T —temperature, K.

The results of calculations of limiting diffusion flows for different process stages are given in Table 2.23. β_{Me} value for the arc zone is taken according to the estimation [4, 89], for the flying stage—like for the melt at plasma-hydrogen deoxidation [89].

Table 2.23 Estimation of the limiting diffusion flows of oxygen in gas and metal

Stages	1. End face of electrode	2. Arc burning zone	3. Distance, mm				
			10	25	50	75	100
T_g, K	2400	2900	2102	1266	968	784	712
$P_{O_2} \times 10^{-5}, Pa$	0.0485	0.0713	0.21	0.21	0.21	0.21	0.21
$V_z, m/s$	1127	1127	889	571	351	254	199
$V_p, m/s$	0.045	7.63	69	92	105	111	114
Re	238	19.5	273	269	221	184	127
Nu	10.3	4.0	11.1	10.9	10.2	9.2	8.1
$D \times 10^4, m^2/s$	6.6	9.1	3.6	2.1	1.3	0.9	0.7
$\nu \times 10^4, m^2/s$	4.8	5.7	2.7	1.6	1.0	0.7	0.6
$\beta_{Me} \times 10^2, m/s$	0.25	0.25	0.025	0.025	0.025	0.025	0.025
$\beta_g \times 10^2, m/s$	6844	36298	4224	2562	1469	924	673
$I_g^n, mole/(m^2 \cdot s)$	0.13	55	26	26	21	15	11
$I_{Me}^n, mole/(m^2 \cdot s)$	14	41	0.2	0.2	0.2	0.2	0.2

The coefficient of mass transfer in gas was estimated according to Nusselt criterion, which was determined in compliance with criterial dependences according to [35]:

$$\beta_g = Nu \cdot D/d, \quad (2.181)$$

$$Nu = 2 + 0.459 \cdot Re^{0.55} \cdot Pr^{0.75}, \quad Re = W \cdot d/\nu, \quad Pr = \nu/D \quad (2.182)$$

where d —characteristic size, m.

Diffusion and viscosity coefficient values in gas (D , ν) were calculated taking into account the mutual influence of the mixture components. The corresponding gas composition at the nozzle exit was calculated for the case of metal spraying by products of propane–air mixture combustion, $\alpha_{ox} = 0.8$. As d value there were taken: the gap between electrodes—for the end face of the electrode, the thickness of the “tongue”—for the burning zone, drop diameter—for the flying stage. According to the estimation of the gap size between the electrodes made in the paper [22], we take $d = 0.1$ – 1 mm for the end face of the electrode. The thickness of the tongue is taken as the smallest drop diameter of the sprayed metal, $d = 10 \times 10^{-6}$ m, according to the data obtained from the sieve test. As for the flying stage, the most likely diameter of sprayed particles $d = 90 \times 10^{-6}$ m is taken.

When comparing the calculated values of the limiting diffusion flows, it is seen that at the end face of the electrode we deal with the inner problem of the mass transfer and for stages 2 and 3 the outer problem takes place. The value of the mass transfer coefficient in gas is more than an order of magnitude higher than at welding or plasma-arc melting [89]. It can be explained by small size of drops transferred and high relative velocity of the drops and gas compared to the above mentioned

processes. The order of magnitude β_g at the distance 25–125 mm corresponds to the values of this parameter for plasma-arc melting given by Lakomskiy [89]. β_g is several times higher at the starting segment of the jet, where temperatures are higher and drops are smaller. If taken are drops from the side fractions, their diameters are 10 and 237 μm , in this case the mass transfer in gas is about two times different along the distance (Fig. 2.47).

The calculations given show that at the end face and in the arc burning zone, at stages 1 and 2, the limiting segment of the oxidation process is the external diffusion and the diffusion in metal along the spraying distance.

It can be noted the specific character of the drop transfer in arc spraying in comparison with the data at arc welding given by Pokhodnya at the same values of current and electrode diameter [69]. The specific area of drops in arc spraying is 1–2 orders higher, Fig. 2.48. On the other hand, the time of liquid metal drop interaction with air is 100–1000 times shorter. It is evident that there is no direct similarity with the welding process according to these parameters. For the purposes of the welding process estimation Pokhodnya introduced the generalized kinetic index of interaction, which is the characteristic of transfer under electrode melting:

$$\omega = F_k t_k / V_k, \quad (2.183)$$

where F_d , V_d —drop surface area, m^2 , and volume, m^3 ; $\tau_k = m_{av}/v_{av}$ —mass-averaged time of interaction, s; m_{av} —drop mass, kg; v_{av} —average melting velocity, kg/s.

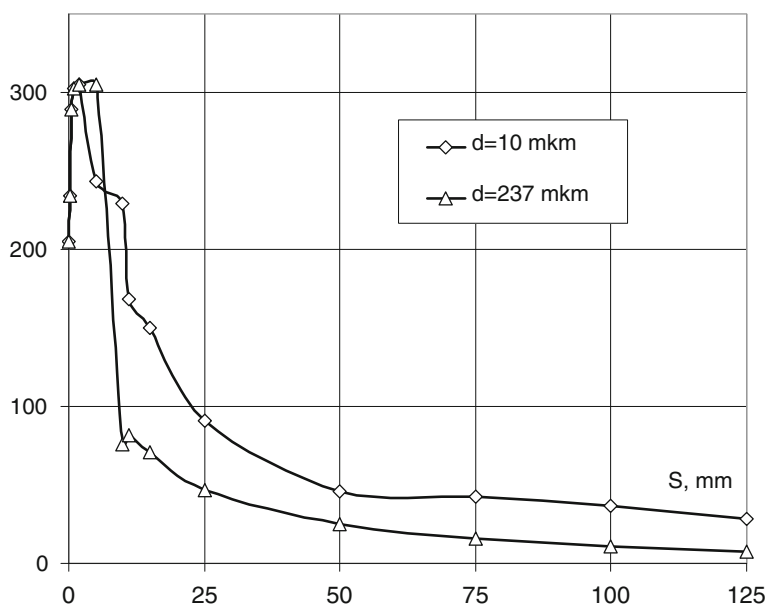
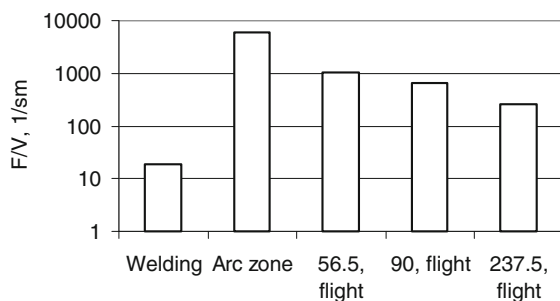


Fig. 2.47 Change of mass transfer coefficient in gas for drops with different diameters along the distance. Jet axis

Fig. 2.48 Comparison of the specific area of drops at welding and arc spraying



When ω values are compared for welding and arc spraying (Fig. 2.49), it is seen that the intensity of the mass transfer at arc spraying is about 30 % higher for the arc burning zone.

The possibility of mass transfer in flight goes down sharply for the drops belonging to the main range of diameters and becomes an order of magnitude lower than for welding in drop transfer. Despite the sharp difference of individual indices of interaction, the generalizing index for both welding and arc spraying has the same order of values. This justifies the application of welding parameters of transfer in arc spraying process.

Erokhin [4] studied regarding the metal melting on the end face of the electrode under arc welding the kinetics of the interaction process of liquid metal with surrounding atmosphere. For an open system, where reacting agents are delivered and taken off, the mass transfer balance was described by the equation of the first-order reaction, when the process rate is proportional to first-degree concentrations:

$$\frac{dm_O}{d\tau} = \frac{g_{nm} \cdot [O]_0}{100} - \frac{g_{om6} \cdot [O]}{100} + \frac{\beta_{Me} \cdot \rho \cdot F}{100} \cdot ([O]' - [O]), \quad (2.184)$$

where $m_O = \rho \cdot V \cdot [O]$ —amount of oxygen in liquid metal, kg; ρ , V —density, kg/m³, volume, m³, of liquid metal; g_{nm} , g_{om6} —mass rates of metal melting and

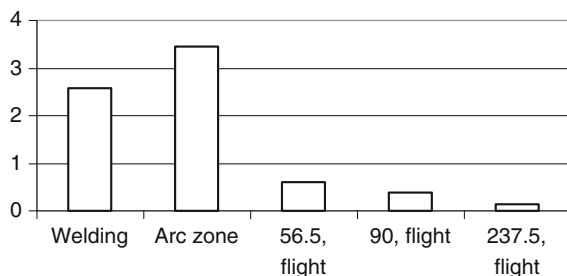


Fig. 2.49 Kinetic index of interaction at welding and arc spraying. At *Welding* drop diameter is 3 mm, $\tau_d = 0.14$ s. At *Arc spraying* in the arc burning zone the conventional drop diameter is 100 μ m, $\tau_d = 5.74 \times 10^{-4}$ s; when flying, $\tau_d = 0.001$ s

taking off, kg/s; β_{Me} —coefficient of mass transfer in metal, m/s; F —reaction surface area, m²; $[O]_0$, $[O]$, $[O]'$ —starting, current and phase interface concentrations of oxygen, wt%.

This approach can be successfully applied in calculations if the mutual influence of simultaneous reactions is not significant and if inhibitions due to internal diffusion mechanisms are not important. It should be also noted that the equilibrium oxygen concentration in the phase interface ($[O]'$) in the Eq. (2.184) was not analytically determined by Erokhin. This is related to the lack of data regarding the influence of melting conditions on the equilibrium and due to non-isothermal conditions of melting. As for plasma-arc melting, Erokhin determined equilibrium concentrations by experiment [93].

As noted before, at first all delivered oxygen is dissolved in liquid metal until the saturation point. The thermal dependence of solubility, according to Ryzjonkov [18], is:

$$\lg [O]_{Fe}^{sat} = -6320/T + 2.734 \quad (2.185)$$

where $[O]_{Fe}^{sat}$ is the oxygen saturation point in iron, wt%; T —melt temperature, K.

Unlike hydrogen and nitrogen, which comply with Sieverts' law, oxygen discharge from liquid iron into gaseous atmosphere is not likely. Let us show that, the oxygen solvability in iron is described by the equation:

$$0.5\{O_2\} = [O]_{Fe} \quad (2.186)$$

When general air pressure is 3×10^{-3} Pa, in other words, under low vacuum, oxygen partial pressure in gas phase at 1809 K will be $\leq 6 \times 10^{-4}$ Pa [43]. This value is not proper to the arc spraying process.

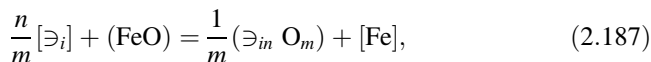
2.8.2.2 Slag-Covered Surface

For quantitative description of the interaction within the system “metal—slag” Boronenkov and colleagues proposed kinetic analysis method of simultaneous reactions taking place between metal and slag under diffusion mode. It takes into account the mutual influence of reactions and the diffusion of all components in metal and slag [94].

The theoretical ground of the method includes two principles:

- (1) under the diffusion mode of all reactions, the relation of concentrations in the interface “metal—slag” is close to equilibrium for each reaction;
- (2) velocity of transfer of each agent in metal and slag to or from their interface is proportional to the difference of concentrations in the phase volume and on the interface “metal—slag”.

Let us consider an example, which has a practical importance, of oxidation of iron impurities by slag:



where $[\mathfrak{D}_i]$ —are chemical agents (Mn, Si, Cr, P, Ti, and etc.).

Under the diffusion mode, when reactions take place in phase interface, the relation of concentrations near the border “metal–slag” is determined by the expression:

$$K_i = \frac{a_{\mathfrak{D}_{in}\text{O}_m}^{1/m} \cdot a_{\text{Fe}}}{a_{\mathfrak{D}_i}^{n/m} \cdot a_{\text{FeO}}} = \frac{\gamma_{\mathfrak{D}_{in}\text{O}_m}^{1/m} \cdot \gamma_{\text{Fe}}}{\gamma_{\mathfrak{D}_i}^{n/m} \cdot \gamma_{\text{FeO}}} \cdot \frac{N_{\mathfrak{D}_{in}\text{O}_m}^{1/m} \cdot N_{\text{Fe}}}{N_{\mathfrak{D}_i}^{n/m} \cdot N_{\text{FeO}}} \quad (2.188)$$

where K_i includes the activities of corresponding components or, at normalizing of reaction products by infinitely weak solution, coefficients of activities and coefficients for calculation of concentration in their weight equivalents; a_i , γ_i , N_i —activities, respectively, mole fractions of agents $\mathfrak{D}_i$.

This implies:

$$\frac{(\text{FeO})'}{[\text{Fe}]'} = \frac{1}{K_i} \frac{(\mathfrak{D}_{in} \text{O}_m')^{1/m}}{[\mathfrak{D}_i]'^{n/m}}, \quad (2.189)$$

where index (') corresponds to the parameters on the phase interface.

Under quasi-stationary mode the mass transfer velocity I_j , mole/(m² s) of any agent j to the interface Me–slag (or from it) is equal to:

$$I_j = \pm k_j^\partial (C_j - C_j'), \quad (2.190)$$

which implies:

$$C_j' = C_j \left(1 \pm I_j / I_j^\Pi \right) \quad (2.191)$$

where κ^∂ —mass transfer constant, m/s; S , S' are concentrations in the volume and on the interface, mole/m³; $I^\Pi = \kappa^\partial s$ —limiting diffusion flow, mole/(m² s); (–)—for initial agents, (+)—for reaction products.

Substituting molar concentrations in the Eq. (2.191) by weight concentrations and putting them into the expression (2.189) we have

$$x = \frac{(\text{FeO})'}{[\text{Fe}]'} = \frac{1}{K_i} \frac{(\mathfrak{D}_{in} \text{O}_m')^{1/m} \cdot \left(1 + I_{\mathfrak{D}_{in}\text{O}_m} / I_{\mathfrak{D}_{in}\text{O}_m}^\Pi \right)^{1/m}}{[\mathfrak{D}_i]'^{n/m} \cdot \left(1 - I_{\mathfrak{D}_i} / I_{\mathfrak{D}_i}^\Pi \right)^{n/m}} \quad (2.192)$$

The stoichiometry of the reaction (2.187) implies:

$$I_{\text{FeO}} = I_{\text{Fe}}; \quad I_i = \frac{m}{n} I_{\text{O}_i} = m I_{\text{O}_m} \quad (2.193)$$

Besides that, it should be taken onto account that I_i is a part of the total flow of iron oxide (I_{FeO}), which is consumed for the oxidation of additive i . Consequently:

$$I_{\text{FeO}} = I_1 + I_2 + \dots + I_i + \sum I_i = \sum \frac{m}{n} I_{\text{O}_i} \quad (2.194)$$

Let us express I_{FeO} and I_{O_i} in terms of x using the Eqs. (2.192 and 2.193):

$$I_{\text{FeO}} = \left(\frac{(\text{FeO})}{[\text{Fe}]} - x \right) / \left(\frac{x}{I_{\text{Fe}}^{\text{II}}} + \frac{(\text{FeO})}{[\text{Fe}] \cdot I_{\text{FeO}}^{\text{II}}} \right), \quad (2.195)$$

where $n = 1$

$$I_{\text{O}_i} = \left(x^m - \frac{1}{K_i^m} \frac{(\text{O}_m)}{[\text{O}_i]} \right) / \left(\frac{x^m}{I_{\text{O}_i}^{\text{II}}} + \frac{(\text{O}_m)}{K_i^m [\text{O}_i] \cdot I_{\text{O}_m}^{\text{II}}} \right), \quad (2.196)$$

where $n = 2$

$$I_{\text{O}_i} = I_{\text{O}_i}^{\text{II}} \left[1 + b_i \frac{I_{\text{O}_i}^{\text{II}}}{4 \cdot I_{\text{O}_m}^{\text{II}}} - \sqrt{\left(1 + b_i \frac{I_{\text{O}_i}^{\text{II}}}{4 \cdot I_{\text{O}_m}^{\text{II}}} \right)^2 - 1 + b_i} \right], \quad (2.197)$$

where $b_i = \frac{(\text{O}_m)}{K_i^m \cdot x^m \cdot [\text{O}_i]^2}$

The limiting diffusion flows of components on the interface metal—slag will be, according to [90]:

$$I_i^{\text{II}} = \kappa_i^{\partial} \cdot C_1 = \frac{D_i}{\delta_i} \cdot C_i = \beta \cdot D_i^{\alpha} \cdot C_i = \beta \cdot D_i^{\alpha} \frac{\rho}{100 \cdot M_i} \cdot c_i, \quad (2.198)$$

where K_i^{∂} is mass transfer constant, m/s; δ_i —diffusion layer thickness, m; $\alpha = 0.5$, according to [52]; S_i , c_i —molar, mole/m³, and wt%, concentrations; β —convection constant, s^{-1/2}; ρ —metal or slag density, kg/m³; M_i —molecular weight of components, kg/mole.

The convection constant is the same for all elements, is characterizes the intensity of mass transfer as the result of hydrodynamic conditions. According to calculations and experimental data obtained by Boornenkov [95], the range of most likely values is

$$\beta = (0.5 - 2.0), c^{-0.5}.$$

Putting I_{FeO} and I_{Ξ_i} into the expression (2.194), we have an equation with one unknown x . Furthermore, from the expressions (2.196, 2.197), I_{Ξ_i} is found.

It should be taken into account that, in case of significant differences of metal and slag compositions in the volume and their interface, changes in K_i values due to, first, changes in coefficients of activity, must be accounted in the Eq. (2.189). Popel and colleagues analyzed the influence of this factor [52].

The results of calculations will not depend on which reactions of all the possible ones are taken into consideration. It is sufficient that they are algebraically independent, include all necessary components, and contain a common chemical agent. This is related to the essence of the method itself, which does not allow the determination of rates of reactions themselves, but determined the flows of agents to (or from) the phase interface, consequently, the velocities of their transfer through this interface. The question of what exactly adsorption and chemical acts give place to this process cannot be solved under the diffusion mode of reactions.

2.8.3 Example Calculation of Oxygen Delivery to a Drop

After calculations of velocity, temperature of two-phase flow, and concentrations of components of the carrier mixture, items (2.2–2.7), there are enough data to calculate the kinetics of oxygen delivery to metal. We offered a scheme of oxygen delivery to a drop at arc spraying, which takes into account processes of both external and internal diffusions [96, 97]. Particularly examined is the stage of drop separation in the arc burning zone. This stage has a significant influence on the final amount of oxygen delivered. The deliveries of oxygen into metal by solution from the gas phase and by interaction with slag are considered separately.

Let us divide the process into three consequent stages characterized by different features of the interaction between liquid metal and oxygen.

Stage 1. Metal on the End Face of the Electrode

Metal is continuously delivered to the zone of interaction (to the end face) and taken off from it to a drop. The agent (oxygen) concentration in gas phase is constant. Metal temperature on the end face, according to Erokhin and Pokhodnya [4, 69], is 2400–2700 K. We take the temperature $T = 2500$ K in our calculations.

There is a restricted amount of data concerning the oxygen mass transfer coefficient in the melt (β_{Me}) for liquid metal in the arc discharge under gas flow. According to Lakomskiy [89], nitrogen in a drop of electrode metal at the end face of the welding electrode has the mass transfer coefficient in metal $\beta_{\text{Me}} = 0.29 \times 10^{-2}$ m/s. This is an order of magnitude higher than under metallurgical processes in melts during steel production, where

$$\beta_{\text{Me}} = (0.8 - 2.9) \times 10^{-4} \text{ m/s.}$$

It is evidently explained by higher values of diffusion coefficient at the drop temperature. At the same time, the interface becomes thinner due to active mixing of the drop metal. If assumed that the character of the change β_{Me} of oxygen is similar, it is likely that $\beta_{\text{Me}} = (0.1-0.3) \times 10^{-2} \text{ m/s}$. According to Erokhin's experiments, where the mass transfer coefficient is determined for drops at shielded metal arc welding, $\beta_{\text{Me}} = (0.19-0.25) \times 10^{-2} \text{ m/s}$ [4]. Let us take $\beta_{\text{Me}} = 0.25 \times 10^{-2} \text{ m/s}$ for our calculations.

Stage 2. Metal in the Arc Burning Zone

Let us take, in calculations, the scheme proposed when the features of drop formation were considered. The metal is in the form of tongue in the arc burning zone. In necks formed as the result of gas pressure drops in places of local perturbations, there take place breaks of the liquid metal jet. Joule heat discharges more intensively here than in other segments of the conductor due to higher ohmic resistance. It is assumed that this leads to increase in temperature in necks up to 2900 K and the process of jet breakage intensifies sharply due to joint processes of liquid metal boiling and collisional broadening of gas near the necks. The value of the oxygen mass transfer coefficient in metal was taken like for Stage 1.

The shape of the tongue is continuously changing under joint influence of hydrodynamic and electro-dynamic forces [48, 98] and it seems to be difficult to describe it analytically. Let us a simplification taken for the description of the initial velocity of metal drops, according to which, there are spherical metal drops in the arc burning zone with a nominal diameter equal to the "tongue" thickness, that is $d_{\text{conv}} = 10 \times 10^{-6} \text{ m}$. The dimension of the arc burning zone was estimated from the condition of the equality of the volumes of the broken away layer and drop, according to the data obtained by oscillography of drop breaking away at arc spraying. Its length, $s = 6.3 \times 10^{-3} \text{ m}$, is quite accurate because it corresponds to the data obtained by high-speed shooting [20, 98]. At the end of the arc burning zone, the fraction composition of drops corresponds to data obtained by grain-size analysis, (Table 2.15). This simplification allows making calculations though the physical pattern of the process has more complicated character. Furthermore, in compliance with experimental data on oxygen amount in the coating parameter d_{conv} can be corrected.

Stage 3. Flight along the Spraying Distance

Let us give a short characteristic of parameters in accordance with the calculations of velocities and temperatures having been made before. Approximately, after 15–25 mm of the spraying distance the drop temperature goes down to $T_{\text{m}} = 1809 \text{ K}$ and then it stays unchanging until the end of the distance due to a significant enthalpy of melting. When fraction decreases from 237×10^{-6} to $10 \times 10^{-6} \text{ m}$, average in section drop velocities on the spraying distance increase 2.5–3 times, Fig. 2.31.

Consequently, the time of drop interaction with oxygen is different. Gas temperature drops to 600–800 K. Due to intense blending of the gas jet with atmosphere the gas jet composition does not differ from air composition after the end of the initial part of the jet.

Figure 2.50 shows the physical model of oxygen delivery to a drop at arc spraying that we propose.

When the surface oxygen concentration $[O]'$ is lower than the saturation point $[O]_{Fe}^{sat}$, oxygen solves in liquid metal, which is described by the Eq. (2.185).

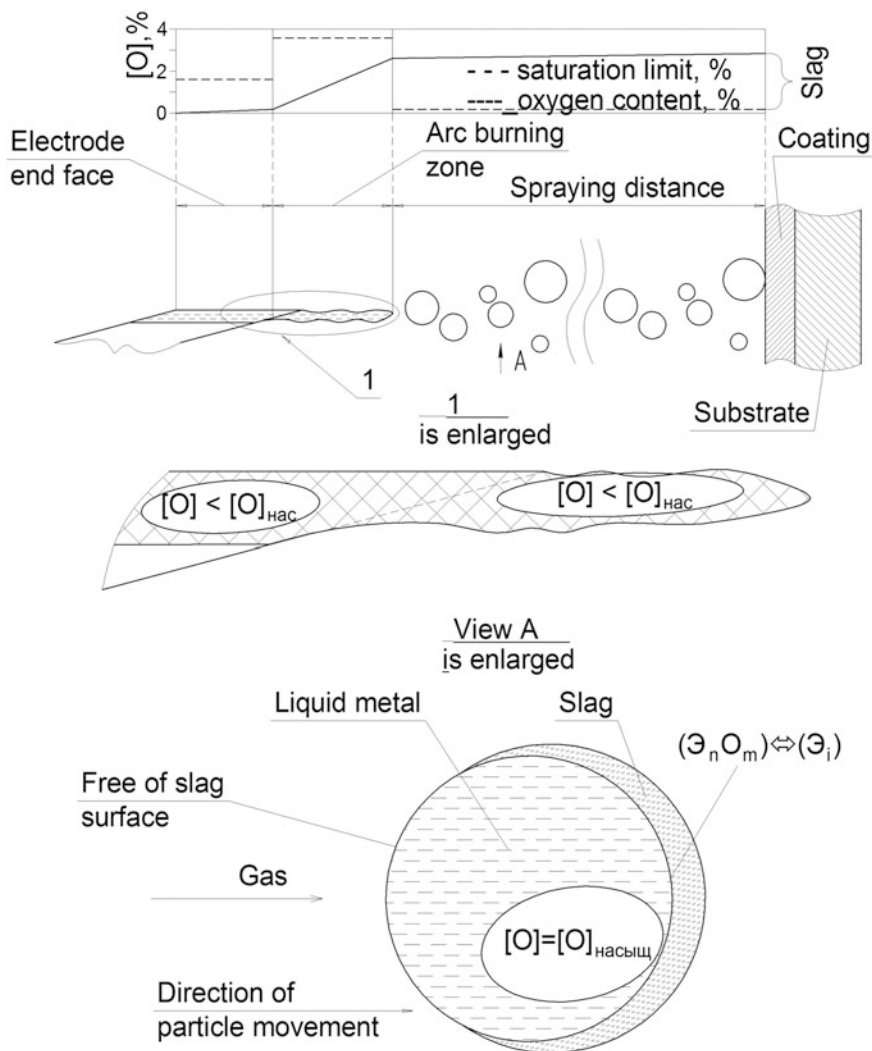


Fig. 2.50 Scheme of oxygen delivery at different stages of arc spraying process

Table 2.24 Calculation results of solubility limit of oxygen in liquid metal according to the Eq. (2.185)

T, K	1809	2500	2900
$[O]_{Fe}^{sat}$, wt%	0.174	1.607	3.587

Due to high temperatures at Stages 1 and 2, that is at the end face of the electrode and in the arc burning zone, the solubility limit of oxygen in metal is significantly higher than during drop flight before the distance, Table 2.24.

At these stages, all oxygen delivered or, at least, most of it solves in liquid metal. At the same time, the specific area of drops in the arc burning zone is much larger than the area of the end face of the electrode. It leads to the creation of the most favorable conditions for oxygen delivery to metal in the arc burning zone. At the third stage, in flight, the drop temperature decreases to $T_{nr} = 1809$ K. That leads to a decrease in solubility limit (see Table 2.24) and, correspondingly, to oxygen discharge from the supersaturated solution.

Oxygen discharged from the solution is fixed in oxides forming slag film on the surface of drops. Due to the action of the carrier gas flow, this film is pushed aside from the rear side of the drop leaving a part of surface open for oxygen delivery from outside (Fig. 2.50 view A). Oxygen from outside is used for slag film formation, which is also pushed to the front side. Here, the elements are redistributed between slag and metal.

Let us consider an example. Welding wire Cв08Г2С (wire like ER70S-6) is taken as the feedback. It contents, wt%: 0.110 C, 1.92 Mn, 0.79 Si, 0.020 P, 0.021 S.

The total amount of oxygen (m_O) delivered to the drop of the i -fraction will be equal to the sum of oxygen solved in liquid metal and contained in slag:

$$m_O = m_{Me} \frac{[O]}{100} + m_{sl} \left(\frac{(FeO)M_O}{100M_{FeO}} + \frac{(MnO)M_O}{100M_{MnO}} + \frac{(SiO_2)M_O}{100M_{SiO_2}} \right) \quad (2.199)$$

The degree of oxidation of particles in this fraction r_i will be:

$$r_i = (m_O \cdot 100) / (m_{Me} + m_{sl}) \quad (2.200)$$

The total degree of oxidation R is summed according to the distribution of drops in the weight fraction:

$$R = \left(\sum (r_i \cdot dm_i) \right) \cdot 100, \quad (2.201)$$

where for drops belonging to i -fraction: m_{Me} —liquid metal weight, kg; m_{sl} —slag weight, kg; dm_i —weigh fraction; M —molecular weight of a component, kg/mole; (FeO), (MnO), (SiO₂)—slag component concentrations, wt%.

The investigations of metallurgical interaction within the system “liquid metal–slag–gas” at welding [99–101] and steel production [52] show that the influence of

reaction of slag with gas phase can be neglected. The exception is provided by reactions with hydrogen and nitrogen participation [69], which are not considered here. Based on these papers, we assume that the process of oxygen adsorption takes place in the diffusion mode.

The kinetics of oxygen delivery into melt at the stages indicated above will be considered as in paper [4], but taking onto account the diffusion not only in metal but also in gas. For an open system with the delivery and taking off agents, the description of the process will be made through the first-order equation:

$$\frac{dm_O}{d\tau} = \frac{g_{nn} \cdot [O]_0}{100} - \frac{g_{omg} \cdot [O]}{100} + \frac{\beta_{Me} \cdot \rho \cdot F}{100} \cdot ([O]' - [O]), \quad (2.184)$$

An additional equation to find $[O]'$ and $[O]$ will be obtained from the condition of the equality of diffusion flows on the phase interface:

$$j_g = j_{Me} \quad (2.202)$$

Let us make transformations:

$$\begin{aligned} j_g &= \beta_g \cdot (C_{O_2}^0 - C_{O_2}^{'}) = \beta_g \cdot (P_{O_2} - P_{O_2}^{'}) / (2 \cdot R \cdot T), \\ j_{Me} &= \beta_{Me} \cdot (C_{[O]}^{'}) - C_{[O]} = \beta_{Me} \cdot \rho \cdot ([O]' - [O]) / M_O, \\ 1/2 \beta_g (C_{O_2}^0 - C_{O_2}^{'}) &= \beta_{Me} (C_{[O]}^{'}) - C_{[O]} \\ \beta_g \cdot (P_{O_2} - P_{O_2}^{'}) / (2RT) &= \beta_{Me} \cdot \rho \cdot ([O]' - [O]) / (100 \cdot M_O) \end{aligned}$$

The Eq. (2.202) will take the following view:

$$A_1 \cdot (P_{O_2} - ([O]')^2 / k^2) = [O]' - [O] \quad (2.203)$$

where $A_1 = 100 \cdot \beta_g \cdot M_O / (2 \cdot \beta_{Me} \cdot R \cdot T \cdot \rho)$.

The partial pressure of oxygen on the phase interface surface (P_{O_2}') will be determined through the equilibrium constant of the process of oxygen solution in liquid iron:

$$k = f_{[O]} \cdot [O]' / (P_{O_2}' / P^O)^{0.5}, \quad \lg k = 6120/T + 0.15 \quad [52], \quad (2.204)$$

where P^O is partial pressure of oxygen above the diffusion layer in gas; $f_{[O]}$ —coefficient of oxygen activity, due to its low concentration we take $f_{[O]} \approx 1$.

The calculation results given in Table 2.23 show that the relation of values of diffusion flows in gas and metal changes following the stages of metal drop life. Taking onto account of only one of them distorts the pattern of the present

processes. The introduction of an additional equation of equality of diffusion flows on the phase interface let us take into account both internal and external diffusions, obtain a more real idea of the kinetics of the processes.

We assume that at stage 1, on the end face of the electrode, quasi-stationary state is established. Here, the velocities of agent delivery and taking off are the same: $g_{dl} = g_{take\ off} = g$; and the volume of liquid layer on the face end of the electrode is constant:

$$dm_O/d\tau = 0.$$

In this case, the Eq. (2.184) will be as follows:

$$g \cdot [O]_0/100 - g \cdot [O]/100 + \beta_{Me} \cdot \rho \cdot f \cdot ([O]' - [O])/100 = 0 \quad (2.205)$$

From the joint solution of the Eqs. (2.203) and (2.204):

$$[O] = \left(-M/2 + \sqrt{M^2/4 + A_1 \cdot (1 + A_2)^2 \cdot N} \right) / \left(A_1 \cdot (1 + A_2)^2 \right), \quad (2.206)$$

$$[O]' = ([O] \cdot (1 + A_2) - [O]_0) / A_2, \quad (2.207)$$

where $A_2 = \beta_{Me} \cdot f \cdot \rho / g$,

$$M = A_2 \cdot (1 + A_2) \cdot k^2 - 2 \cdot A_1 \cdot (1 + A_2) \cdot [O]_0 - (A_2 \cdot k)^2,$$

$$N = A_1 \cdot (A_2 \cdot k)^2 \cdot P_{O_2} - A_1 \cdot [O]_0^2 + A_2 \cdot k^2 \cdot [O]_0$$

At stages 2 and 3, in the arc burning zone and in flight, we assume that a drop is a closed system, where metal is not delivered to or taken off: $g_{dl} = g_{take\ off} = 0$.

The third stage differs from the second one by decreasing temperatures of gas and metal, growing partial pressure of oxygen, smaller coefficient of mass transfer in gas (Table 2.23). The Eq. (2.184) for stages 2, 3 is as follows:

$$d(\rho \cdot V \cdot [O]) / (100 \cdot d\tau) = \beta_{Me} \cdot \rho \cdot f \cdot ([O]' - [O]) / 100 \quad (2.208)$$

If the process time is divided into so short time intervals that

$$\frac{d[O]}{d\tau} \approx \frac{\Delta[O]}{\Delta\tau},$$

we can note for time interval $(n + 1)$:

$$([O]_{n+1} - [O]_n) / ([O]' - [O]_{n+1}) = A_3 \cdot \Delta\tau \quad (2.209)$$

where $A_3 = \beta_{Me} \cdot f / V$.

From the condition of equality of diffusion flows in gas and metal $j_z = j_{Me}$ the Eq. (2.203) for the $(n + 1)$ th step of calculation will be as follows:

$$A_1 \cdot (P_{O_2} - ([O]')^2/k^2) = [O]' - [O]_{n+1} \quad (2.210)$$

Oxygen contents $[O]_{n+1}$ and $[O]'$ are defined from the joint solution of the Eqs. (2.209) and (2.210):

$$[O]' = \frac{-\frac{1}{2 \cdot (1+A_3 \cdot \Delta\tau)} + \sqrt{\left(\frac{1}{2 \cdot (1+A_3 \cdot \Delta\tau)}\right)^2 + \frac{A_1}{k^2} \cdot \left(A_1 \cdot P_{O_2} + \frac{[O]_n}{1+A_3 \cdot \Delta\tau}\right)}}{A_1/k^2} \quad (2.211)$$

$$[O]_{n+1} = ([O]_n + A_3 \cdot \Delta\tau \cdot [O]') / (1 + A_3 \cdot \Delta\tau) \quad (2.212)$$

When $[O]' > [O]_{Fe}^{sat}$, all oxygen coming from the atmosphere is consumed for slag formation. The oxygen content in metal is found from the Eq. (2.209), when $[O]' = [O]_{Fe}^{sat}$. The diffusion flow from gas into metal I_g is equal to:

$$I_g = \beta_g \cdot \left(P_{O_2} - ([O]_{Fe}^{sat}/k) \right) / (2 \cdot R \cdot T) \quad (2.213)$$

Oxygen can additionally discharge from the supersaturated solution when the drop temperature is going down after the segment of the arc burning zone. The additional diffusion flow I_{add} will be:

$$I_{add} = \beta_{Me} \cdot \rho \cdot ([O]_{sat} - [O]_n) / (100 \cdot M_O) \quad (2.214)$$

The amount of slag formed in the front side on the calculation step, Δm_s :

$$\Delta m_s = (I_g \cdot L + I_{add}) \cdot F \cdot \Delta\tau \cdot M_{FeO} \cdot (FeO) / 100 \quad (2.215)$$

where L is the part of the surface area of the rear side free of slag.

In comparison with the previous step, the weight of liquid metal will decrease due to transfer of Fe to slag:

$$m_{Me}^{n+1} = m_{Me}^n - \Delta m_w \cdot M_{\Sigma} / M_i \quad (2.216)$$

Slag from the Eq. (2.215) will be added to the slag formed before. The content of slag components on a step after events at the rear side will be:

$$(FeO)^{n+1} = ((FeO)^n \cdot m_s / 100 + \Delta m_s) / (m_s + \Delta m_s) \quad (2.217)$$

for other components of type $(\exists_K O_m)$:

$$(\exists_K O_m)^{n+1} = (\exists_K O_m)^n \cdot m_w / ((m_w + \Delta m_w) \cdot 100) \quad (2.218)$$

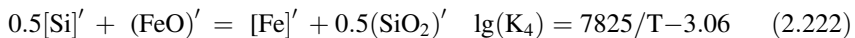
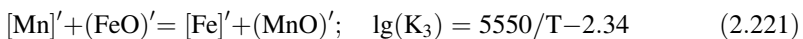
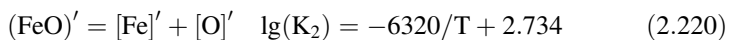
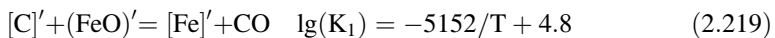
Besides slag formation, there will take place its interaction with metal. Boronenkov exposes the principles of analytical description of kinetics of this process with colleagues [94]. Arc spraying process has a number of features, which are necessary to be taken into account in the calculation of metal—slag interaction:

- Stages (electrode end face, arc burning zone, spraying distance) have different thermal, geometrical, and hydrodynamic conditions;
- Continuous renewal of metal and slag masses in interaction on the electrode end face and in the arc burning zone;
- Simultaneity of all reactions at each stage and their mutual thermodynamic and kinetic influence;
- Influence of diffusion inhibition of all agents in metal and slag on the rate of each reaction.

There are some simplifying assumptions used in the model:

- Chemical composition of metal and slag is uniform at each stage within a phase (ideal mixing);
- Contact surface of metal with slag at each stage is uniform under kinetic aspect; that is it can be characterized by some average in time contact area and mass transfer constant;
- Influence of reactions between slag and gas phase can be neglected at the first approximation.

In case the above assumptions are taken, a complex of reactions of additive oxidation with the common agent—FeO, describes the interaction of metal and slag on the phase interface:



In the Eqs. (2.219–2.222) constants K_i correspond to the Eq. (2.188)

Let us assume:

- Influence of CO diffusion on gas is small to negligible because in bubbles on the interface “metal—slag” $P_{CO} \sim 0.1$ MPa;
- Sulfur and phosphorus do not have any significant influence on slag composition because of their small concentrations.

As every reaction is accompanied by consumption of some amount of FeO, the summary diffusion flow of components is equal to the diffusion flow of FeO. For these components, the process is described by the equation:

$$I_{\text{FeO}} - I_{\text{C}} - I_{\text{O}} - I_{\text{Mn}} - 2I_{\text{Si}} = 0 \quad (2.223)$$

According to the equation of concentration dependence on the phase interface from the concentration in the volume and diffusion flows (2.191):

$$(\text{FeO})' = (\text{FeO}) \cdot (1 - I_{\text{FeO}}/I_{\text{FeO}}^n) \quad (2.224)$$

$$[\text{C}]' = [\text{C}] \cdot (1 - I_{\text{C}}/I_{\text{C}}^n) \quad (2.225)$$

$$[\text{O}]' = [\text{O}] \cdot (1 + I_{\text{O}}/I_{\text{O}}^n) \quad (2.226)$$

$$(\text{MnO})' = (\text{MnO}) \cdot (1 + I_{\text{MnO}}/I_{\text{MnO}}^n) \quad (2.227)$$

$$[\text{Mn}]' = [\text{Mn}] \cdot (1 - I_{\text{Mn}}/I_{\text{Mn}}^n) \quad (2.228)$$

$$[\text{Si}]' = [\text{Si}] \cdot (1 - I_{\text{Si}}/I_{\text{Si}}^n) \quad (2.229)$$

$$(\text{SiO}_2)' = (\text{SiO}_2) \cdot \left(1 + I_{\text{SiO}_2}/I_{\text{SiO}_2}^n\right) \quad (2.230)$$

Let us introduce a variable

$$x = \frac{(\text{FeO})'}{[\text{Fe}]'} = \frac{1}{K_i} \frac{(\vartheta_{\text{in}} O_m)^{1/m} \cdot \left(1 + I_{\vartheta_{\text{in}} O_m}/I_{\vartheta_{\text{in}} O_m}^n\right)^{1/m}}{[\vartheta_i]^{n/m} \cdot \left(1 - I_{\vartheta_i}/I_{\vartheta_i}^n\right)^{n/m}} \quad (2.231)$$

Because of high concentration of iron, its diffusion, as the inhibiting agent, can be excluded, then, from the Eq. (2.192):

$$x = (\text{FeO})'$$

From the Eqs. (2.219–2.222):

$$[\text{C}]' = \frac{1}{K_1 \cdot x}; \quad [\text{O}]' = K_2 \cdot x; \quad K_3 = \frac{(\text{MnO})'}{[\text{Mn}]' x};$$

$$K_4 = \frac{\left((\text{SiO}_2)' / [\text{Si}]'\right)^{0.5}}{x} \quad (2.232)$$

From the Eqs. (2.224) and (2.231):

$$I_{\text{FeO}} = I_{\text{FeO}}^n (1 - (\text{FeO})' / (\text{FeO})) = I_{\text{FeO}}^n (1 - x / (\text{FeO})) \quad (2.233)$$

From the Eqs. (2.219), (2.225), and (2.232):

$$I_{\text{C}} = I_{\text{C}}^n (1 - [\text{C}]' / [\text{C}]) = I_{\text{C}}^n (1 - 1 / (K_1 \cdot x \cdot [\text{C}])) \quad (2.234)$$

From the Eqs. (2.220), (2.226), and (2.232):

$$I_{\text{O}} = I_{\text{O}}^n ([\text{O}]' / [\text{O}] - 1) = I_{\text{O}}^n (K_2 \cdot x / [\text{O}] - 1) \quad (2.235)$$

From the Eqs. (2.221), (2.227), (2.228), and (2.232):

$$K_3 = \frac{(\text{MnO})'}{x \cdot [\text{Mn}]} = \frac{(\text{MnO}) \cdot (1 + I_{\text{MnO}} / I_{\text{MnO}}^n)}{x \cdot [\text{Mn}] \cdot (1 - I_{\text{Mn}} / I_{\text{Mn}}^n)} \quad (2.236)$$

Under the stationary mode, substances diffuse toward the interface in accordance with the stoichiometry of the reaction; that is $I_{\text{O}} = I_{\text{O}_m}$, or for this particular case $I_{\text{Mn}} = I_{\text{MnO}}$, then

$$I_{\text{Mn}} = \frac{K_3 \cdot x \cdot [\text{Mn}] - [\text{MnO}]}{K_3 [\text{Mn}] \cdot x / I_{\text{Mn}}^n + [\text{MnO}] / I_{\text{MnO}}^n} \quad (2.237)$$

From the Eqs. (2.222), (2.229), (2.230), and (2.232):

$$K_4 = \frac{(\text{SiO}_2)^{0.5} \left(1 + I_{\text{SiO}_2} / I_{\text{SiO}_2}^n \right)^{0.5}}{[\text{Si}]^{0.5} (1 - I_{\text{Si}} / I_{\text{Si}}^n)^{0.5} \cdot x} \quad (2.238)$$

Like in the previous case, in this one $I_{\text{Si}} = I_{\text{SiO}_2}$, then

$$I_{\text{Si}} = \frac{x^2 \cdot K_4^2 \cdot [\text{Si}] - (\text{SiO}_2)}{x^2 \cdot K_4^2 \cdot [\text{Si}] / I_{\text{Si}}^n + (\text{SiO}_2) / I_{\text{SiO}_2}^n} \quad (2.239)$$

Taking into account (2.233)–(2.239):

$$\begin{aligned} & I_{\text{FeO}}^n \left(1 - \frac{x}{(\text{FeO})} \right) - I_{\text{C}}^n \left(1 - \frac{1}{K_1 [\text{C}] x} \right) - I_{\text{O}}^n \left(\frac{K_2 x}{[\text{O}]} - 1 \right) \\ & - \frac{K_3 [\text{Mn}] x - [\text{MnO}]}{K_3 [\text{Mn}] x / I_{\text{Mn}}^n + [\text{MnO}] / I_{\text{MnO}}^n} - 2 \cdot \frac{x^2 K_4^2 [\text{Si}] - (\text{SiO}_2)}{x^2 K_4^2 [\text{Si}] / I_{\text{Si}}^n + (\text{SiO}_2) / I_{\text{SiO}_2}^n} = 0 \end{aligned} \quad (2.240)$$

Table 2.25 Coefficient of diffusion of components [52, 102]

Component	(FeO)	[C]	[O]	(MnO)
$D_0, \text{m}^2/\text{s}$	5.00×10^{-4}	3.90×10^{-6}	3.34×10^{-7}	5.00×10^{-4}
$E, \text{J/mole}$	2.04×10^5	6.68×10^4	5.00×10^4	2.04×10^5
Component	[Mn]	(SiO ₂)	[Si]	
$D_0, \text{m}^2/\text{s}$	1.80×10^{-7}	5.00×10^{-4}	3.20×10^{-5}	
$E, \text{J/mole}$	5.43×10^4	2.04×10^5	1.23×10^5	

We assumed $\beta = 1.2, \text{s}^{-1/2}$. For the calculation of the diffusion coefficients, we used the following temperature dependence:

$$D_i = D_0 \cdot \exp(-E/RT). \quad (2.241)$$

Numerical values of parameters D_0 and E are given in Table 2.25. Drop temperature is taken according to calculation results in item 2.3.

Having determined x from the Eq. (2.240) and, correspondingly, diffusion flows in the interface metal—slag: for FeO— I_{FeO} , for components— $I_{\ni i}$, we can calculate the content of oxides and elements in a drop after the events on a step of calculation in the front side.

MnO, SiO₂ in slag:

$$\left(m_{(\ni_K O_M)}^{n+1}\right)_i = \left(m_{(\ni_K O_M)}^n\right)_i + I_{\ni i} \cdot (M_{\ni_K O_M})_i \cdot f \cdot (1 - L) \cdot \Delta\tau \quad (2.242)$$

FeO in slag:

$$m_{(\text{FeO})}^{n+1} = m_{(\text{FeO})}^n - I_{\text{FeO}} \cdot M_{\text{FeO}} \cdot f \cdot (1 - L) \cdot \Delta\tau \quad (2.243)$$

C, Mn, Si in a particle:

$$\left(m_{\ni}^{n+1}\right)_i = \left(m_{\ni}^n\right)_i - I_{\ni i} \cdot M_{\ni i} \cdot f \cdot (1 - L) \cdot \Delta\tau \quad (2.244)$$

Oxygen in a particle:

$$m_{\text{O}}^{n+1} = m_{\text{O}}^n + I_{\text{O}} \cdot M_{\text{O}} \cdot f \cdot (1 - L) \cdot \Delta\tau \quad (2.245)$$

Slag mass:

$$\begin{aligned} m_w^{n+1} &= m_w^n - I_{\text{FeO}} \cdot M_{\text{FeO}} \cdot f \cdot (1 - L) \cdot \Delta\tau \\ &+ \sum \left(I_{\ni i} \cdot (M_{\ni_K O_M})_i \cdot f \cdot (1 - L) \cdot \Delta\tau \right) \end{aligned} \quad (2.246)$$

Particle mass:

$$m_{\text{Me}}^{n+1} = m_{\text{Me}}^n + I_{\text{O}} \cdot M_{\text{O}} \cdot f \cdot (1 - L) \cdot \Delta\tau - \sum (I_{\text{O}_i} \cdot M_{\text{O}_i} \cdot f \cdot (1 - L) \cdot \Delta\tau) \quad (2.247)$$

Concentration of slag components:

$$(\text{FeO}) = m_{(\text{FeO})}^{n+1} \cdot 100 / m_w^{n+1} \quad (2.248)$$

$$(\text{O}_M)_i = \left(M_{(\text{O}_M)_i}^{n+1} \right) \cdot 100 / (m_w^{n+1}) \quad (2.249)$$

The degree of oxidation of drops belonging to the i -fraction is calculated through the Eq. (2.200) and the summary degree of oxidation—through the Eq. (2.201).

When comparing experimental and calculated data (Fig. 2.51), it is seen that under limited amount of oxygen in the arc burning zone ($\alpha_{\text{ox}} = 0.8\text{--}1.2$, which corresponds to the partial pressure of oxygen $P_{\text{O}_2} = 0.0035\text{--}0.009$ MPa) the calculated values fit the interval of experimental data when the part of drop surface clean of slag is $L = 0.4\text{--}0.5$. If only compressed air is used as the carrier gas ($P_{\text{O}_2} = 0.021$ MPa), the calculated and experimental data match each other when $L = (0.3\text{--}0.35)$, in this case the velocity of oxygen delivery gets slower.

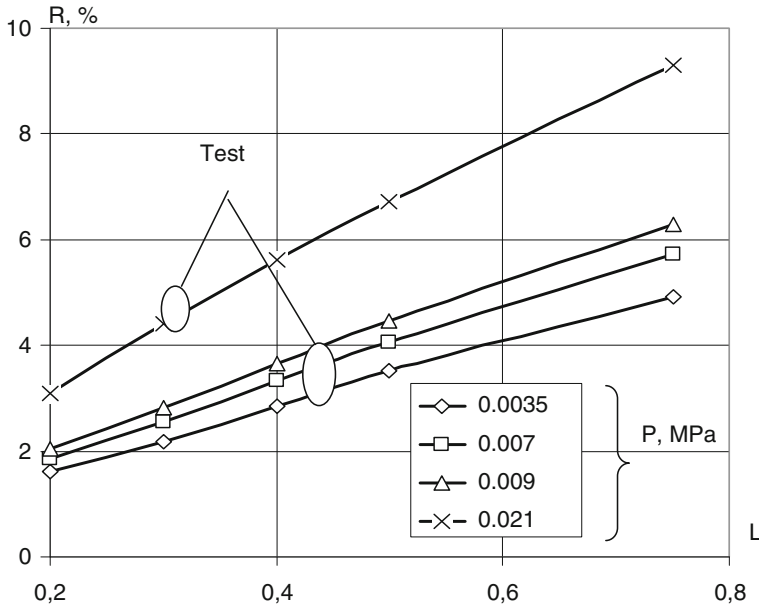
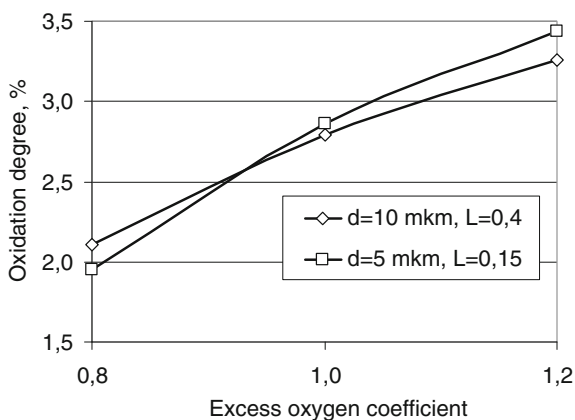


Fig. 2.51 Correspondence of experimental and calculated data according to oxidation degree (R) and partial pressure of oxygen (P) for different areas of drop surface clean of slag (L)

Fig. 2.52 Increase in oxygen content resulting from decrease in initial drop diameter



Thus, increase in oxygen delivery gas double action on the drop. On the one hand, the increase of oxygen content in gas leads to the growth of values of diffusion flows on the drop surface. On the other hand, decrease in surface tension of liquid metal in the presence of oxygen leads to the growth of the surface part covered by slag and, consequently, to the decrease in oxygen delivery to the drop. In case of large amount of oxygen, this inhibition is spectacular. Based on the hydrodynamic pattern of the process, a similar result can be obtained by use of alloying components increasing slag viscosity.

Decrease in surface tension of liquid metal on the end face of the electrode (by temperature increase or adding of surface-active agents) will lead to decrease in size of sprayed drops. However, to provide low content of oxygen at the same time, it requires more reliable protection against oxidation. Thus, if we assume that in the arc burning zone $d_{\text{экг}} = 5 \mu\text{m}$, at $L = 0.15$ the degree of oxidation is almost the same as at $L = 0.4$ for $d_{\text{экг}} = 10 \mu\text{m}$ for all compositions of atmosphere, Fig. 2.52. Thus, the requirements to factors determining the quality of coating are opposite. Increasing the density requires fine spray of metal, and decreasing the oxygen content requires a decrease in drop diameter. Bringing these requirements closer is possible, if protective atmosphere is created.

On the end face of the electrode $[O]' < [O]_{\text{Fe}}^{\text{sat}}$ and all oxygen delivered at this stage solves in metal. On the end face of the electrodes, the amount of the oxygen delivered goes down in direct proportion to the voltage on the arc when the gap between electrodes increases. The estimation of the gap size h given in the paper [22] shows $h = 0.1\text{--}1.0 \text{ mm}$. Let us show the calculation results of oxygen content on the end face of the electrode when $\alpha_{\text{ox}} = 0.8$ (Table 2.26).

Decrease in gap size leads to growth of turbulence in this area, which, in its turn, improves the conditions of mass transfer in gas.

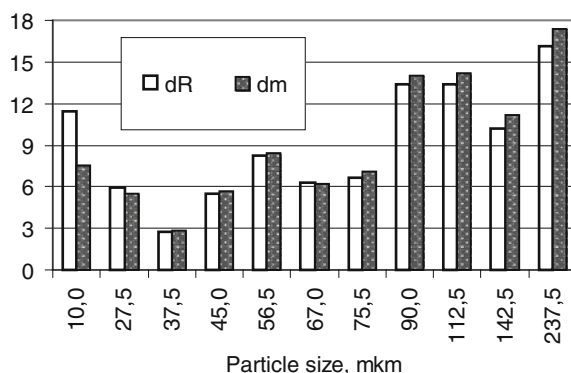
The contribution of drops belonging to small fractions to oxygen delivery $dR_i = r_i \cdot 100/R, \%$ is over their weight fraction, and for drops belonging to large fractions

Table 2.26 Changes of oxygen content in liquid metal on the end face of the electrode

h, mm	Re	Nu	β_g , m/s, Eq. (2.181)	[O], %, Eq. (2.206)	[O]', %, Eq. (2.207)
1.0	2376	31.5	20.90	0.067	0.288
0.1	237.6	10.7	68.44	0.171	0.914

Different gaps between the electrodes. Calculated data

Fig. 2.53 Relation of the contribution of fractions to oxygen delivery (dR , %) and their mass fractions (dm , %), $P_{O_2} = 0.035$ MPa



is under the corresponding weight fractions, Fig. 2.53. The contribution of drops belonging to fractions 27–75 μm to oxygen delivery is 1.5–2.5 times lower than of drops in other sizes. It is evidently related to the ratio of the specific surface $s = f/v$ and drop velocity along the spraying distance. Thus, the calculations show that the developed equipment and technologies will provide the smallest amount of oxygen delivered to coating if fraction composition of the jet is 27–75 μm .

The contribution of small fractions to oxygen delivery dR_i goes down due to increase in initial partial pressure of oxygen, Fig. 2.54.

The main oxygen delivery, 85–95 %, takes place in the arc burning zone. This is conditioned by an extremely high value of the coefficient of mass transfer in gas and large specific surface of particles. The value of oxidation degree depends very much on the coefficient of excess of gas oxidant at this stage (Fig. 2.55).

At stages 1 and 2, all oxygen delivered solves in the metal of particles. At the stage of flight, as the result of sharp drop in saturation point, there takes place slag formation only due to oxygen discharging from supersaturated solution and delivered to the rear side from atmosphere. For the most likely $L = 0.45$ on the spraying distance, the weight fraction of slag is 5–12 % and it increases along with the growth of partial pressure of oxygen and decrease in particle diameters (Fig. 2.56).

The indicated features must be taken into account for the development of technologies of coating application and design of equipment as well.

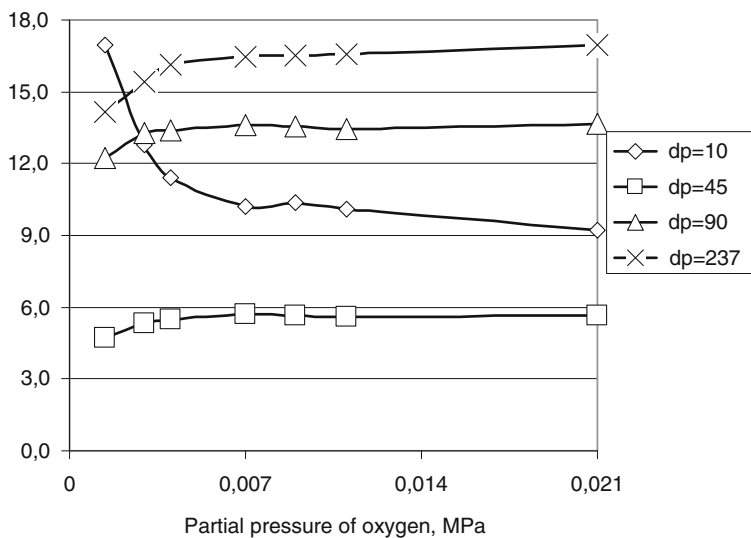


Fig. 2.54 Contribution of fractions to oxygen delivery under different initial partial pressures of oxygen

Fig. 2.55 Contribution of arc spraying process stages to oxygen delivery into drops under different initial partial pressures of oxygen, P , MPa

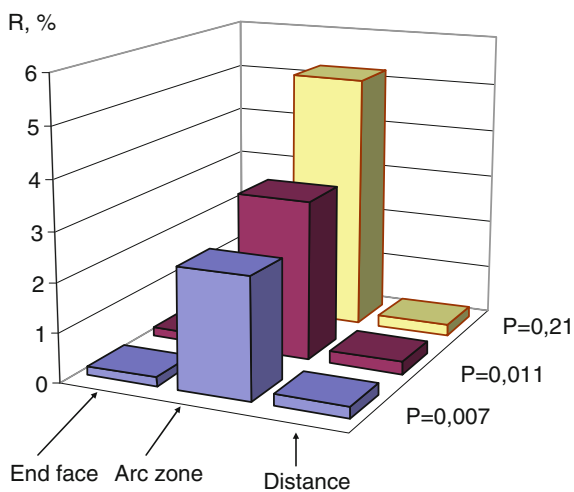
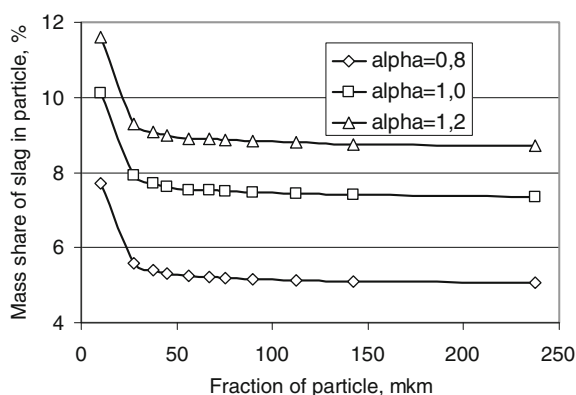


Fig. 2.56 Weight fraction of slag in a drop under different initial gas composition (α is oxidant excess coefficient)



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