Modeling of ash deposition on the wall of a high temperature slagging gasifier

Tommaso Melchioria, Micael Bouletb, Jean-Michel Lavoiea,⇑

a Industrial Research Chair On Cellulosic Ethanol and Bio-commodities (CRIEC-B), University of Sherbrooke, 2500 boul. de l’Université, Sherbrooke, QC J1K2R1, Canada
b Enerkem Inc., 3375 King Street West Suite 200, Sherbrooke, QC J1L1P8, Canada

Keywords:
Slag
Gasification
Ash
Modeling

A B S T R A C T

In high temperature gasification reactors, inert ashes coming with the feedstock deposit on the walls in the form of slag. In these kind of reactors, external cooling screens are used to control slag deposition. A transient 2D model was developed and implemented to simulate the growth of a slag liquid-solid layer at the walls of an entrained flow coal gasifier operating at 1200–1500 °C and external cooling at 30–600 °C. The model results were compared with those of other models present in the literature. The effect of operating variables on layer thickness and wall heat flux were studied through sensitivity analysis. The study shows that the most important parameter controlling slag accumulation is the difference between gas temperature and slag critical viscosity temperature. This affects slag viscosity and eventually the degree of wall agglomeration at steady state. The role of the external cooling temperature and ash flowrate was also investigated. The model was modified to account for the effect of gas cooling due to wall heat transfer by convection and radiation. Results show that if gas flowrate is low, e.g. a small-scale unit, this effect is not negligible, contrarily to what is usually assumed in the literature.

1. Introduction

Gasification is an important technology for the conversion of solid hydrocarbons (coal and/or biomass) into valuable syngas. This intermediate product is used in a number of important applications, from synthesis of liquid fuels (Fischer-Tropsch process), to methanol [1]. Besides, it can be used in combined heat and power applications [2].

The main product of gasification is a mixture of gases called syngas (H₂ and CO mainly, with a variable content of CO₂ and CH₄). Important by-products are tars (hydrocarbons that are condensable at room temperature), char (solid carbonaceous species with very low hydrogen content), and ashes, a mixture of inorganic compounds (mainly metal oxides) that are originally present in the feedstock and cannot be further converted. In general, the target is to maximize gas yields. Some studies about catalytic conversion of tars at relatively low temperatures show promising results [3]. Nevertheless, kinetics for gas phase conversion of tars and char are known to be quite slower than those of the original biomass at low temperatures [4]. Alternatively, high temperatures (>1300 °C) are needed to carry out thermal conversion of tar and char at a reasonably low residence time, to avoid the need of very big reactor volumes. This temperature is usually higher than the melting point of ashes. So if high temperature gasification is carried out, partial or total melting of ashes is expected. This can lead to a number of problems in the reactor operation. Molten slag can easily stick to the reactor walls, and the inlet and outlet pipes. Its accumulation can possibly lead to clogging if not properly controlled. Liquid slag can also be chemically active at high temperatures, leading to leaching of the refractory walls when permanently in contact with it. As a result, the lifetime of the walls’ material is decreased to a few months [5,6]. As a consequence, slag control is one of the main issues in a high temperature gasifier. One possible way to address these problems is to use the slag itself as a refractory layer at the walls [7–9]. Layer growth can be controlled by applying external cooling, thus setting the heat flux at the walls [10]. The deposited solidified slag has an insulation effect because of the low thermal conductivity of the metal oxides composing it. After some time, the system reaches an equilibrium so that the inner temperature is high enough to avoid solidification of the new liquid slag, which flows downwards after being deposited on the solid layer [11]. From that moment on, the layer does not grow any more. Final thickness depends on the properties of the slag and the operating conditions of the reactor and must be estimated when designing the reactor, ensuring that no clogs are formed.

⇑ Corresponding author.
A first analytical model describing the slag flow was proposed by Seggiani [12]. The latter is quite simple and can predict the behaviour of different gasifiers with a reasonable degree of confidence. Some simplifications were made on the temperature profile, allowing analytical integration of the 1D momentum equation. More detailed approximations of the temperature profile were introduced by Kittel et al. [13], and Yong et al. [14]. Ye and Ryu observed. A critical viscosity temperature (Tcv) can be defined, which is progressively softened can be defined. This range can be correlated to an adequate characterization of slag properties, in particular its viscosity as a function of temperature [9,19]. Since the critical viscosity temperature is linked to the temperature of slag, gas, and cooling water (K).

In general, Tcv depends on the chemical composition of the ash [29,30], so it is a representative parameter of a system operating with a certain type of feedstock. Some synthetic indicators used to correlate this parameter with composition are the base-to-acid ratio [31,30], or the Q factor (related to the degree of polymerization of the molecular chains [29]). A lot of research has been done in the years to characterize the viscosity of slags. One of the earliest correlations by Hoy et al. [32] assumes a dependency on the square of the Silica factor. The matching of experimental data was improved by Watt and Fereday [33], who introduced an Arrhenius dependency of viscosity on temperature, with both activation energy and pre-exponential factor as a linear combination of the parameters (glass formers, amphoterics and modifiers). The Urbain model was extended and modified by several authors. Riboud and Larrechq [36] changed the equation parameters, introducing polynomial functions of the composition. Other variations were proposed by Kondratiev and Jak [37], Frank and Kalmanovitch [38], Streeter et al. [39], Mudersbach et al. [40]. A compared analysis of different models by Vargas et al. [41] on different types of coal ash showed that while the Urbain model leads to generally satisfactory results compared to experimental data, the Frank and Kalmanovitch has better prediction of slag viscosity for slags with high silica content. A different correction to the Urbain model was proposed by Mills and Sridhar [42], who introduced a function of the optical basicity, which better quantifies the degree of polymerisation. Other authors calculate the parameters from thermodynamics [43,44]. Different types of model may include a function of the concentration of solid crystals [45] (particularly suitable when temperature is close to the Tcv), and the application to non-Newtonian fluid models [46]. More comprehensive reviews of the main constitutive models for slag viscosity and other transport properties are reported elsewhere [29,47–49].

The present study, a detailed time-dependent 2D model to describe slag flow is presented and discussed. The model results were then compared with the main ones found in literature models for a given base case, and the main variables controlling the sensitivity of the model were identified. A model refinement was also proposed to account for gas cooling due to heat transfer with the slag at the wall.

In general, Tcv depends on the chemical composition of the ash [29,30], so it is a representative parameter of a system operating with a certain type of feedstock. Some synthetic indicators used to correlate this parameter with composition are the base-to-acid ratio [31,30], or the Q factor (related to the degree of polymerization of the molecular chains [29]). A lot of research has been done in the years to characterize the viscosity of slags. One of the earliest correlations by Hoy et al. [32] assumes a dependency on the square of the Silica factor. The matching of experimental data was improved by Watt and Fereday [33], who introduced an Arrhenius dependency of viscosity on temperature, with both activation energy and pre-exponential factor as a linear combination of the parameters (glass formers, amphoterics and modifiers). The Urbain model was extended and modified by several authors. Riboud and Larrechq [36] changed the equation parameters, introducing polynomial functions of the composition. Other variations were proposed by Kondratiev and Jak [37], Frank and Kalmanovitch [38], Streeter et al. [39], Mudersbach et al. [40]. A compared analysis of different models by Vargas et al. [41] on different types of coal ash showed that while the Urbain model leads to generally satisfactory results compared to experimental data, the Frank and Kalmanovitch has better prediction of slag viscosity for slags with high silica content. A different correction to the Urbain model was proposed by Mills and Sridhar [42], who introduced a function of the optical basicity, which better quantifies the degree of polymerisation. Other authors calculate the parameters from thermodynamics [43,44]. Different types of model may include a function of the concentration of solid crystals [45] (particularly suitable when temperature is close to the Tcv), and the application to non-Newtonian fluid models [46]. More comprehensive reviews of the main constitutive models for slag viscosity and other transport properties are reported elsewhere [29,47–49].

In the present study, a detailed time-dependent 2D model to describe slag flow is presented and discussed. The model results were then compared with the main ones found in literature models for a given base case, and the main variables controlling the sensitivity of the model were identified. A model refinement was also proposed to account for gas cooling due to heat transfer with the slag at the wall.

### Nomenclature

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cp, Cpg</td>
<td>heat capacity of slag and gas (J/kg/K)</td>
</tr>
<tr>
<td>Cd</td>
<td>drag coefficient (–)</td>
</tr>
<tr>
<td>g</td>
<td>gravitational acceleration (m/s²)</td>
</tr>
<tr>
<td>hlg, hsw</td>
<td>gas-liquid and solid-wall heat transfer coefficients (W/m²/K)</td>
</tr>
<tr>
<td>jfl</td>
<td>slag mass flux at gas-liquid interface (kg/m²/s)</td>
</tr>
<tr>
<td>Kgfl</td>
<td>momentum transfer coefficient (Pa s/m)</td>
</tr>
<tr>
<td>k</td>
<td>slag thermal conductivity (W/m/K)</td>
</tr>
<tr>
<td>mdep</td>
<td>slag flow rate (kg/s)</td>
</tr>
<tr>
<td>mg</td>
<td>gas flow rate (kg/s)</td>
</tr>
<tr>
<td>n</td>
<td>normal coordinate to the boundary surface</td>
</tr>
<tr>
<td>qrad, qconv</td>
<td>heat fluxes due to radiation, convection, and slag deposition (W/m²)</td>
</tr>
<tr>
<td>R</td>
<td>reactor radius (m)</td>
</tr>
<tr>
<td>r</td>
<td>radial coordinate (m)</td>
</tr>
<tr>
<td>T</td>
<td>temperature of slag, gas, and cooling water (K)</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>ur, uz</td>
<td>radial and axial velocity components in cylindrical coordinates (m/s)</td>
</tr>
<tr>
<td>ur, ut</td>
<td>normal and tangential components of velocity at boundary (m/s)</td>
</tr>
<tr>
<td>ufl</td>
<td>gas velocity (m/s)</td>
</tr>
<tr>
<td>Vfl</td>
<td>slag volume (m³)</td>
</tr>
<tr>
<td>zd</td>
<td>axial coordinate (m)</td>
</tr>
<tr>
<td>∆θl, ∆θs</td>
<td>slag thickness, total, of liquid and solid layer (m)</td>
</tr>
<tr>
<td>s</td>
<td>slag emissivity (–)</td>
</tr>
<tr>
<td>μ</td>
<td>slag viscosity (Pa s)</td>
</tr>
<tr>
<td>ρs</td>
<td>slag density (kg/m³)</td>
</tr>
<tr>
<td>σ</td>
<td>Stefan-Boltzmann constant (W/m²/K⁴)</td>
</tr>
<tr>
<td>τrr, τzz, τzz</td>
<td>components of 2D stress tensor in cylindrical coordinates (Pa)</td>
</tr>
<tr>
<td>τnn, τnt</td>
<td>normal and tangential component of boundary stress (Pa)</td>
</tr>
</tbody>
</table>

---

T. Melchiori et al. / Fuel 197 (2017) 100–110
2. Model description

The developed model aims at describing the evolution in time of the local thickness of the solid and liquid slag layers at the reactor walls in an axisymmetric geometry. A local radial slag deposition rate is assumed along the walls. The local temperature gradients were solved and, from these, slag viscosity is calculated. This is used to estimate the slag velocity field. Finally, a local mass balance based upon slag velocity and determining local growth of the solid layer was developed. It is assumed that liquid slag is chemically inert, so no reactions can occur after it is deposited on the walls. This hypothesis is correct if the conversion of the carbonaceous components in the momentum equations did not change the results, thus confirming this hypothesis. The non-linear convective term can be valid for all the types of geometries that we want to describe. This part of the model differs from the equations formulated by the other authors.

2.1. Momentum balance

The momentum balance inside the slag layer was derived by considering the equilibrium between gravity and viscous forces. The first one makes the slag flow downwards, whereas the second works in the opposite direction. Considering a 2D axisymmetric geometry, the equations to calculate the radial and axial velocity components in cylindrical coordinates are as follows:

\[
\begin{align*}
- \left( \frac{1}{r} \frac{\partial}{\partial r} (r \tau_r) + \frac{\partial \tau_z}{\partial z} \right) &= 0 \\
- \left( \frac{1}{r} \frac{\partial}{\partial r} (r \tau_z) + \frac{\partial \tau_r}{\partial z} \right) - \rho_g g &= 0
\end{align*}
\]

where the components of the 2D stress tensor are given by:

\[
\tau_r = -2\mu \frac{\partial u_r}{\partial r} \\
\tau_z = -2\mu \frac{\partial u_z}{\partial z}
\]

\[
\tau_{zz} = \tau_{rr} = -\mu \left( \frac{\partial u_r}{\partial z} + \frac{\partial u_z}{\partial r} \right)
\]

In this formulation, the time derivative term of the velocity was neglected, which means that whenever there is a local change of thickness in the slag layer, a new forces equilibrium is reached relatively quickly. This is a fair assumption, considering the slow dynamics of the layer growth. Some preliminary simulations performed by including the time derivative term of the velocity components in the momentum equations did not change the results, thus confirming this hypothesis. The non-linear convective term of the Navier-Stokes equations was also neglected due to very low slag velocities (10^{-2} – 10^{-3} m/s) and Reynolds numbers, showing that the Stokes regime can be safely assumed. Again, some preliminary simulations were made including the convective terms, leading to exactly the same numerical results.

Because of the very high viscosity gradients in the slag layer (particularly radial), it is not possible to take \( \mu \) out of the derivatives. The radial velocity component is basically generated by the axial variation of the \( \tau_r \) stress component. When the slag deposition is simulated on a cylindrical wall, the radial velocity component is very small as compared to the axial one. In this situation a fair assumption is to set it equal to zero. As a consequence, only one component of the momentum equations must be solved. However, if the flow of slag in a conical region (the so called “slag tap”) is also simulated, this hypothesis is no longer true. The radial velocity component can also appear when the layer thickness changes significantly along the axial direction, because the slag tends to flow along the direction of the gas-liquid interface. For these reasons, we considered this more general formulation, which can be valid for all the types of geometries that we want to describe. This part of the model differs from the equations formulations shown by the other authors.

At the gas-liquid interface, the boundary conditions are set assuming that normal and tangential stresses are equal to zero:

\[
\tau_{ns} = -2\mu \frac{\partial u_n}{\partial n} = 0
\]

\[
\tau_{nz} = -\mu \left( \frac{\partial u_n}{\partial z} + \frac{\partial u_z}{\partial n} \right) = 0
\]

\[
u_n = u_{slag}
\]
where the normal and tangential velocity components are found as a function of the radial and axial ones with the following transformation:

\[u_n = u_r \cdot n_r + u_z \cdot n_z\]  \hspace{1cm} (9)

\[u_t = -u_r \cdot n_r + u_z \cdot n_z\]  \hspace{1cm} (10)

The local normal slag velocity at the gas-liquid interface can be calculated as the ratio between local slag flux coming from the gas phase and slag density.

At the outlet, a zero tangential velocity and normal stress is assumed:

\[u_t = 0\]  \hspace{1cm} (11)

\[\tau_{nt} = -2\mu \frac{\partial u_n}{\partial n} = 0\]  \hspace{1cm} (12)

In these equations, \(n_r\) and \(n_t\) are the radial and axial components of the normal vector pointing out of the boundary surface, respectively. In general, they are different in any point of the gas-liquid interface. If gas velocity at the interface is very high, tangential stress may not be zero. In that case, the boundary condition could be change to account for the drag effect of the gas on the liquid. Drag force will be proportional to the local net velocity difference between the gas and the liquid at the interface.

\[\tau_{nt} = -K_{gl}(u_t^* - u_t)\]  \hspace{1cm} (13)

In this formulation, \(K_{gl}\) is the momentum transfer coefficient between gas and liquid, and depends on the drag coefficient \(C_d\) as follows:

\[K_{gl} = \frac{\rho \cdot C_d}{2} |u_t^* - u_t|\]  \hspace{1cm} (14)

The drag coefficient can be calculated using a proper correlation [51]. In the present work, the effect of drag was neglected. This could be a fair assumption, especially for low gas Reynolds numbers. At the wall interface, a no-slip boundary condition was set (both velocity components must be zero).

To describe the dependency of slag viscosity on temperature, an Arrhenius type equation was used:

\[\mu = a \cdot T \cdot \exp \left(\frac{b}{T}\right)\]  \hspace{1cm} (15)

where \(a\) and \(b\) are two constants that depend on the type of slag (basically its chemical composition). The equation by Frank and Kalmanovitch [38] was used to calculate \(a\) and \(b\), knowing the mass fractions of 15 metal oxides.

It is commonly assumed in literature that if slag viscosity is higher than 25 Pa s, slag can be considered to be in the solid phase, and to move no more [1,11,15,48]. The viscosity equation can be solved in terms of \(T\) to obtain the temperature of critical viscosity \(T_{cv}\). The value of \(T_{cv}\) is very important because it is related to the minimum operating temperature of the gas to avoid an infinite growth of the slag layer, which can lead to serious problems in reactor operation. To account for the transition between solid and liquid, the value of slag viscosity was set to infinite in any point of the system where \(T < T_{cv}\). In this way, the conservation equations are solved in a single geometry for both liquid and solid phases.

2.2. Energy balance

Convection and thermal conduction were considered inside the slag layer. The following balance can be obtained, for a 2D axisymmetric geometry:

\[\rho_s C_p \left( u_t \frac{\partial T}{\partial t} + u_r \frac{\partial T}{\partial r} + u_z \frac{\partial T}{\partial z} \right) = \frac{1}{r} \frac{\partial}{\partial r} \left( r k \frac{\partial T}{\partial r} \right) + \frac{\partial}{\partial z} \left( k \frac{\partial T}{\partial z} \right)\]  \hspace{1cm} (16)

Conduction in the radial direction is the dominating term in the equation because of the relatively low velocities. However, convection must be included to properly describe the partial cooling of the liquid slag.

At the gas-liquid interface, the boundary condition considered is set assuming that the conductive flux is equal to the contribution of the external heat fluxes coming from the gas phase, as follows:

\[k \left( \frac{\partial T}{\partial n} \right)_g = q_{rad} + q_{con} + q_{dep}\]  \hspace{1cm} (17)

The first two terms on the right-hand side are related to the contribution of gas radiation and gas heat transfer by convection/conduction, respectively. They can be calculated as follows:

\[q_{rad} = \sigma_s e_s (T_g^4 - T^4)\]  \hspace{1cm} (18)

\[q_{con} = h_L (T_g - T)\]  \hspace{1cm} (19)

The chosen radiation model (Eq. (18)) accounts for the partial absorptivity of the slag, that can be generally considered as a “gray body” for a layer thickness greater than 3.5 mm [52]. The gas phase, on the contrary, is assumed to have an ideal emissivity (\(e_s = 1\)). In reality, this number may depend on several factors, namely gas composition (\(H_2O, CO_2\) and \(CO\) will highly contribute to increase it) and thickness (basically related to the reactor diameter). Besides, char particles behaves like black bodies, so their concentration in the gas phase will also give an important contribution. In general, Eq. (18) can be considered as a good approximation for large reactors with high concentrations of steam, \(CO_2\) and char. The radiation model can be further tuned and improved once gas properties and composition are known. The third flux is related to the slag heat of deposition. It is the heat flux necessary to cool down the slag from the temperature of the gas down to the local temperature of the liquid film surface. It can be calculated as follows:

\[q_{dep} = j_s C_p (T_g - T)\]  \hspace{1cm} (20)

where \(j_s\) is the local slag flux deposited on the layer surface. At the outlet, a zero conductive flux was assumed:

\[k \left( \frac{\partial T}{\partial n} \right)_l = 0\]  \hspace{1cm} (21)

At the wall, the conductive flux was set equal to the heat extracted from the cooling membrane:

\[k \left( \frac{\partial T}{\partial n} \right)_w = q_{wall}\]  \hspace{1cm} (22)

\[q_{wall} = h_w (T - T_w)\]  \hspace{1cm} (23)

In this expression, \(T_w\) is the temperature of the cooling water, whereas \(h_w\) is the global heat transfer coefficient at the wall.

2.3. Mass balance

The continuity equation for a single phase in differential form is the following:

\[\frac{\partial \rho}{\partial t} + \nabla \cdot (\rho u) = 0\]  \hspace{1cm} (24)

This equation can be written in an integral form. Doing that, and applying the divergence theorem, it becomes:

\[\frac{\partial \rho}{\partial t} = -\int_S u \cdot n dS\]  \hspace{1cm} (25)
where slag density is considered to be a constant.

This equation means that the change of volume of slag is given by the net difference of volumetric flowrate entering or exiting the system. If we apply this equation to a control volume of slag between two coordinates \(z\) and \(z + \Delta z\), this becomes:

\[
\frac{dV_s}{dt} = - \int_{R_{k-o}}^{R} 2\pi r u_z(r, z + \Delta z) dr + \int_{R_{k-o}}^{R} 2\pi r u_z(r, z) dr + \frac{\dot{m}_{dep}}{\rho_s}
\]

(26)

If \(\Delta z\) is very small, this becomes a finite volume approximation that can be used to determine the local variation of thickness \(\delta\). Assuming that \(\delta\) is constant in a small volume, the following change of variable can be applied, obtained from simple geometrical derivations:

\[
V_s = \pi (R^2 - (R - \delta)^2) \Delta z
\]

(27)

Replacing this into the previous equation, it becomes:

\[
2\pi (R - \delta) \Delta z \frac{d\delta}{dt} = - \int_{R_{k-o}}^{R} 2\pi r u_z(r, z + \Delta z) dr + \int_{R_{k-o}}^{R} 2\pi r u_z(r, z) dr + \frac{\dot{m}_{dep}}{\rho_s}
\]

(28)

This equation was derived assuming that the radius of the walls is constant along \(z\) (this is true for a cylindrical vessel). It can be proven that it is still valid for a geometry with non-uniform radius (for instance for conical shaped walls). This equation can be solved once the velocity profiles are known. At the equilibrium, the left-hand side of Eq. (28) is zero. This occurs when the velocity of the slag is high enough to balance the effect of the slag flux deposited at the interface. According to momentum balance, this is possible if viscosity is low.

2.4. Numerical solution

Although for the present study only the steady state solution was desired and reported in the results, the model had to be solved with a transient solver to avoid convergence instabilities. The described system of PDE presents two main complications:

- The space domain must be re-meshed every time step (or every iteration, if a steady state solver is used).
- The mass balance is a mixed differential-integral equation, which requires a special treatment.

The elimination of time derivatives in the momentum and energy equations has the advantage of decoupling the solution of the space and time derivatives. Basically, at every time step, the new geometry and mesh are obtained (based on the value of \(\delta\)), then the velocity and temperature profiles are calculated. Finally, the integral terms in the mass balance are computed, and the new value of \(\delta\) is found for every \(z\) coordinate.

The momentum and energy equations were solved with a Finite Element approximation, using three point triangular elements, and a weak formulation of the second order terms (stresses and conduction flux). This approach has the considerable advantage of a very easy implementation of the boundary conditions, particularly the Neumann type. The model was implemented in Matlab [53] using some external functions compiled in Fortran to calculate the system matrices.

3. Results and discussion

The presented slag model was used to simulate the Prenflo coal gasifier that was first studied by Seggiani [12]. The characteristics of the reactor, the properties of the slag, and other parameters used in the simulations are reported in Table 2. It was assumed that the total amount of ash entering the reactor is deposited uniformly at the reactor wall along the axial coordinate. The effect of heat transfer from the gas due to convection (Eq. (19)) was neglected. Some preliminary simulations confirmed that such a term is small when compared to the radiation flux, as confirmed by other authors [13]. The slag properties used for the base case simulations are the same considered by Ye and Ryu [15] in their comparative study. The value of the wall heat transfer coefficient, used in Eq. (23), was estimated using the analogy of the equivalent thermal circuit, including three thermal resistances: the wall refractory lining (16 mm thickness SiC), the wall of the cooling membrane tubes (6.3 mm steel) and the cooling water heat transfer. This last term was calculated using the correlation for helical tubes reported by Kittel et al. [13].

Fig. 1 shows the calculated map of temperature and liquid velocity magnitude as a function of the reactor height and radial distance to the wall. The results refer to the solution after steady state is reached. A line also shows the interface between the solid layer and the liquid slag. Such a line is formed by the points in the system where slag viscosity is equal to critical viscosity (25 Pa·s). Consistently, slag velocity external to it is uniformly zero. For the considered base case, calculated thickness at the bottom of the reactor is of 27 mm for the solid phase and 7 mm for the liquid phase.

Temperature profiles in the radial direction are fairly linear, as the radial gradients form the dominant terms in the transport equations. Temperatures at slag surface vary from 1400 °C at the top to 1490 °C at the bottom and decrease to critical temperature value (1275 °C) at the gas-liquid interface. At the wall, temperature is 366 °C at the top and 560 °C at the bottom. The solid layer is much thicker than the liquid layer since the difference between the critical viscosity temperature and the cooling water temperature is bigger than the difference between the gas temperature and the critical viscosity temperature. Liquid slag velocity at the gas interface goes from 0.2 cm/s at the top, where the slag is colder, to 5 cm/s at the bottom, where it is closer to the gas temperature.

The results of the present model were compared with those obtained by Ye and Ryu [15], Yong et al. [14], and Seggiani [12]. Figs. 2 and 3 show the comparison of the models in terms of liquid thickness, liquid surface temperature, solid thickness, and liquid-to-solid flux profiles as a function of the reactor height coordinate. The results of the presented model agree quite well with those of the other authors, particularly with the Ye-Ryu model [15], which is the most comparable with regards to the considered equations. The simplified model by Seggiani [12] also has very similar results, whereas the Yong model [14] predicts a lower thickness of the liquid and solid slag and a lower liquid temperature. The discrepancies of small temperature profiles between the current model and the one reported by Ye and Ryu can be due to the fact that
the authors considered a slag deposition temperature 50 °C lower than the gas temperature. For the present calculations, instead, the temperature of slag coming with the feedstock was assumed to be equal to the gas temperature. This difference did not, however, have a visible impact on the estimation of slag layer thickness. Estimated local heat flux at the wall is in agreement with the range measured in a real pilot gasifier [54].

3.1. Sensitivity to gas temperature and slag viscosity

The present model was used to perform different simulations where slag critical viscosity temperature was modified. The effect of slag critical temperature on wall accumulation in a high temperature gasifier was estimated and is shown in Figs. 4 and 5. For each value of $T_{cv}$, parameters $a$ and $b$ in Eq. (15) were calculated through the Frank and Kalmanovitch model [38] and simulation was repeated. The reported curves are parametric to gas temperature, which was also varied between 1200 °C and 1500 °C. All other variables were the same as reported in Table 2. Fig. 4 shows calculated thickness of solid and liquid phases at the wall, averaged along the axial coordinate at steady state. When $T_{cv}$ approaches gas temperature, solid thickness shows a sharp increase. At the limit, when the two values are identical, no equilibrium can be reached, and slag deposition continues until complete blockage of the reactor. Even when $T_{cv}$ is 100 °C lower than $T_g$, solid thickness is very high (~60–70 mm), and it is quite sensitive to a small variation of $T_{cv}$. Thickness of the liquid slag also increases with the critical viscosity temperature, but to a lower extent (it varies in the range of 3–6 mm for the considered cases).

Results for the heat extracted at the wall due to external cooling are shown in Fig. 5. The total heat extracted $Q$ (kW) was calculated as a surface integral of the local heat flux $q_w$ (kW/m²) estimated by the model from Eq. (23). The following expression was used:
When solid layer thickness is below 10 mm, heat loss is in the order of 10 MW. This decreases with increasing critical temperature and decreasing gas temperature. If $T_g - T_{cv}$ is 100 °C, heat rate at the wall is below 2 MW.

These simulations show that the cooling membrane system cannot work if the critical temperature of the ash is too close to the operating temperature of the gasifier. On the other hand, the solid layer is necessary to ensure a sufficient thermal insulation of the system. If gas temperature is too high, this layer will not form, and heat losses may be too important. This could eventually lead to a decrease in gas temperature and a consecutive increase of solid layer thickness at the bottom of the reactor. This aspect will be discussed in more details below.

3.2. Sensitivity to cooling water temperature

Another series of simulations was carried out, considering the same set of parameters as that of the base case, only with a different cooling water temperature. The height averaged thickness of solid and liquid layers was calculated at steady state for the different cases, as well as maximum thickness (the one that can be observed at the bottom of the reactor). The results are shown in Fig. 6.

If the cooling water temperature is lower, slag temperature close to the reactor walls also decreases, thus increasing the thickness of the solid layer at the equilibrium. Such thickness is roughly proportional to the difference between critical

$$Q = 2\pi R \int_0^H z \cdot q_w \, dz$$  \hspace{1cm} (29)
viscosity temperature (equal to slag temperature at the interface between the solid and liquid phases) and the temperature at the wall. However, the thickness of the liquid layer was found to be constant for all simulations (average value is 5 mm). The calculated total heat extracted at the walls is also not sensitive to cooling water temperature, as shown in Fig. 7. For all considered cases, this value was found to be equal to 5600 kW. The global temperature gradient, being the driving force of heat flux and thickness (which influence thermal resistance and promote the insulation effect), changes accordingly to counterbalance their effects. The relationship between Figs. 6 and 7 can be explained more clearly by considering the energy balance in the solid layer, where conduction is the only heat transport mechanism. For the solid part, Eq. (16) becomes:

\[
0 = \frac{1}{r} \frac{\partial}{\partial r} \left( r k \frac{\partial T}{\partial r} \right)
\]

where the axial component of the heat flux was neglected because axial temperature gradients are very small as compared to the radial ones.

Solving this equation between the wall \((r = R)\) and the solid-liquid interface \((r = R - \delta_s)\), if the thermal conductivity is constant, we obtain:

\[
T(r) = T_{\text{wall}} + \frac{T_{cv} - T_{\text{wall}}}{\ln \left( \frac{R}{R - \delta_s} \right)} \ln \left( \frac{R}{r} \right)
\]

The heat flux calculated at the wall, combining Eq. (31) with Fourier’s law, becomes:

\[
q(R) = -k \frac{dT}{dr} = -\frac{(T_{cv} - T_{\text{wall}})k}{\ln \left( 1 - \frac{\delta_s}{R} \right)}
\]

Finally, using the Taylor expansion of the logarithm for \(\delta_s \ll R\), Eq. (32) can be approximated and rearranged as follows:

\[
\delta_s \approx k \frac{q(R)}{(T_{cv} - T_{\text{wall}})}
\]

Eq. (33) says that, for a given value of heat flux at the wall, solid thickness varies linearly with the temperature at the wall (as well as with the temperature of cooling water), which is what is shown in Fig. 6 (right). The slope of the line is equal to the ratio between slag thermal conductivity and local value of heat flux at the wall.

This study proves that cooling water temperature can be increased to limit thickness of the slag layer without affecting thermal losses in the reactor. On the other hand, this parameter may be set considering other constraints in the plant. If the cooling membrane is used for steam generation, this temperature is basically determined by design steam pressure.

3.3. Sensitivity to slag flowrate

A third sensitivity analysis was carried out, changing the flowrate of slag deposited at the wall. Again, the height averaged and maximum thickness of solid and liquid layers were calculated at steady state (see Fig. 8).

The thickness of both solid and liquid slag increases with an increase of the ash deposition rate. The relative change is similar for the two phases, which confirms what was already shown by Ye et al. [16], which is that model sensitivity is higher at low flowrates. For \(m_s > 10,000\) kg/h, the curves become linear. When increasing slag deposition rate ten times, from 2000 kg/h to 20,000 kg/h, the average thickness of the solid layer changes from 13 mm to 22 mm, while that of the liquid is doubled, from 2.6 to 5.2 mm. The thickness of the liquid increases with the slag deposition rate because the temperature profile, and consequently slag viscosity and velocity, do not change significantly. As a consequence, slag flow area must be greater if the slag flowrate is higher.

The difference between the curve of maximum thickness and that of height averaged thickness gets more important at higher...
slag flowrates. This happens because more local ash deposition locally leads to a greater local increase of the layers’ thickness moving from top to bottom of reactor. Ultimately, the variance of thickness distribution is more significant.

The reactor cooling rate calculated for different slag deposition rates is shown in Fig. 9. The extracted heat decreases for higher slag flowrates. This is a consequence of the thicker solid layer at the walls, which acts as insulation and so increases the overall thermal resistance at the wall. As opposed to previous sensitivity analyses, in this case wall temperature is the same for all simulations. According to Eq. (32), if $T_{cw} - T_{w}$ is constant, heat flux at the wall must be inversely proportional to the thickness of the solid layer, which is why Figs. 8 and 9 are symmetrical.

These results show that the flowrate of ash in the reactor can influence slag accumulation at the walls, but at a lower degree than that of the difference between gas temperature and slag critical temperature shown in the first sensitivity analysis.

3.4. Simulations with changing gas temperature

Results presented in the previous sections prove that the amount of heat extracted at the gasifier wall can be important in multiple cases. This means that the insulation effect provided by the slag deposited at the walls may not be good enough to avoid thermal dissipation and gas cooling. The thermal resistance provided by the slag basically depends on two factors: thermal conductivity of slag, and thickness of the layer. The former is basically determined by the chemical composition of the ashes originally found in the feedstock. The latter depends partly on operating conditions, but most of all it is the difference between gas temperature and slag critical temperature that determines material viscosity. The lower this temperature difference, the thicker the layer.

One possible way to decrease heat losses is decreasing the heat transfer coefficient at the wall, which can be done by increasing the thickness of the refractory layer. This may not be an optimal choice because of the previously mentioned degradability of the refractory material in contact with slag.

If the heat extracted is not negligible, the gas temperature will not be constant along the axial coordinate, but will be affected by heat transfer. This can have an influence on the slag deposition itself (if temperature is lower, viscosity is higher and thickness increases). To include this effect, the model was modified including an energy balance for the gas phase, as follows:

$$\dot{m}_g C_{pg} \frac{dT_g}{dz} = -2 \pi (R - \delta) (q_{out} + q_{cw} + q_{dep})$$  \hspace{1cm} (34)

In this equation, the effect of reactions in the gas phase was neglected, so it was assumed that the inlet temperature represents the adiabatic reaction temperature in the gas system. An even more accurate evaluation of gas temperature can be obtained by adding the kinetic part to Eq. (34), together with a set of species equations. This is specific to the reacting system in the gasifier and the description of this part goes beyond the purpose of this work.

The base case considered in Section 3 was simulated again with the modified model. Gas flowrate $\dot{m}_g$ was calculated from slag flowrate assuming that gas represents 90% of the total mass entering the gasifier. Fig. 10 compares the results obtained with

![Fig. 9.](image)


![Fig. 10.](image)

the base model and the modified one in terms of temperature profile of gas and slag liquid surface, as a function of the axial coordinate. As expected, gas cooling due to heat transfer decreases the temperature of the liquid slag as well. This has an influence on calculated slag thickness, which is higher with the modified model, as shown in Fig. 10 (right). The temperature profile of the liquid-gas interface obtained with the new model shows a maximum at about 4.5 m. This can be explained with the combined effect of slag cooling at the wall, and heat flux coming from the gas phase, which is lower at the bottom.

For the considered base case, the temperature decrease of the gas phase is ~30°C at the outlet. This parameter is important because it quantifies the refractory performance of the slag. This value can change depending on the gas flowrate in the reactor. A sensitivity study was carried out, changing the relative amount of gas fed to the system and repeating the simulation with the modified model. In this case, the flow rate of gas was changed, whereas the one of slag was always kept equal to the one of the first base case. Fig. 11 shows the obtained temperature profiles of gas and liquid.

As expected, the modified model becomes similar to the original one when the gas flowrate is large, as compared to the slag flow. In such cases, gas temperature can be assumed to be constant. The effect of gas cooling on thickness of the liquid and solid layers is shown in Fig. 12. The average thickness of the solid layer can increase from 22 mm to 27 mm when the relative amount of ashes changes from 1 % to 15 % of total inlet flowrate. This is due to the increased viscosity of the slag at lower temperatures, enhancing wall accumulation. Thickness of the liquid layer is not influenced by this parameter and has a constant value of 5 mm for all simulations.

4. Conclusions

A model to describe ash accumulation and flow along the reactor walls of an industrial gasifier was developed. The model includes the numerical solution of the momentum, energy, and mass balances, and can describe both solid and liquid phases. A proper slag viscosity model was used to simulate the transition between the two phases. Some sensitivity analyses were carried out to assess the influence of the main operating variables on slag accumulation.

From this study, it can be concluded that the difference between gas temperature and slag critical temperature is the key parameter that affects slag accumulation and layer growth, because it heavily influences slag viscosity. When this temperature difference is small, control of layer growth can become challenging. This is something to consider, especially if the source of ashes is quite heterogeneous, for instance if biomass is used as a feedstock. Homogenization of the feedstock becomes even more important for this reason, and solid additives can be an option when ash fusion temperature is too high. Operating the reactor with a thick layer of slag on the wall can be advantageous to avoid high thermal losses in the gas phase, at the cost of needing a bigger reactor volume. In this sense, the temperature of cooling water can be increased to decrease the solid holdup without affecting extracted heat.

The effect of gas cooling in the reactor was estimated, thanks to a model update that included a second energy balance. The modified model predicted a gas temperature drop that can exceed 30 °C if the ash content in the feedstock is higher than 10% of the total. This leads to an increase in solid layer thickness. This effect is usually neglected in the literature, but can become important if gas flowrate is not very high compared to slag deposition rate. The study proves that it is essential to correctly characterize slag properties (particularly its rheological behaviour) to correctly describe slag deposition in a particular system.