A One-Dimensional Flow Model of a Flashing Black Liquor Gun: Study of Vapor Generation Sub-Models

Järvinen, M. P.1*, Kankkunen, A. P. 1, Miikkulainen, P. H. 2 and Heikkilä, V.P. 3

1 Aalto University School of Science and Technology, Department of Energy technology, P.O. Box 14400, Sähkömiehentie 4, 00076 Aalto, Finland, mika.jarvinen@tkk.fi, ari.kankkunen@tkk.fi

2 Andritz Oy, Kyminlinnantie 6, 48601 Kotka, pasi.miikkulainen@andritz.com

3 Metso Power Oy, Lentokentänkatu 11, 33101 Tampere, Finland, ville-pekka.heikkila@metso.com

* Corresponding author

ABSTRACT
This paper presents a new computational flow model for a flashing black liquor gun. The primary motivation for this work was to have a tool to reliably calculate initial droplet data for CFD furnace combustion simulations of a recovery boiler. The model will also be actively used for industrial engineering and design of black liquor nozzle development. The model can also be used for other liquid fuels sprayed under flashing and non-flashing conditions. The model is based on the numerical solution of one-dimensional conservation equations of mass, momentum, energy, steam mass fraction and bubble number density. Mass-momentum coupling is solved by the SIMPLE method. Scalar equations are solved by the fully implicit control volume method. Two different vapor generation models were tested: the relaxation model and a more detailed vapor generation model based on a physical growth model of a single vapor bubble. The paper presents comparison data for real flashing conditions with industrial black liquor, validating also initial spray velocity of the droplets. The role of important parameters of the vapor generation model such as the bubble nucleus number density (1/m³) and initial nucleus size will be discussed.

Keywords: black liquor spraying, flashing, modeling

1. INTRODUCTION
Black liquor is a by-product of the chemical pulping. It is a highly viscous, high solid content liquid containing the inorganic cooking chemicals (e.g. NaOH, Na₂CO₃, Na₂S, Na₂S₂O₃, Na₂SO₄, NaCl) and the organic material (lignin, aliphatic acids, extractives) separated from the wood during the cooking process [1]. Black liquor is burned in recovery boilers for utilizing the energy and recovering the chemicals. The liquor is usually sprayed above its atmospheric boiling point by splash-plate nozzles, which produce volume median droplet size of 3-8 mm [2]. At the room temperature black liquor with a typical solid content 70-80 % is in a solid form. To be able to pump it, it has to be
heated up to 130-140 °C, which means that the atmospheric boiling point is exceeded by 12-20 °C. During the spraying, when liquor is rapidly depressurized as it flows out from the nozzle, this excess temperature introduces a noticeable potential for flash evaporation, Figure 1. It has been shown that the generation of vapor inside the nozzle tube may increase the initial velocity by a factor of 2-3 [3]. Initial velocity of the droplets affects the location of the combustion in recovery furnace. Flashing also effectively decreases the droplet size, increasing the rate of heat and mass transfer processes taking place during combustion.

Flash evaporation accelerates the flow and has a dramatic effect on the properties of the spray such as droplet size, shape and initial velocity [2, 3]. Considering also the effect of increase in velocity, these affect greatly the location and intensity of the combustion.

The classical paper on heterogeneous nucleation mechanism was presented by Soplenkov and Blinkov [4]. Their model is based on the assumption that at some superheat, flashing will take place on particles smaller than the critical value defined by surface tension and local superheat. A paper by Blinkov et al. [5] presented a model that considers both heterogeneous wall and bulk nucleation mechanisms. Based on the five equation model and the approach suggested by [4] for heterogeneous bulk nucleation, they found out that heterogeneous bulk nucleation is negligible in small geometries with large wall area to volume ratio. These results were for water. For black liquor, the liquid is filled with precipitated salt crystals that can act as nucleation sites. For example, the effect of heterogeneous bulk nucleation for water-particle suspensions was studied by Borkent et al. [6]. They found out that during the mixing the particles into the water the gas is entrapped into the small surface cracks and imperfections of the particles. Once removed by induced ultrasonic cavitation, their activity to act as nucleus disappeared. On the other hand, if we have a case wherein the particles are formed by precipitation, no air can be entrapped in the cavities. The role of crystal particles on the bubble nuclei formation must be therefore different. For species that has higher vapor pressure than water can be released before primary flashing and form the gas nuclei required for flashing. In addition to this, as black liquor exhibits non-Newtonian behavior, yield stress prevents small mixed gas bubbles from escaping the liquid. During the long evaporation process, different gases and vapors are formed and it is quite probable that some fraction of smallest bubbles are remained within the liquid, as free bubbles of as gas voids attached to salt crystal particles. One of the most important parameters of the models is the bubble
number density \( N_b \text{ (1/m}^3) \). Unfortunately, the number of active nuclei sites could not be directly determined. Therefore, it is used as a free variable that will be iteratively determined.

In our case here, the superheated and highly viscous black liquor flows out from the nozzle. The distance over which the flashing can take place and the time for vapor to generate are very short. However, as it has been experimentally shown, these conditions are sufficient for significant amounts of vapor to be released, approximate void fractions at the nozzle exit being 50-70 %.

The objective of this paper is to present a new flow simulation model for a flashing black liquor gun. In practice, the new model is tailored to be in active industrial engineering and design use in nozzle development and giving also reliable initial data for CFD furnace simulations. It will give also insight to processes taking place in nozzle flow that are not possible to measure yet.

2. Model description

The principles of the new nozzle flow model are briefly presented here. The main simplification of the flow was the use of 1-dimensional plug-flow model, see Figure 2. In this approach we dump all the effects of rheology and turbulence into an effective viscosity that is then used to estimate viscous pressure loss. Although the main flow field is only roughly approximated by this, it gives us a pretty good picture of the axial velocity and pressure distributions in the nozzle. Two different vapor generation models were implemented and compared: homogenous relaxation model HRM [7] and a more detailed vapor generation model based on the growth of a discrete vapor bubble BGM. HRM model has here only one unknown parameter, relaxation time. This was already obtained in our previous work [8] and had a value of \(~1\) s. Bubble growth based vapor generation model is much more complex to be used. It requires the initial nucleus density and nucleus size to be used. These both are defined and discussed in this paper. A detailed version of this bubble model was available and published prior to this work [9]. Based on the physical observations from this a new simplified growth model was developed and will also be presented here shortly.

2.1. Conservation Equations and Boundary Conditions

In this work the five equation model [5] was used to describe the system. This includes the conservation equations for mass (1), linear momentum (2), energy (3), mass of vapor (4) and bubble number density (5), all presented for a general discrete control volume \( V_i \) in the control volume format, see Figure 2.

\[
\dot{m}_{i-\frac{1}{2}} - \dot{m}_{i+\frac{1}{2}} = V_i \frac{\partial \rho_i}{\partial t} \tag{1}
\]

\[
\dot{m}_{i-\frac{1}{2}} u_{i-\frac{1}{2}} - \dot{m}_{i+\frac{1}{2}} u_{i+\frac{1}{2}} + p_{i+\frac{1}{2}} S_{i+\frac{1}{2}} - p_{i-\frac{1}{2}} S_{i-\frac{1}{2}} - p_i S_i - F_{\mu} - F_g = V_i \frac{\partial (u_i \rho_i)}{\partial t} \tag{2}
\]
The Swedish and Finnish National Committees of the International Flame Research Foundation – IFRF

\[ \dot{m}_{i,j} h_{i,j} - \dot{m}_{i+1,j} h_{i+1,j} + \Phi_i A_i = V_i \frac{\partial (e_i \rho_i)}{\partial t} = V_i \frac{\partial (h_i \rho_i - p_i)}{\partial t} \]  

\[ \dot{m}_{i,j} y_{i,j} - \dot{m}_{i+1,j} y_{i+1,j} + \Gamma_i V_i = V_i \frac{\partial (y_i \rho_i)}{\partial t} \]  

\[ \dot{V}_j N_{b,j} - \dot{V}_{j+1} N_{b,j+1} + \dot{N}_v V_j = V_j \frac{\partial N_{b,j}}{\partial t} \]  

Where \( S_i = 0.5 \left( S_{i,j} + S_{i,j+1} \right) \) is the area that average wall pressure effects to, \( F_\mu = \xi_i \frac{S_i \Delta x_i \rho_i u^2}{d_i} \) is the friction force and \( F_g = V_i \rho_i g \sin(\alpha) \) is the gravity force, \( \dot{m} \) is the total mass flow rate, \( \rho \) is the density and \( t \) equals time. \( u \) is the plug flow velocity, \( \rho \) is absolute static pressure and \( S \) is the cross sectional surface area of the computational cell faces. \( h \) is the total enthalpy, \( \Phi_i \) is the wall heat flux from furnace radiation or cooling to environment, \( A_i \) is the wall surface area of the computational cell and \( e_i \) is the total specific energy \( e = h - pv + \frac{1}{2} u^2 + gh \). \( y \) is the mass fraction of steam and \( \Gamma \) is the volumetric mass source term. \( \dot{V} \) is the total volume flow rate, \( N_b \) is the bubble number density and \( \dot{N}_v \) is the volumetric bubble nucleation rate. \( d \) is pipe inner diameter, \( \Delta x \) is the axial length of the cell, \( \xi \) is the viscous friction factor and \( \alpha \) is the pipe inclination angle from horizontal level (+ direction upwards). Friction factors for the pipe flow were calculated from

\[ \xi = \frac{64}{\text{Re}}, \text{ if } \text{Re} < 2300 [10], \]  

\[ \xi = \frac{1.325}{\left( \frac{\ln \left( \frac{\varepsilon}{D} + 5.74 \frac{\text{Re}^{0.8}}{3.7} \right)}{\text{Re}^{0.5}} \right)^2}, \text{ if } 5000 < \text{Re} < 10^8, 0.000001 < \varepsilon / D < 0.01 [10], \text{ and} \]  

\[ \xi = 0.118 \frac{\text{Re}^{0.017}}{\varepsilon} \text{, for a flexible metal hose used in recovery boiler spraying systems} \]  

To close this system of equations, physical and thermo-dynamical properties and an equation for the vapor mass source term are required. Numerical solution of the all conservation equations was carried out with the fully implicit finite volume method [11]. The fluxes of all scalar variables i.e. \( x = \{ y, h, N_b \} \), were calculated using the face values obtained from the MUSCL equation [12]. Generally, minimum requirement is the 2nd order upwind scheme [11]. Van Albada TVD limiter was used to prevent non-physical oscillation at discontinuity locations [12]. All time integration was carried out by a 2nd order accurate method. The resulting algebraic tri-diagonal system was then solved with the Newton-like predictor-corrector method and the Thomas algorithm [12]. The SIMPLE pressure correction method was used to solve the momentum equation together with the continuity equation [11]. This method was tailored for an incompressible two-phase flow by considering differentiates of density with respect to pressure.
2.2. Vapor source models

One very simple and broadly used model for vapor generation is the relaxation model [7]. This model was also used here as the first option to calculate the vapor generation rate. The relaxation model is based on the heuristic but physically sound assumption that the rate is proportional to the difference between local equilibrium quality and local quality divided by the relaxation time constant \( \Theta \).

\[
\Gamma = \frac{(y_{eq} - y)}{\Theta}
\]

\( y_{eq} = c_P(T_L - T_b(p)/l_v \) is the equilibrium steam mass fraction at a local pressure \( p \). This model has been previously used for water but here it was applied here for black liquor. Relaxation time was assumed constant, leaving only one free variable. In reality, relaxation time is a complex function of local pressure, vapor pressure, void fraction etc. but not data was available to develop a correlation to denote these. An approximate value \( \Theta = 1 \) was obtained iteratively. This model gives a very robust way of defining the vapor source term. The model behaves very stably in numerical iteration as the differential of the vapor generation rate is always negative, increasing the value of diagonal of the Jacobian matrix.

A more physical approach to vapor generation rate can be obtained by considering the growth of single vapor bubbles within the flow. Based on our previous work with a detailed bubble growth model for viscous liquids, initial stages of the bubble growth are controlled by surface tension and viscous forces [9]. After these initial stages, bubble growth becomes limited by external heat transfer to bubble surface. Our simplified model should consider all these. Our starting point is here the Rayleigh-Plesset equation for a Newtonian liquid. In our future work, non-Newtonian characteristics will be considered.

\[
\rho_l R \ddot{R} + \rho_g \frac{3}{2} R^2 \dot{R}^2 = p_s(T) - p_v - \frac{2\sigma}{R} - \frac{4\mu \dot{R}}{R}
\]

The left hand side terms describe the inertia. Right hand side is the driving potential i.e. the pressure difference between the gas and bulk reduced by the surface tension and viscous forces. Initial stages of bubble growth could be solved directly from this as the bubble surface temperature is initially equal to liquid temperature \( T_L \). However, after a short period of time, the surface temperature drops and finally reaches the local saturation temperature in most cases. Eventually, to solve the bubble growth rate from (10), surface temperature at different times is required. This is typically obtained from the numerical solution of the detailed energy conservation equation using a dense radial computational grid in the surrounding liquid, such as in our previous work [9]. Analytical methods are also available but their use is limited typically in the cases on constant boundary conditions, which is not the case in nozzle flow. Our approach was to use a simplified energy equation to link it to equation (10). The objective is to express the driving
pressure potential with corresponding temperature difference that can be then linked to heat transfer rate. This can be obtained from the Clausius-Clapeyron equation [13].

\[ p_\lambda(T_s) - p_\infty = \frac{\partial p_L}{\partial T}(T_s - T_b(p_\infty)) = \frac{\partial p_L}{\partial T}(T_L - T_b(p_\infty) - (T_L - T_s)) \]  

(11)

Here, we have written the surface superheat as a combination of the local excess temperature \( T_L - T_b(p_\infty) \) and the temperature difference between the bulk liquid and the bubble surface \( T_s \) i.e. the driving potential for heat transfer. The former is not dependent on the bubble growth rate. The latter is directly related to vapor generation rate via the heat transfer rate. To get this, we first write the overall energy balance for the bubble using a heat transfer coefficient \( \alpha \) that approximates all heat transfer mechanisms in the liquid boundary layer.

\[ \alpha(T_L - T_s) = \hat{R} \rho_G l_v + \frac{R}{3} \frac{dT}{dt} \left( \frac{d\rho_G}{dT} l_v + \rho_G c_p G \right) \]  

(12)

The second term on the right is very small compared with the evaporation term, especially after the initial stages if the surface temperature has dropped to constant value \( T_b(p_\infty) \), it is neglected. The heat transfer coefficient could be solved from a detailed energy conservation equation, as in our previous work. To simplify, the transient heat transfer coefficient was approximated by the correlation presented by [14].

\[ \alpha = \frac{\lambda}{R} \left( 1 + \frac{R}{\sqrt{\pi \alpha_t l}} \right) \]  

(13)

This expression is applicable in the non-convective case \( Re = 0 \). It does not take into account the effect material transport to boundary layer as the bubble grows, but is sufficiently accurate for our purposes here. Combining (11) and (12) gives us

\[ p_\lambda(T_s) - p_\infty = \frac{\partial p_L}{\partial T} \left( T_L - T_b(p_\infty) - \frac{\hat{R} \rho_G l_v}{\alpha} \right) \]  

(14)

This is then implemented into (10) to give

\[ \rho_l \ddot{R} + \rho_l \frac{3}{2} \dot{R}^2 = \frac{\partial p}{\partial T} \left( T_L - T_b(p_\infty) - \frac{\hat{R} \rho_G l_v}{\alpha} \right) - 2\sigma \frac{2\mu \dot{R}}{R} \]  

(15)

Based our previous work, the first term on the left is not important in the application studied here, so it is omitted. In addition, we collect terms and move them all to left; following quadratic equation is then obtained for \( \dot{R} \).

-6-
\[ \frac{1}{2} \dot{R}^2 + \left( \frac{\partial p}{\partial T} \frac{I_v}{3\alpha \rho_L} + \frac{4\mu}{3\rho_L R} \right) \dot{R} + \frac{2\sigma}{3\rho_L R} \left( T_L - T_b(p_\infty) \right) = 0 \] (16)

This equation is interesting. It directly links the bubble growth rate to local excess temperature, \( T_L - T_b(p_\infty) \). From this the growth rate and the instantaneous surface temperature can be solved to give.

\[
\dot{R} = \sqrt{\frac{\left( \frac{\partial p}{\partial T} \frac{I_v}{3\alpha \rho_L} + \frac{4\mu}{3\rho_L R} \right)^2}{\left( \frac{\partial p}{\partial T} \frac{I_v}{3\alpha \rho_L} + \frac{4\mu}{3\rho_L R} \right) + \frac{2\sigma}{3\rho_L R} \left( T_L - T_b(p_\infty) \right)}}
\]

\[ T_s = T_L - \frac{\dot{R} \rho_G l_v}{\alpha} \] (17)

This first order non-linear differential equation can be integrated with 4th order Runge-Kutta method for obtaining sufficient accuracy with maximal time step for computational economy. In order for a bubble to start growing, the driving pressure difference \( \Delta p \) has to be positive. This condition can be solved from equation (10) by setting pressure difference > 0 and growing rate equal to zero.

\[ R_{\text{min}} > \frac{2\sigma}{\frac{\partial p}{\partial T}(T_\infty - T_b(p_\infty))} \] (19)

Within the flow, atmospheric pressure \( p_\infty \) is continuously decreasing, which means that use of the equation (19) is questionable as such. Our approach was to assume an initial size of a vapor nucleus \( R_n \). When the radius \( R_{\text{min}} \), obtained from equation (19) exceeds this value, bubble growth can begin. The overall vapor generation rate can be calculated from the obtained growth history of a single bubble as follows.

\[
\Gamma_i = \frac{\pi}{6V_i} \left( \dot{V}_{i_{-1/2}N_{i_{-3/2}R_{i_{-3/2}}}} \dot{V}_{i_{-1/2}N_{i_{-3/2}R_{i_{-3/2}}}} \dot{R}_{i_{-1/2}}^3 - \dot{V}_{i_{-1/2}N_{i_{-3/2}R_{i_{-3/2}}}} \dot{R}_{i_{-1/2}}^3 \right) = \frac{\pi \dot{V}_0 N_{i_{-3/2}R_{i_{-3/2}}}}{6V_i} \left( R_{i_{-1/2}}^3 - R_{i_{-1/2}}^3 \right) \] (20)

The latter form is obtained by assuming that the local nucleation rate is zero and all bubble nuclei are already present in the incoming flow and related to salt crystals or micro-bubbles. Initial nucleus density is assumed also constant at this point and is used as a free variable that is to be fitted with experiments.
3. RESULTS

At first, we validate the overall flow model with water data under non-flashing conditions. After this, the overall two-phase flow model is compared with flashing black liquor experiments [2, 3]. Finally, some results demonstrating the capabilities of the new model with black liquor under flashing conditions will be shown and discussed.

3.1. Model validation of the flow model with water experiments

To validate the overall flow model, we first carried out a simple test wherein the firing pressure was first increased and then decreased, Figure 3. Although being very simple test, it gives a very good idea on how the model equations work. Before any attempts are done to modeling the two-phase flow this stage is required to be on solid basis.

![Figure 3](attachment:figure3.png)

**Figure 3. Validation of the mass flow rate – pressure characteristic.** ○ experimental points, — model prediction, 25 °C water, 28 mm splash-plate nozzle.

As Figure 3 shows, the correlation with the experimental data is excellent. These simulations did not contain any free variable that could be varied. Of course, friction factor correlations were used as such. In the following, models capabilities are tested with black liquor, also under flashing conditions.

3.2. Validation of the model with black liquor experiments

When comparing the model and the experiments, it needs to be realized that the experimental value of velocity is measured at the spray centerline. In our previous work it has been shown that every nozzle has a typical velocity profile. However, typically in a flashing case the sheet velocity is constant at all directions. Figs. 4 and 5 present the absolute velocity and dimensionless velocity, respectively for the relaxation model. Figs. 6 and 7 present the same results with BGM. Bubble number density \( N_b = 1 \times 10^9 \text{ m}^{-3} \) was obtained iteratively by minimizing the error between experiments.
As Figs. 4 and 6 shows, both models predict a minimum value for the absolute velocity as function of mass flux. Up to a certain point, increased mass flow rate decreases flashing as the point at where the flashing initiates moves towards the exit of the nozzle. When mass flow rate further increased form this, the effect of increasing mass flow rate to flashing diminishes and finally, we reach the region where is no flashing and then, velocity starts to increase linearly and follows the line, $u = \frac{m^*}{P_{bl}}$, shown also in Fig. 4 and 6. HRM model gives a sharp minimum for the velocity, Fig. 4. This trend can not be seen in the experiments. With the BGM there is also a minimum, but the shape of the curve is much closer to experiments. Figs. 5 and 7 show the model predictions for dimensionless velocity. Dimensionless velocity is defined as the actual velocity at the outlet/sheet centerline divided by the velocity based on the mass flow rate and liquid.
density. In other words, it’s the ratio of liquid density and apparent density at the outlet. In the non-flashing case, dimensionless velocity has a typically a value 1-1.5, depending on the geometry of the nozzle. Here we used a value 1 for dimensionless velocity for non-flashing case. As Figs. 6 and 7 shows, with this value, the correlation between model and experiments is good.

### 3.3. Controlling the degree of flashing with nozzle design

There are many different nozzle designs already available. One option is to have a splash-plate nozzle with a fully spherical nozzle exit at the pipe end. Another widely used possibility is to cut a segment off the outlet with the splash-plate and to reduce the exit area this way. Here, we study the effect of the nozzle exit geometry by varying discharge area ratio from 1 to 0.4 (60% smaller area). The diameter of the main tube, mass flow rate and other spraying parameters are held constant.

![Figure 8](image1.png)  ![Figure 9](image2.png)

**Figure 8.** The effect of outlet contraction of exit velocity and flashing, dashed line presents the velocity for non-flashing case, 5 kg/s, 28 mm main tube diameter, $\Delta T=14$ °C, HRM.

**Figure 9.** The effect of outlet contraction of exit velocity and flashing, dashed line presents the velocity for non-flashing case, 5 kg/s, 28 mm main tube diameter, $\Delta T=14$ °C, BGM.

As can be seen from Figures 8 and 9, decreasing the exit area has only very small effect of the absolute velocity as this simultaneously decreases flashing. Absolute values for both models are very close to each other. There is a local minimum predicted by both models. For smaller contractions than 0.5 there is practically no flashing taking place inside the nozzle. After this, the velocity increases according to the one phase flow characteristics (dashed line). Another case studied was the same situations with a constant pressure of 0.9 bars. These results are shown in Figs. 10 and 11.
As can be seen, with a constant pressure decreasing the exit area has no significant effect on the absolute velocity. As area decreases, the effect flashing decreases. Although no directly shown, mass flow rate naturally decreases too. For example with RHM, with contraction factor value of 1 mass flow rate was 5 kg/s. With the value 0.4, mass flow rate was only 3.5 kg/s. In our future work, experiments will be carried out to verify these results.

3.3. Contribution analysis of rate limiting mechanisms in bubble growth

Based on our previous work with the detailed bubble growth model it was shown that under constant liquid pressure, the early stages of bubble growth are controlled by surface tension and viscous forces [9]. The majority of the growth history was controlled by external heat transfer to bubble surface. Here, the controlling mechanisms under rapidly changing nozzle flow are studied. Two different cases are calculated: a reference case with normal black liquor viscosity and a case with a 10 times higher viscosity value.
As Fig. 12 shows, the first moments with the small bubble size of bubble growth are controlled by the surface tension forces. After this, the rate becomes to be controlled by external heat transfer rate. With an increased viscosity, Fig. 13, after the initial surface tension controlled bubble growth start-up, viscous forces play a small role for a short while but after this, growth is controlled by heat transfer. In addition, with a larger viscosity the friction pressure loss is greater and therefore the location of flash initiation moves towards the exit.

4. SUMMARY AND CONCLUSIONS

This paper presented a new computational flow model for a flashing black liquor gun. The model was based on the numerical solution of one-dimensional conservation equations of mass, momentum, energy and steam mass fraction. Mass-momentum coupling was solved by the SIMPLE method. Scalar equations were solved by the fully implicit control volume method. Two different vapor generation models were implemented and tested: relaxation model and vapor growth based model. The model was also successfully validated with water and black liquor experiments. Importantly, these simulations did not contain any free variable that could be varied. When validating the model with flashing black liquor, it succeeded to predict experimental results well. Especially with the bubble growth model the results were in good agreement with experiments.

A new simplified bubble growth model was also developed and presented. The model was based on the physically relevant mechanisms isolated by a detailed model, developed in our previous work. The new model considered inertia, surface tension, viscous forces and the external heat transfer to bubble surface. It is important to include all these, as the growth mechanism varies at different stages of bubble growth. In addition, the environment of the bubble is continuously changing in the nozzle. The bubble number
density was iteratively determined and the value $1 \times 10^9$ $1/m^3$ gave best results. The typical value obtained in studies on black liquor evaporation has been much higher [9, 15], typical values have been in the order of $10^{10}$...$10^{13}$ $1/m^3$. In our future work, this difference will be studied. In addition, the non-Newtonian behavior and the yield stress that initially restricts the growth rate will be considered in the model.

Generally, the model gives a very broad picture on the phenomena that takes place inside the nozzle. As an output, we get information of the complex processes taking place inside the nozzle tube. The model gives axial profiles of pressure, velocity, void fraction, bubble number density, bubble size and all the physical properties of the fluids. The flow model is tailored to be in active industrial engineering and design use in nozzle development and giving also reliable initial data for CFD furnace simulations. It will give also insight to processes taking place in nozzle flow that are not possible to measure yet.

5. REFERENCES


6. NOMENCLATURE

\[ \dot{m} \] mass flow rate, kg/s
\[ n^* \] mass flux, kg/m²s
\[ N_b^n \] bubble number density, 1/m³
\[ A \] wall area, m²
\[ \alpha_T \] thermal diffusivity, m²/s
\[ e \] energy, J/kg
\[ F \] force, N
\[ G \] acceleration for gravity, m/s²
\[ h \] enthalpy, J/kg
\[ l \] latent heat, J/kg
\[ p \] pressure, Pa
\[ R \] radius, m
\[ Re \] Reynold number, -
\[ S \] area, m²
\[ t \] time, sec
\[ T \] temperature, K
\[ u \] velocity, m/s
\[ V \] volume, m³
\[ y \] mass fraction, -
\[ \sigma \] surface tension N/m
\[ \Phi^* \] heat flux, W/m²
\[ \alpha \] pipe declination, °
\[ \lambda \] thermal conductivity, W/mK
\[ \rho \] mass concentration, kg/m³
\[ \xi \] friction factor, -
\[ \Gamma \] vapor generation rate, kg/m³s
\[ \theta \] relaxation time, s

Sub-scripts

\[ \infty \] bulk
\[ 0 \] reference state
\[ A \] water
\[ b \] bubble, boiling
\[ eq \] equilibrium
\[ g \] gravity
\[ G \] gas
\[ L \] liquid
\[ s \] surface

Greek

\[ \mu \] absolute viscosity, Pas

7. ACKNOWLEDGEMENTS

The support provided to this work by TEKES (National Technology Agency of Finland) and their National Research Programme MASI/INTER and TEKES/Innovis-project, the Andritz Corporation and Metso Power Corporation is gratefully acknowledged.