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Nozzle design influence on particle attrition by a supersonic steam jet

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ABSTRACT

This paper presents an extension of a mathematical model for particle attrition inside a fluidized bed by a supersonic air jet and its application to optimize the nozzle design. A new method to calculate grinding efficiency is presented. Also, heat transfer is included in the model because of the large interfacial temperature difference. Numerical simulations are conducted to investigate various nozzle designs, i.e. a range of area ratios (indicative of the jet being over- or under-expanded) and nozzle expansion angles, and different bed fluidization velocities. It is found that the perfectly expanded nozzle (the exit pressure equal to the outside pressure) provides better attrition performance than over- and under-expanded jets. The nozzle expansion angle also has an influence on the grinding efficiency: narrow angled nozzles have higher grinding efficiency. In addition, the analysis of various bed fluidization velocities indicates that increasing the velocity results in a modest improvement of the grinding efficiency.

1. Introduction

In many chemical processes involving fluidized beds, particle size grows due to agglomeration, granulation and other processes. One of the methods to control particle size inside the reactor is to break them up into smaller ones. This is achieved by an array of supersonic nozzles that are located circumferentially in the lower part of the reactor (note that larger particles are segregated towards the bottom of the bed) and directed towards the reactor axis. Steam, supplied through these nozzles, accelerates particles entrained in the jet, which facilitates inter-particle collisions with high impact velocity. Such collisions lead to the effective reduction of the size of bed particles through particle breakage, called attrition. This process is limited by the amount of available steam; therefore, it is very important to use it with the highest efficiency by optimizing the nozzle design and placement.

The reactor environment, which quite often is characterized by extremely high temperatures and high pressures, may make experimental investigations at actual process conditions practically unfeasible. Instead, the experiments are usually conducted at room temperature and atmospheric pressure using air in place of steam. McMillan et al. [1,2] studied the influence of the nozzle design experimentally for scaled-down versions of the nozzle. They demonstrated that increased inlet pressure with a constant exit diameter and an increased exit diameter with constant inlet pressure resulted in better grinding efficiency, which was defined as the ratio of the increase of the particle surface area to the mass flow rate of supplied gas. In addition, it was shown that the larger fluidization velocities were also beneficial for particle attrition.

As actual process conditions are unattainable in experiments, the only remaining option is to model the attrition process either analytically or numerically. Recently, Pougatch et al. [3] proposed a mathematical model that simulates particle attrition by a supersonic gas jet. This model was applied to the results of the small scale experiments of sand particle attrition [1] and demonstrated a fair agreement in reproducing experimentally observed trends and grinding efficiency values.

In the present paper, an attrition model is used to determine the optimal area ratio of the expansion section of the convergent-divergent (Laval-type) nozzle to provide the best grinding efficiency and to investigate the influences of the nozzle expansion angle and the bed fluidization velocity.

2. Numerical model

The numerical model is based on an Eulerian–Eulerian approach and the Kinetic Theory of Granular Flow (KTGF) to close the equations for the particulate phase. The mean average particle diameter represents the local particle size distribution. To account for the reduction in the particle size during attrition, we allow spatial and temporal variations of the average particle diameter throughout the flow field. For the sake of brevity, as the model was already described in detail in [3], we present the equations here in a table form while emphasizing the additions and modifications that are necessary for an application to an industrial scale nozzle.

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Table 1 presents conservation equations for both phases. Eqs. (T1.1–T1.4) are ensemble averaged [4] continuity and momentum equations. In contrast to small scale experiments, in the industrial reactors there are significant variations in temperature between the incoming steam and the fluidized bed particles. Therefore, an assumption of a constant total enthalpy used in [3] instead of the full energy conservation equation is no longer applicable. Full energy equations for both phases (Eqs. (T1.5, T1.6)) obtained with a similar averaging procedure are implemented into the model instead. In writing the equations we assumed that the heat capacity c_p is constant for each of the phases. While it is clearly the case for the particulate phase, the gaseous phase, steam, does exhibit some variations of this parameter. However, these variations for the temperatures encountered in the reactor ($200 \div 550\,^{\circ}C$) are in the range of 10%. As this value falls within a margin of the overall modeling errors, our assumption is acceptable. The multiphase version of k-ε turbulence model for the primary phase including a compressibility correction [5] and the interfacial energy exchange (Eqs. (T1.7, T1.8)) is utilized in the model. Eq. (T1.9) represents the conservation of the granular temperature, which is a measure of the fluctuating energy of the particulate phase. Finally, the spatial variation of the particle sizes due to break up and convection are described with a particle number density approach (Eq. (T1.10)). This approach requires an assumption that the local particle size distribution can be represented by the local average diameter. The particle number density, which is the number of solid particles per unit volume, can be determined from the particle diameter and volume fraction, $n = 6\alpha_s/\pi d^3$.

In order to close the equations listed in Table 1, a number of constitutive equations are required. These equations are presented in Table 2. As already mentioned, superheated steam behavior deviates from an ideal gas equation of state. However, we still employ an ideal gas equation (Eq. (T2.1)) because these deviations are relatively minor when compared to the overall model uncertainty arising from the very complex nature of the flow to be modeled. The drag model of Gidaspow et al. [6] is implemented to calculate the interfacial drag coefficient (Eq. (T2.2)). The approach for closing energy equations is adopted from Schmidt and Renz [7]. The interfacial heat transfer coefficient is obtained through the Nusselt number (Nu) correlation proposed by Gunn [8] and presented in Eq. (T2.3). The gas effective thermal conductivity expression (Eq. (T2.4)) is taken from Kuipers et al. [9]. For the solid phase, the relation obtained by Hunt [10] (Eq. (T2.5)) connects the solid effective thermal conductivity to the random movement of particles. KTGF described in [11–13] is utilized to evaluate the diffusion coefficient and the dissipation in the granular temperature equation (Eqs. (T2.6, T2.7)). It is also used to calculate the solid pressure (Eq. (T2.8)) and the shear and bulk viscosities (Eqs. (T2.9, T2.10)). An additional frictional viscosity is applicable when the values of solid volume fraction are high ($\alpha_s > 0.5$), and it is evaluated by Eq. (T2.11) following [14]. Ma and Ahmadi's [15] correlation for the radial distribution function (Eq. (T2.12)) is adopted as it is applicable for a wide range of solid volume fractions. Instead of solving a differential equation for the gas–solid turbulence correlation, we used a simplified approach proposed by Simonin and He [16] to relate it to the granular temperature (Eq. (T2.13)). Finally, the particle breakage frequency needed in Eq. (T1.10) is calculated by Eq. (T2.14) according to our model [3]. It was shown in [3] and also can be inferred from an attrition propensity parameter proposed by Ghadiri et al. [17] that the breakage frequency C_{br} is an empirical parameter that depends on the material of the particulate phase.

In all computations presented in this paper we utilized the code that was previously developed by Nowak and Salcudean [18] and

Table 1
Conservation equations.

Gas continuity equation (T1.1)

$$\frac{\partial}{\partial t}\alpha_g\rho_g + \nabla\cdot\alpha_g\rho_g\mathbf{V}_g = 0$$

Solid continuity equation (T1.2)

$$\frac{\partial}{\partial t}\alpha_s\rho_s + \nabla\cdot\alpha_s\rho_s\mathbf{V}_s = 0$$

Gas momentum equation (T1.3)

$$\frac{\partial}{\partial t}\alpha_g\rho_g\mathbf{V}_g + \nabla\cdot\alpha_g\rho_g\mathbf{V}_g\mathbf{V}_g = \nabla\cdot\boldsymbol{\tau}_g - \alpha_g\nabla P + \rho_g\mathbf{g} + \beta(\mathbf{V}_s - \mathbf{V}_g)$$

where $\boldsymbol{\tau}_g = \alpha_g\left(\mu_g + \mu_g^t\right)\left(\left(\nabla\mathbf{V}_g + \nabla\mathbf{V}_g^T\right) - \frac{2}{3}\mathbf{I}\nabla\mathbf{V}_g\right) - \frac{2}{3}\alpha_g\rho_g k_g\mathbf{I}$

Solid momentum equation (T1.4)

$$\frac{\partial}{\partial t}\alpha_s\rho_s\mathbf{V}_s + \nabla\cdot\alpha_s\rho_s\mathbf{V}_s\mathbf{V}_s = \nabla\cdot\boldsymbol{\tau}_s - \alpha_s\nabla P - \nabla P_s + \rho_s\mathbf{g} + \beta(\mathbf{V}_g - \mathbf{V}_s)$$

where $\boldsymbol{\tau}_s = \alpha_s\left(\mu_s\left(\nabla\mathbf{V}_s + \nabla\mathbf{V}_s^T\right) + \left(\lambda_s - \frac{2}{3}\mu_s\right)\mathbf{I}\nabla\mathbf{V}_s\right)$

Gas enthalpy equation (T1.5)

$$\frac{\partial}{\partial t}\alpha_g\rho_g H_g + \nabla\cdot\alpha_g\rho_g\mathbf{V}_g H_g = \alpha_g\frac{\partial p}{\partial t} - \nabla\cdot\kappa_g\nabla T_g + h(T_s - T_g) + \boldsymbol{\tau}_g : \nabla\mathbf{V}_g$$

where $H_g = c_{pg}T_g + \frac{\mathbf{V}_g^2}{2} + k$

Solid enthalpy equation (T1.6)

$$\frac{\partial}{\partial t}\alpha_s\rho_s H_s + \nabla\cdot\alpha_s\rho_s\mathbf{V}_s H_s = \alpha_s\frac{\partial p}{\partial t} - \nabla\cdot\kappa_s\nabla T_s + h(T_g - T_s)$$

where $H_s = c_{ps}T_s$

Turbulent kinetic energy equation (T1.7)

$$\frac{\partial}{\partial t}\alpha_g\rho_g k + \nabla\cdot\alpha_g\rho_g\mathbf{V}_g k = \nabla\cdot\left(\mu_g + \frac{\mu_g^t}{\sigma_k}\right)\nabla k + \boldsymbol{\tau}_g : \nabla\mathbf{V}_g - \alpha_g\rho_g\varepsilon - \beta(2k - q_{gs}) - 2\alpha_g\rho_g\varepsilon\frac{k}{a^2}$$

where $\mu_g^t = C_\mu\rho_g\frac{k^2}{\left(1 + \frac{2k}{a^2}\right)\varepsilon}$

Turbulent dissipation equation (T1.8)

$$\frac{\partial}{\partial t}\alpha_g\rho_g\varepsilon + \nabla\cdot\alpha_g\rho_g\mathbf{V}_g\varepsilon = \nabla\cdot\left(\mu_g + \frac{\mu_g^t}{\sigma_\varepsilon}\right)\nabla\varepsilon + \frac{\varepsilon}{k}\left(C_{1\varepsilon}\boldsymbol{\tau}_g : \nabla\mathbf{V}_g - C_{2\varepsilon}\alpha_g\rho_g\varepsilon - C_{3\varepsilon}\beta(2k - q_{gs})\right)$$

Granular temperature equation (T1.9)

$$\frac{3}{2}\left(\frac{\partial}{\partial t}\alpha_s\rho_s\theta + \nabla\cdot\alpha_s\rho_s\theta\mathbf{V}_s\right) = (-P_s\mathbf{I} + \boldsymbol{\tau}_s) : \nabla\mathbf{V}_s + \nabla\cdot(k_\theta\nabla\theta) - \gamma + \beta q_{gs} - 3\beta\theta$$

Particle number density equation (T1.10)

$$\frac{\partial n}{\partial t} + \nabla\cdot n\mathbf{V}_s = nf_{br}$$