Numerical and experimental investigation of the flow behavior of sand particles in core shooting process

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Abstract: The flow behavior of sand particles during the core shooting process has been studied by combination of computational fluid dynamics (CFD) simulations and experiments. In the CFD simulations, a two-fluid model (TFM), in which a kinetic-frictional constitutive correlation is incorporated, has been applied to describe the flow dynamics of core shooting process. The influences of turbulence model and boundary conditions on the modeling of core shooting process are further investigated. The simulation results indicate that there are certain sensitivities to the wall boundary condition in the sand flow behavior while no significant difference is observed between the results of the laminar and turbulence models. Using high-speed photography system and pressure measuring system, systematic experiments have been carried out to fully validate the model and good agreement is obtained between simulations and experiments. Based on the simulation and experimental results, the flow behavior of sand particles in the core box is analyzed, and the effects of nozzle dimension ($D=6, 8, 10, 15\text{mm}$) and shooting pressure ($P_{in}=0.3, 0.4, 0.5\text{MPa}$) on the hydrodynamic properties such as sand particle mass flux, sand volume fraction at the nozzle and distribution of sand velocity are discussed.

Keywords: core shooting process, flow dynamic, two-fluid model, nozzle dimension, shooting pressure

1. Introduction

Core shooting process is the most widely used technique to make sand cores which have been extensively used for the manufacture of complicated castings in metal casting industry [1]. Due to the inherent complexity of the core shooting process, the effective use of core shooting process still faces some challenges. Firstly, effectiveness of the process and the quality of the sand cores are strongly affected by the operative conditions. Secondly, an excellent understanding of the mechanisms governing the complex gas-solid behavior involved in core shooting process is still a scientific and technical issue. Therefore, a detailed investigation of the instantaneous gas-particle behavior in core shooting process is of great importance.

Computational fluid dynamics (CFD), which has evolved into a useful tool for a wide range of engineering and scientific cases, can provide valuable insights into hydrodynamic behavior of core shooting process. In the framework of Eulerian-Eulerian two-fluid model, kinetic theory of granular flow (KTGF), initiated by Jenkins and Savage [2], Lun et al. [3] and Ding and Gidaspow [4], was commonly applied to model the constitutive correlations for the particle phase. In the last two decades, the KTGF has been successfully used to simulate dilute flows [5-6]. However, the KTGF showed some shortcomings when applied to dense flows. Several researchers [7-8] attributed these to the fact that the particle-particle interactions are long-term multi-particle contacts rather than binary collisions as assumed by the KTGF. In recent years, frictional-kinetic modeling of dense flows have flourished and most of them originate from the assumptions that frictional stress can be modeled based on the rigid-plastic rheology model proposed by Schaeffer [9] and that frictional and kinetic stresses can be treated in an additive manner [10]. Though simple, frictional-kinetic models have been successfully used to describe the hydrodynamic behavior in spouted beds [11-13], pneumatic conveying of pulverized coal [14] and bin discharge [15].

The research on modeling of core shooting process is still limited. Wu et al. [16-17] applied two-fluid model (TFM) with KTGF for core shooting process simulation and the sand flow behavior at dilute regions was predicted well. Winartomo et al. [18] modeled the core shooting process in a similar way and qualitatively validated the simulation results with experiments. Bakhtiyarov et al. [1] applied TFM with an empirical approach assuming a constant viscosity of the solid phase (CVM). In the previous studies, the frictional stress among the particle phase, the turbulence interaction between particle phase and gas phase, and the particle-wall interaction were not considered reasonably. In addition, the simulations in the previous studies were limited to the domain of core box while the effects of sand bin and nozzle were ignored, which could result in some inaccuracy.

It must be emphasized that an essential step towards the development of reliable CFD models is a fully experimental validation of the predicted results. To

investigate the instantaneous particle behavior, different non-intrusive techniques have been successfully used in the gas-particle flows, among those are high speed photography, Electrical Capacitance Tomography and X-ray, as demonstrated by Heindel et al. [19]. High speed photography has been chosen by a number of researchers due to its applicability for various experimental conditions. Busciglio et al. [20] and Liu et al. [21] applied high speed camera to study the bubbling behavior of a fluidized bed and the particle flow dynamics in a spouted bed respectively. Wu et al. [17] and Winartomo et al. [18] applied high speed camera to photograph the sand behavior during the core shooting process. However, their experimental results can't be used to analyze the core shooting process as the experiments were lack of systematization and the photographs were not clear enough.

In this paper, a two-fluid model, together with a kinetic-frictional constitutive correlation, was applied to simulate the flow dynamics of core shooting process. Different viscous models ($k-\varepsilon$ turbulence and laminar model) and several wall boundary conditions (nearly-free slip, partial slip and no slip) were incorporated to investigate their effects on the modeling of core shooting process. Simulations were then carried out for the whole domain including the sand bin, the nozzle and the core box and compared with systematic experimental results to fully validate the model. Based on both simulation and experimental results, the flow behavior of sand particles in the core box was analyzed. Finally, effects of nozzle dimension and shooting pressure on the flow behavior of sand particles were investigated in details.

2. Mathematical model

The two-fluid model is based on the fundamental concept of interpenetrating continua for the gas-particle mixtures. The equations of mass and momentum conservation are solved for each considered phase. Appropriate constitutive equations are specified to describe the rheological properties of each phase.

2.1 Governing equations

The governing equations of mass and momentum conservation for each phase are given in Table 1. Besides the mass and momentum equations, a fluctuation kinetic energy equation, Eq. (T1-5), is also solved to account for the conservation of the fluctuation energy of particle phase in the kinetic theory of granular flow (KTGF). In theory, the KTGF, initialed by Jenkins and Savage [2], Lun et al. [3] and Ding and Gidaspow [4], has been derived from the kinetic theory of dense gases. In KTGF, it assumes that the particle-particle interactions are mainly dominated by binary collisions and stresses arise because of collisional transfer of momentum. A quantity named the 'granular temperature', θ , which is analogous to the thermal temperature in the kinetic theory of gases, is defined as $\theta = \langle C^2 \rangle / 3$, where C is the particle random fluctuating velocity. The granular temperature can be computed by solving the fluctuation kinetic energy equation for the particle phase.

Table 1 Governing equations

Gas phase		
Continuity	$\frac{\partial}{\partial t}(\alpha_g \rho_g) + \nabla \cdot (\alpha_g \rho_g \mathbf{V}_g) = 0$	T1-1
Momentum	$\frac{\partial}{\partial t}(\alpha_g \rho_g \mathbf{V}_g) + \nabla \cdot (\alpha_g \rho_g \mathbf{V}_g \mathbf{V}_g) = \alpha_g \nabla \cdot \boldsymbol{\tau}_g - \beta(\mathbf{V}_g - \mathbf{V}_s) - \alpha_g \nabla P_g + \alpha_g \rho_g \mathbf{g}$	T1-2
Particle phase		
Continuity	$\frac{\partial}{\partial t}(\alpha_s \rho_s) + \nabla \cdot (\alpha_s \rho_s \mathbf{V}_s) = 0$	T1-3
Momentum	$\frac{\partial}{\partial t}(\alpha_s \rho_s \mathbf{V}_s) + \nabla \cdot (\alpha_s \rho_s \mathbf{V}_s \mathbf{V}_s) = \nabla \cdot \boldsymbol{\tau}_s + \beta(\mathbf{V}_g - \mathbf{V}_s) - \alpha_s \nabla P_g - \nabla P_s + \alpha_s \rho_s \mathbf{g}$	T1-4
Fluctuation kinetic energy		
	$\frac{\partial}{\partial t}(\alpha_s \rho_s \theta) + \nabla \cdot (\alpha_s \rho_s \mathbf{V}_s \theta) = \frac{2}{3} [(\boldsymbol{\tau}_s - P_s \cdot \mathbf{I}) : \nabla \mathbf{V}_s - \nabla \cdot (k_s \nabla \theta) - \gamma_s]$	T1-5

2.2 Kinetic-frictional constitutive correlation

Following Srivastava and Sundaresan [10] and Delannay et al. [22], it is assumed that the stress tensor $\boldsymbol{\tau}_s$ can be treated by a sum of the frictional stress tensor $\boldsymbol{\tau}_s^f$ and the kinetic stress tensor $\boldsymbol{\tau}_s^k$:

$$\boldsymbol{\tau}_s = \boldsymbol{\tau}_s^f + \boldsymbol{\tau}_s^k \quad (1)$$

The kinetic part of the stress tensor $\boldsymbol{\tau}_s^k$ is defined from KTGF, which assumes collisions are binary and instantaneous and neglects multi-particle and longer-lasting contacts. The kinetic constitutive equations derived from KTGF are listed in Table 2 and a comprehensive discussion of KTGF can be found in Gidaspow [23].

Table 2 Kinetic constitutive equations

Stress tensor	$\boldsymbol{\tau}_s^k = 2\mu_s^k \left\{ \frac{1}{2} [\nabla \mathbf{V}_s + (\nabla \mathbf{V}_s)^T] - \frac{1}{3} \nabla \cdot \mathbf{V}_s \mathbf{I} \right\} + (-P_s^k + \xi_s^k \nabla \cdot \mathbf{V}_s) \mathbf{I}$	T2-1
Particle pressure	$P_s^k = \rho_s \alpha_s \theta [1 + 2(1+e) g_0 \alpha_s]$	T2-2
Shear viscosity	$\mu_s^k = \frac{4}{5} \alpha_s^2 \rho_s d_s g_0 (1+e) \sqrt{\frac{\theta}{\pi}} + \frac{10 \rho_s d_s \sqrt{\pi \theta}}{96(1+e) \alpha_s g_0} \left[1 + \frac{4}{5} g_0 \alpha_s (1+e) \right]^2$	T2-3
Bulk viscosity	$\xi_s^k = \frac{4}{3} \alpha_s^2 \rho_s d_s g_0 (1+e) \sqrt{\frac{\theta}{\pi}}$	T2-4
Conductivity of the fluctuating energy		

$$k_s = -2\alpha_s^2 \rho_s d_s g_0 (1+e) \sqrt{\frac{\theta}{\pi}} - \frac{75\rho_s d_s \sqrt{\pi\theta}}{384(1+e)g_0} \left[1 + \frac{6}{5} g_0 \alpha_s (1+e) \right]^2 \quad \text{T2-5}$$

$$\text{Dissipation fluctuating energy} \quad \gamma_s = 3(1-e^2) \alpha_s^2 \rho_s g_0 \theta \left[\frac{4}{d_s} \left(\frac{\theta}{\pi} \right)^{1/2} \right] - \nabla \cdot \mathbf{V}_s \quad \text{T2-6}$$

At high particle volume fractions, particles start to endure sustained contacts and transmit forces over long-range stress chains [24]. Under such conditions, the dominant forces are the frictional stress. It is common to model the frictional stress with the critical state theory of soil mechanics and a rigid-plastic rheological model proposed by Schaeffer [9]:

$$\boldsymbol{\tau}_s^f = P_s^f \mathbf{I} - P_s^f \sqrt{2} \sin \phi \frac{\mathbf{D}_{ij}}{\sqrt{\mathbf{D}_{ij} : \mathbf{D}_{ij}}} \quad (2)$$

where

$$\mathbf{D}_{ij} = \frac{1}{2} \left\{ \nabla \mathbf{V}_s + (\nabla \mathbf{V}_s)^T \right\} - \frac{1}{3} (\nabla \cdot \mathbf{V}_s) \mathbf{I} \quad (3)$$

and ϕ is the angle of internal stress. It can be readily proven that the deformation of granular material described by Eq. (2) satisfies an extended von Mises yield function [10]. At the critical state, where the granular material deforms without volume change, P_s^f is the mean stress at the critical state. Generally, P_s^f increases monotonically with sand volume fraction α_s and becomes diverging as α_s approaches maximum sand volume fraction α_{max} . The semi-empirical correlation proposed for the dependence of P_s^f on α_s by Johnson et al. [25] has been successfully used in the literature. In this work, the correlation considered by Johnson et al. [25] is used and is given by:

$$P_s^f(\alpha_s) = \begin{cases} F \frac{(\alpha_s - \alpha_{min})^c}{(\alpha_{max} - \alpha_s)^s} & \alpha_s > \alpha_{min} \\ 0 & \alpha_s \leq \alpha_{min} \end{cases} \quad (4)$$

where F , c , s are empirical constants and assigned 0.05, 2 and 5 respectively. Therefore, the frictional viscosity can be described as:

$$\mu_s^f = \frac{P_s^f(\alpha_s) \sqrt{2} \sin \phi}{2 \sqrt{\mathbf{D}_{ij} : \mathbf{D}_{ij}}} \quad (5)$$

Once the frictional stress and kinetic stress have been constructed, the particle phase pressure and the shear viscosity are given as:

$$P_s = P_s^k + P_s^f \quad (6)$$

$$\mu_s = \mu_s^k + \mu_s^f \quad (7)$$

2.3 Interfacial momentum force

In dense gas-particle systems, inter-phase forces other than the drag force are usually neglected because of the large density difference between particle and gas. Gidaspow [23] proposed a widely used gas-particle drag coefficient which is a combination of the Ergun and Wen-Yu experimental correlations:

$$\beta|_{Ergun} = 150 \frac{\alpha_s^2 \mu_{eg}}{\alpha_g d_s^2} + 1.75 \frac{\rho_g \alpha_s |\bar{\mathbf{V}}_g - \bar{\mathbf{V}}_s|}{d_s} \quad \text{for } \alpha_s > 0.2 \quad (8)$$

$$\beta|_{Wen-Yu} = \frac{3}{4} C_d \frac{\rho_g \alpha_s \alpha_g |\mathbf{V}_g - \mathbf{V}_s|}{d_s} \alpha_g^{-2.65} \quad \text{for } \alpha_s \leq 0.2 \quad (9)$$

with

$$C_d = \begin{cases} \frac{24}{Re} (1 + 0.15 Re^{0.687}) & Re < 1000 \\ 0.44 & Re \geq 1000 \end{cases} \quad (10)$$

where Re is Reynolds number and $Re = \alpha_g \rho_g d_s |\mathbf{V}_g - \mathbf{V}_s| / \mu_{eg}$.

To avoid discontinuity of these correlations, a blending function was constructed by Huilin et al. [26] to provide a smooth but rapid transition between the two regimes:

$$\varphi(\alpha_s) = \frac{\text{Arc tan}(150 \times 1.75(0.2 - \alpha_s))}{\pi} + 0.5 \quad (11)$$

Thus, the gas-particle drag coefficient is given by

$$\beta = (1 - \varphi(\alpha_s)) \beta_{Ergun} + \varphi(\alpha_s) \beta_{Wen-Yu} \quad (12)$$

2.4 Turbulence model

In literatures, good agreement with experimental result has been achieved in turbulent fluidized bed with both laminar [27-28] and turbulent model [29]. Thus it is difficult to confirm the importance of gas or particle phase turbulence in the core shooting process. In the present work, both approaches were established to discuss the effect of turbulence on the prediction of the hydrodynamic behavior in core shooting process. Due to their simplicity and reasonable accuracy, k - ε models are widely used for a variety of turbulent flows. In the current work, the per-phase k - ε turbulence model was applied to describe the effect of turbulence. The gas phase was simulated with the widely used k - ε model for single-phase flows, and the particle phase was described by the particle turbulence k_s - ε_s model, which is analogous to the equations of k - ε model. Based on the k - ε - k_s - ε_s turbulence model, the turbulent kinetic energy equation and the dissipation rate equation are given respectively by

$$\begin{aligned} \frac{\partial}{\partial t}(\alpha_i \rho_i k_i) + \nabla \cdot (\alpha_i \rho_i k_i \mathbf{U}_i) = \nabla \cdot (\alpha_i \frac{\mu_{t,i}}{\sigma_k} \nabla k_i) + (\alpha_i G_{k,i} - \alpha_i \rho_i \varepsilon_i) + \\ \beta(C_{ll} k_l - C_{il} k_i) - \beta(\mathbf{U}_l - \mathbf{U}_i) \frac{\mu_{t,l}}{\alpha_l \sigma_l} \nabla \alpha_l + \beta(\mathbf{U}_l - \mathbf{U}_i) \frac{\mu_{t,i}}{\alpha_i \sigma_i} \nabla \alpha_i \end{aligned} \quad (13)$$

$$\begin{aligned} \frac{\partial}{\partial t}(\alpha_i \rho_i \varepsilon_i) + \nabla \cdot (\alpha_i \rho_i \varepsilon_i \mathbf{U}_i) = \nabla \cdot (\alpha_i \frac{\mu_{t,i}}{\sigma_\varepsilon} \nabla \varepsilon_i) + \frac{\varepsilon_i}{k_i} (C_{1\varepsilon} \alpha_i G_{k,i} - C_{2\varepsilon} \alpha_i \rho_i \varepsilon_i) + \\ C_{3\varepsilon} \frac{\varepsilon_i}{k_i} [\beta(C_{ll} k_l - C_{il} k_i) - \beta(\mathbf{U}_l - \mathbf{U}_i) \frac{\mu_{t,l}}{\alpha_l \sigma_l} \nabla \alpha_l + \beta(\mathbf{U}_l - \mathbf{U}_i) \frac{\mu_{t,i}}{\alpha_i \sigma_i} \nabla \alpha_i] \end{aligned} \quad (14)$$

where $\mathbf{U}_i$ (i =gas, particle) is the phase-weighted velocity, $G_{k,i}$ is the production of turbulent kinetic energy and $\mu_{t,i}$ is the turbulent viscosity given by

$$\mu_{t,i} = \rho_i C_\mu \frac{k_i^2}{\varepsilon_i} \quad (15)$$

where k_i is the turbulent kinetic energy, ε_i is the turbulent energy dissipation rate and C_μ is 0.09. When k - ε - k_s - ε_s turbulence model is employed, the default modeling parameters in FLUENT were used. A comprehensive discussion of the turbulence model used in this work can be found in Fluent Theory Guide [30].

3. Experimental setup and simulation sets

3.1 Experimental setup

The experiment apparatus used in this work is illustrated in Fig. 1. Silica sand with mean sand diameter of 240 μ m and bulk density of 1417 Kg·m⁻³ was used in the core shooting process. A core box with dimensions of 150mm (height) $\times$ 150mm (width) $\times$ 25mm (depth) was installed in the core shooting machine. The core shooting process started from abrupt transportation of compressed air into the sand bin. The sand particles were then shot into the core box through the nozzle. Finally, the core box was filled with compacted core sand and the remaining air escaped through vents. During the experiments, a computer controlled high-speed camera (Mega speed MS55K) was used to photograph the flow behavior through the transparent front wall (made of plexiglas) of the core box. The flow field in the core box was illuminated by two identical 1000W fluorescent lights symmetrically placed beside the core box. Five pressure sensors (Keyence AP-C30C) and a computer controlled data acquisition instrument (IMC BUSDAQ) were used to measure the pressure variations during the core shooting process. The respective locations of the five pressure sensors (#1, #2, #3, #4 and #5) are shown in Fig.1 and the collection frequency was set to be 100HZ.

3.2 Simulation sets

The set of coupled governing equations were discretized into linear equations using finite-volume method by means of FLUENT. SIMPLEC algorithm was used for the strong coupling of velocity and pressure. The simulations were carried out for the whole domain including the sand bin, the nozzle and the core box, as shown by Fig.

2(a). To study the effects of nozzle dimension and shooting pressure, four different nozzle dimensions ($D=6, 8, 10, 15\text{mm}$) and three different pressure values ($P_{in}=0.3, 0.4, 0.5\text{MPa}$) were set. The grid size and time step used were $2\text{mm}\times 2\text{mm}$ and 1×10^{-5} respectively. To examine the grid dependence, grid size of $2.5\text{mm}\times 2.5\text{mm}$, $3\text{mm}\times 3\text{mm}$ and $4\text{mm}\times 4\text{mm}$ were also used, which will be discussed in section 4.1.

Table 3 gives the modeling parameters used in the simulations. The angle of internal friction is set to be 0.3 according to Winartomo et al. [18]. For the inlet condition, the pressure curves measured by pressure sensor #1 were used and further simplified to set the inlet pressure, as shown in Fig. 2b. The outlet condition (vents) was defined as a pressure boundary with the atmosphere pressure.

Table 3 Modeling parameters

Property	Symbol	Value
Sand density(kg m^{-3})	ρ_s	2650
Mean sand diameter(μm)	d_s	240
Restitution coefficient	e	0.9
Max. sand volume fraction	α_{max}	0.63
Minimum sand volume fraction for transition to consider frictional stress	α_{min}	0.5
Angle of internal friction	ϕ	0.3
Specularity coefficient	φ	0.002,0.5,1

4. Results and discussions

4.1 Grid size independency

When the grid is oversize, there will be uneven clusters within a single grid, as demonstrated by Agrawal et al. [31]. The uneven characteristic may lead to incorrect simulation results. So a grid-independent study should be carried out to confirm that the predicted result is independent of the grid size. Simulations were performed with $2\text{mm}\times 2\text{mm}$, $2.5\text{mm}\times 2.5\text{mm}$, $3\text{mm}\times 3\text{mm}$ and $4\text{mm}\times 4\text{mm}$ grids. The predicted instantaneous sand volume fraction for different grid sizes is plotted in Fig. 3, and it can be seen the contour for $4\text{mm}\times 4\text{mm}$ grid is quite different from other cases, especially the sand volume fraction at the nozzle. To further examine the grid dependence, distributions of sand volume fraction and sand velocity at the nozzle are compared for the four different grid sizes and shown in Fig. 4. It can be seen that the coarse grid of 4mm gives a solution that is even qualitatively wrong: the predicted sand velocity at middle of the nozzle is lower than that at border of the nozzle, as shown in Fig. 4(b). The observed results indicate that a grid size of 2.5mm is adequate to get grid-independent results. This is in agreement with the reported grid-independent condition that the grid size should be of the order of 10 particle diameters [32], corresponding to 2.5mm grid in this study. Thus, a finer 2mm grid was used in the following simulations.

4.2 Model validation

4.2.1 Comparison of the laminar and the turbulence model

Simulations with laminar model and $k-\varepsilon-k_s-\varepsilon_s$ turbulence model were carried out to study the effect of turbulence modeling on flow predictions. Fig. 5 shows the comparison of the predicted sand volume fraction contour between the laminar model and the turbulence model. It is observed that the two models predict almost the same flow behavior with only negligible difference. Shooting pressure is an important operating parameter that may influence the flow pattern. Under general condition, the setting pressure in core shooting process is in the range of 0.3MPa to 0.5 MPa. Therefore, pressure values of 0.3, 0.4 and 0.5MPa are chosen to study the effect of turbulence modeling. As shown in Fig.6, similar contours of sand volume fraction are also observed for the two models under higher shooting pressure. Distributions of sand volume fraction and sand velocity at the nozzle are further compared for the two models and given in Fig. 7. Consistent simulation results are observed for the two models and it suggests that the turbulence is not strong enough to significantly influence the flow behavior, which is in agreement with the reports from Gao et al. [27] and Gao et al. [28]. The observed results indicate that the two models considered in this work predict similar flow behavior, which suggests that the laminar model is sufficient to model the core shooting process. Winartomo et al. [18] also experimentally show that the sand-mixture flows without significant vorticity during the core shooting process. Therefore, laminar model was used for the rest of this study to reduce computational complexity.

4.2.2 Effect of wall boundary condition

No slip condition was employed for the gas phase, which is widely accepted in literatures. However, there is still a dispute on what kind of wall boundary conditions should be used for the particle phase. Benyahia et al. [33-34] suggested that a nearly-free slip boundary condition or a simply free-slip BC should be used to get better predicted results, which is successfully applied by a number of researchers [33-39]. However, Armstrong et al. [40] found that a partial-slip BC showed a more reasonable prediction, and Benzarti et al. [29] and Shuyan et al. [41-42] prescribed no-slip BC for particle phase. Clearly, there is no consistency on the boundary condition for particle phase. Thus, three different boundary conditions (nearly-free slip, partial slip and no slip) were used to study the effects of wall boundary condition on the flow predictions. In this work, the boundary condition proposed by Johnson and Jackson [43] was adopted and different boundary conditions were achieved through a variation of the specular coefficient, ϕ . Three specular coefficient values: 0.002(nearly-free slip), 0.5(partial slip) and 1(no slip), were tested in this work to find out a better fit for the core shooting process.

Fig. 8 shows the predicted instantaneous sand volume fraction for three different boundary conditions and the corresponding experimental result. It is clear that nearly-free slip condition is significantly different from the other two cases and agrees better with the experimental result. The sand volume fraction distribution for partial slip and no slip condition are similar: the sand volume fraction near the wall

is rather low. This is in agreement with the reports that an increase in the specular coefficient results in a reduction in the solids volume fraction at the wall [34-35]. To quantitatively discuss the effects of boundary condition, distributions of sand volume fraction and x-direction sand velocity at bottom of the core box (indicated with a black dashed line in Fig. 8(b)) are compared for the three different boundary conditions and shown in Fig. 9. A 57% reduction (from 0.54 to 0.23) in the sand volume fraction and a 47% reduction (from 5.7m/s to 3m/s) in the x-direction sand velocity at the bottom wall are observed when increasing the specular coefficient from 0.002 (free slip) to 0.5 (partial slip). The observed results indicate that the specular coefficient has a significant effect on the flow behavior, sand volume fraction and sand velocity and a reasonable agreement can be achieved with the nearly-free slip BC ($\phi=0.002$). Similar results can also be found in a number of studies [33-39] and hence the nearly-free slip BC is chosen to be the wall boundary condition for the particle phase.

4.3 Flow behavior of sand particles

Fig. 10 shows the comparison of visualized photo sequences recording the flow behavior of core shooting process by high speed camera with the 3D simulated contours of sand volume fraction. Reasonably agreement is obtained between the simulation results and the experimental results: When sand particles are fed into the core box with compressed air, they move downward and a mushroom-like front-end is generated. After sand flow reaching the bottom of core box, a sand pile is formed and sand particles move laterally towards the sidewall. Subsequently, sand particles move upward along the sidewall and the side and bottom part of the core box is filled in succession. Finally, sand particles pile up uniformly till the core box is completely filled. Similar filling process is observed when comparing 3D simulation results (Fig. 10b) with 2D simulation results (Fig. 5). Fig. 11 shows the comparison of predicted gas pressure at #2, #3, #4 and #5 (indicated in Fig. 1) with the corresponding experimental results. As the measured pressure curve at #1 was used as the pressure inlet (shown in Fig. 2b), the predicted value at #1 is the same as the measured value and thus not compared here. It can be seen that the main characteristics of the predicted pressure curves are consistent with the measured pressure values: The gas pressure in the core box rises rapidly with gas-particle mixture flowing into the core box continuously; After #5, #4, and #3 are filled in succession, the pressure value at these corresponding locations declines slowly; When the core box is totally filled, the gas pressure at #2 reaches a maximum value and then the gas pressure at #2, #3, #4 and #5 declines rapidly to low pressure value.

Fig. 12(a) shows the variation of sand volume fraction at the nozzle, there is a sharp increase and a slight reduction within the initial 0.03s and then the sand volume fraction keeps a constant until the end of core shooting process. Additionally, it can be seen a significant decrease in the sand volume fraction from the middle ($x=0$) to the border ($x=5\text{mm}$) of the nozzle, which indicates that the assumption of constant and uniform sand volume fraction at the nozzle [16-18] is inappropriate. According

to Eq. (T1-4), pressure difference is a main force exerting on the sand particles and can significantly influence the sand velocity. As shown in Fig. 12b, the variation of sand velocity at the nozzle is consistent with the variation of pressure difference (ΔP) between the pressure values above and below the nozzle. The sand velocity rises rapidly with the increase of ΔP during the pressure-raising period, and then it declines gradually till the end of core shooting process with the continuous decrease of ΔP .

4.4 Influence of nozzle dimension

Nozzle dimension is an important parameter that can considerably influence the core shooting process [1], and thus its effect will be discussed thoroughly here. Comparison of the predicted instantaneous sand volume fraction with the corresponding experimental results for 6mm, 8mm, 10mm and 15mm nozzle is shown in Fig.13 and a reasonable agreement is obtained. To further examine the effects of nozzle dimension, the average sand particle flux, the average sand volume fraction and the average sand velocity at the nozzle are compared for the four different nozzles. The variation of the average sand particle flux with the nozzle diameter is shown in Fig. 14 where logarithmic coordinate values are used to linear the relation. There is a quasi-quadratic relationship between the average sand particle flux and the nozzle diameter ($D^{1.9}$) and good agreement is achieved between the simulation and experimental results. It means that the average sand particle flux pre unit area is almost the same for different nozzle diameters. Fig. 15 shows the variations of average sand volume fraction and average sand velocity at the nozzle for the four different nozzle diameters. It can be seen that the average sand volume fraction increases while the average sand velocity decreases with the increase of nozzle diameter. To confirm this conclusion, the distributions of sand volume fraction and sand velocity at the nozzle are further compared and shown in Fig. 16. A consistent conclusion can be drawn that the sand volume fraction increases while the sand velocity decreases with the increase of nozzle diameter. Besides, it can be seen that the distribution of sand volume fraction and sand velocity at the nozzle is the same as that described in section 4.3: bigger at the middle and smaller at the border.

4.5 Influence of shooting pressure

Shooting pressure is an important processing parameter that may significantly influence the effectiveness of core shooting process. As the setting pressure in core shooting process is generally in the range of 0.3MPa to 0.5 MPa, pressure values of 0.3, 0.4, and 0.5MPa are chosen to investigate the effect of shooting pressure on the sand flow behavior. The velocity field of the sand particle phase under three different shooting pressure values is shown in Fig. 17. As shooting pressure is the source of kinetic energy for sand particles filling the core box, the velocity grows noticeably with the increase of shooting pressure, as one would expect and clearly shown in Fig. 17. As shown in Fig. 7a, a 35% increase in the sand velocity at the

nozzle is observed when increasing the shooting pressure from 0.3MPa to 0.5MPa. For the distribution of sand volume fraction, it remains unchanged for the three pressure values, as shown in Fig. 7b. It can be seen that the shooting pressure mainly influences the sand velocity and has no effect on the sand volume fraction. To confirm this conclusion, the variations of average sand volume fraction and average sand velocity at the nozzle over time are further compared and shown in Fig. 18. A consistent conclusion can be made that the shooting pressure significantly influences the sand velocity while has no effect on the sand volume fraction.

5. Conclusions

A two-fluid model, in which a kinetic-frictional constitutive correlation is incorporated, has been applied to simulate the flow dynamics of core shooting process. Then the sensitivity of numerical solution to the turbulence model and the wall boundary condition has been investigated. Experiments have been carried out to validate the model and reasonable agreements have been achieved between the simulation and experimental results. Finally, based on the simulation and experimental results, the sand particle behavior in core shooting process has been studied. Accordingly, the following conclusions can be drawn:

- (1) The simulation results indicate that there are certain sensitivities to the wall boundary condition in the sand flow behavior for nearly-free slip, partial slip and no slip boundary condition. With the nearly-free slip boundary condition, the core shooting process can be well predicted. The laminar and turbulence models predict similar flow behavior, which suggests that the laminar model is sufficient to model the core shooting process.
- (2) The variation of sand velocity at the nozzle is consistent with the variation of pressure difference (ΔP): the sand velocity rises rapidly within the pressure-raising period, and then it declines gradually till the end of core shooting process. While the sand volume fraction at the nozzle keeps a constant after the initial 0.03s.
- (3) There is a quasi-quadratic relationship between the average sand particle flux and the nozzle diameter ($D^{1.9}$). With the increase of nozzle diameter, the sand volume fraction at the nozzle increases while the sand velocity decreases.
- (4) As the shooting pressure increases, the sand velocity increases significantly while the sand volume fraction at the nozzle keeps unchanged.

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Figure captions

Fig. 1 Schematic of the experimental apparatus.

Fig. 2 (a) Simulation sets and (b) the setting of inlet pressure.

Fig. 3 The predicted instantaneous sand volume fraction for (a) 2mm, (b) 2.5mm, (c) 3mm and (d) 4mm grid sizes ($t=65\text{ms}$).

Fig. 4 The distribution of (a) sand volume fraction and (b) sand velocity at the nozzle for different grid sizes ($t=65\text{ms}$, x is the distance to the axis and r is the radius of the nozzle).

Fig. 5 Comparison of simulated maps of sand volume fraction with (a) laminar model and (b) $k-\varepsilon-k_s-\varepsilon_s$ turbulence model.

Fig. 6 Comparison of predicted instantaneous contours of sand volume fraction with (a)-(c) laminar model and (b)-(d) $k-\varepsilon-k_s-\varepsilon_s$ turbulence model under higher shooting pressure ($t=40\text{ms}$, 0.4MPa for (a-b) and 0.5MPa for (c-d)).

Fig. 7 The distribution of (a) sand volume fraction and (b) sand velocity at the nozzle for laminar and $k-\varepsilon-k_s-\varepsilon_s$ turbulence model under the three pressure values ($t=40\text{ms}$, x is the distance to the axis and r is the radius of the nozzle).

Fig. 8 Comparison of (a) experimental result with the predicted instantaneous sand volume fraction for (b) nearly-free slip, (c) partial slip and (d) no slip boundary

conditions ($t=40\text{ms}$, black dashed line in (b) is used to indicate the specified location).

Fig. 9 The distribution of (a) sand volume fraction and (b) x-direction sand velocity at bottom of the core box (indicated with a black dashed line in Fig. 8(b)) for the three different boundary conditions ($t=40\text{ms}$, the x-axis “Height” is the distance to the bottom wall).

Fig. 10 Comparison of (a) visualized photo sequences recording the flow behavior of core shooting process by high speed camera and (b) 3D simulated contours of sand volume fraction.

Fig. 11 Comparison of predicted pressure curves at #2, #3, #4 and #5 with the corresponding experimental data.

Fig.12 The variation of (a) sand volume fraction and (b) sand velocity at the nozzle over time (x is the distance to the axis, inset is the variation of ΔP over time).

Fig.13 Comparison of (a-d) experimental results with (e-h) the predicted instantaneous sand volume fraction for 6mm, 8mm, 10mm and 15mm nozzle ($t=40\text{ms}$).

Fig.14 The average mass flux of sand particles with nozzle diameter (logarithmic coordinate values are used to linear the relation).

Fig. 15 The variations of (a) average sand volume fraction and (b) average sand velocity at the nozzle over time for the four different nozzle diameters.

Fig. 16 The distribution of (a) sand volume fraction and (b) sand velocity at the nozzle for the four different nozzle diameters ($t=40\text{ms}$, the x-axis “Distance” means the distance to the axis).

Fig. 17 The predicted instantaneous maps of sand velocity for shooting pressure of (a) 0.3, (b) 0.4 and (c) 0.5 MPa ($t=40\text{ms}$).

Fig. 18 The variations of (a) average sand volume fraction and (b) average sand velocity at the nozzle over time for the three different pressure values.

Figure 1

Figure 2

(a)

(b)

Figure 3

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