On DEM-CFD Study of the Dynamic Characteristics of High Speed Micro Abrasive Air Jet

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1. Introduction

Abrasive jet micromachining (AJM) is considered as a state-of-the-art technology for manufacturing a wide range of engineering materials. It has become an attractive technology due to its distinct advantages of small thermal effect, small cutting forces, high machining versatility and high flexibility [1]. In particular, AJM is a promising tool for processing hard and brittle materials such as ceramics and semiconductors, as well as for fabricating electronic devices and micro-fluidic channels where damage-free, micro-part features are required. Typically, an AJM system consists of a high-pressure air or gas supplier, a mixing chamber where abrasives are mixed with air or gas, a nozzle, and a nozzle or cutting head positioning system. A mixture of abrasive particles and pressurized air is expelled through a fine nozzle to form a high velocity abrasive-laden air jet, or called abrasive air jet (AAJ). By transferring the momentum from the pressurised air onto the abrasive particles, the AAJ is capable of eroding the target material **during the deburring process** to generate the required part features by means of micro-plastic deformation and/or brittle fracture [2] through the impact of the high velocity abrasives.

In the quest of achieving innovative deburring process via AAJ, micro burrs that remain on the material remains a major problem especially in the production of micro-parts. It is imperative to determine significant factors that govern the deburring process to ensure functional capability and cleanliness of micro-components. Properties of the abrasive particles which is necessary for the control of the dispersed systems need to be properly understood. Generally, the key properties are: particle shape, geometry, surface roughness and porosity. In the simplest case, a spherical shape can be assumed and the diameter characterises the particle. However, an abrasive particle mostly exhibits distinct irregular shape which makes finding the characteristic measure of the particle extremely difficult. For the identification of the particle shape, the concept of sphericity (or shape factor) allows one to determine the

equivalent diameter to a sphere-like particle which then provides a rough measure of different grain sizes suitable for the deburring process. If the particle diameter is too small, particles have a tendency to adhere to each other and particle agglomeration results in non-uniform diffuse pattern during jet blasting. A very high pressure may be utilised but there remains a threshold where the operational pressure of AAJ can be feasibly tolerated. If the particle diameter is too large, there may be a risk where the material is embedded into the micro-part instead of being removed and the surface of the micro-part is significantly damaged by the strong abrasive action.

Although a large amount of research and development effort has been made on the abrasive water jet (AWJ) machining technology [3], the reported research on the AAJ machining appears to be less comprehensive. Nevertheless, a considerable amount of work has been conducted in recent years to develop simulation models for the AJM process [4–9]. Haj Mohammad Jafar et al. [10-11] experimentally investigated the effect of particle size, velocity and angle of attack on the rough micro-channels using AJM. One of their interesting findings is post-blasting with a particle of low kinetic energy is an effective method to reduce surface roughness of AJM channels. It was found that the roughness and erosion per particle were proportional to the particle kinetic energy. Considerable work has also been undertaken to study the particle velocities in a particle-laden air or gas jet; some of the studies on modelling the air and abrasive motion have been reviewed by Momber [12] and Humphrey [13]. These models can provide useful insight into the machining mechanism, and provide valuable guidance in selecting the process conditions. Knowledge of the particle velocity and its influencing parameters is essential for the development of reliable material removal models in AJM.

There have been some earlier reported investigations based on AWJ to understand the dynamic characteristics for particular jetting status through theoretical [14,15] and experimental [16,17] studies as well as CFD simulation [18,19]. More recently, Burzynski et

al. presented an analytical model [20] and experimental measurement [21] on the particle spatial and velocity distributions in micro-abrasive jet. They found that the developed analytical model is capable to produce results that agree with the simulation data in a wide range of jet parameters. In spite of the research that has been conducted on this topic, there is still a lack of comprehensive models for high particle velocity distribution in the AAJ stream. Air-particle interaction in the mixing chamber is regarded to be extremely intricate of which the high velocity and small nozzle and particle dimensions make the investigation of the jet and particle behaviour difficult. Specifically, an understanding of the dynamic characteristics (e.g. velocity and pressure distributions) of an abrasive jet is essential for improving nozzle design, as well as for modelling, evaluating and improving jet impacting process and AJM cutting performance which is in addition to determining the complex properties of abrasive particles.

The present work is to gain a fundamental knowledge of the high velocity jet dynamic characteristics such as air and particle velocity distributions within the jet. For this purpose, a CFD-DEM analysis is found to be a viable approach because direct measurement of particle velocities and visualisation of particle trajectories are very difficult for the high speed and small dimensions involved. CFD is first established to resolve the high velocity of AAJs. Jet dynamic characteristics such as air and particle velocities for the flow downstream from a fine nozzle exit are simulated under transient, turbulent, two-phase flow conditions and a range of inlet conditions. DEM is then employed to characterise particle-particle collisions especially occurring within the small nozzle and particles exiting the nozzle. In this study, particle-particle collisions are taken to occur between sphere-like particles in order to simplify the determination of contact forces. The results from the CFD-DEM study are analysed to gain an insight into the jet characteristics and an understanding of the formation of an abrasive jet. Numerical results are validated against the experimentally measured jet characteristic data.

2. Mathematical Formulation

The governing equations for the gas (or air) phase of the high speed air flow mainly follow the ensemble-averaged form of the two-fluid model presented in Yeoh and Tu [22]. For the solid phase, the motion of particles obeys the Newton second law of motion and particle-particle collisions are described by the soft-sphere model developed by Cundall and Strack [23] and Tsuji et al. [24]. Interactions between the gas and solid phases are accounted for through the momentum exchange term.

2.1 Gas phase governing equations

Based on the two-fluid model formulation, the compressible form of equations governing the conservation of mass, momentum and energy for high speed air flow can be expressed as

$$\frac{\partial(\alpha^g \rho^g)}{\partial t} + \nabla \cdot (\alpha^g \rho^g \mathbf{U}^g) = 0 \quad (1)$$

$$\begin{aligned} \frac{\partial(\alpha^g \rho^g \mathbf{U}^g)}{\partial t} + \nabla \cdot (\alpha^g \rho^g \mathbf{U}^g \otimes \mathbf{U}^g) = & -\nabla(\alpha^g p^g) + \left(\nabla \cdot \alpha^g \left[\mu^g (\nabla \mathbf{U}^g + (\nabla \mathbf{U}^g)^T) \right. \right. \\ & \left. \left. - \frac{2}{3} \mu^g \nabla \cdot \mathbf{U}^g \mathbf{I} \right] \right) - \nabla \cdot (\alpha^g \boldsymbol{\tau}^{g''}) + \mathbf{S}^g + \alpha^g \rho^g \mathbf{g} \end{aligned} \quad (2)$$

$$\begin{aligned} \frac{\partial(\alpha^g \rho^g H^g)}{\partial t} + \nabla \cdot (\alpha^g \rho^g \mathbf{U}^g H^g) = & -\frac{\partial(\alpha^g p^g)}{\partial t} + \nabla \cdot (\alpha^g \lambda^g \nabla T^g) + \alpha^g \rho^g \mathbf{g} \cdot \mathbf{U}^g \\ & - \nabla \cdot (\alpha^g \mathbf{q}_H^{g''}) + \Phi_{H^g} \end{aligned} \quad (3)$$

where α^g is the volume fraction of gas phase, ρ^g is the density of the gas phase, $\mathbf{U}^g$ is the gas velocity vector, p^g is the gas pressure, $\mathbf{g}$ is the gravitational vector, λ^g is the thermal conductivity of the gas phase, and $\boldsymbol{\tau}^{g''}$ and $\mathbf{q}_H^{g''}$ are the Reynolds stress flux and Reynolds flux terms that are to be resolved via the consideration of a two-equation turbulence model. The effects due to the viscous extra stresses that appear in the conservation energy equation, Φ_{H^g} , are accounted for the high speed air flow which is described by $\nabla \cdot \left(\alpha^g \mathbf{U}^g \cdot \left[\mu^g \left(\nabla \mathbf{U}^g + (\nabla \mathbf{U}^g)^T \right) - \frac{2}{3} \mu^g \nabla \cdot \mathbf{U}^g \mathbf{I} \right] \right)$. This so-called viscous heating represents a source of energy due to work done deforming the fluid element whereby work is extracted from the mechanical energy that causes fluid movement, which is subsequently converted into heat. The specific enthalpy H^g is defined as

$$H^g = e^g + \frac{p^g}{\rho^g} + \frac{1}{2} \mathbf{U}^g \cdot \mathbf{U}^g \quad (4)$$

where e^g is specific internal energy for the gas phase. Because the carrier medium is air, the use of the equation of state $\rho^g = p^g / R^g T^g$ is deemed to be appropriate in this study such as already demonstrated by Anderson [25] for a supersonic air flow of a Prandtl-Meyer expansion wave problem. Once the temperature is evaluated from the specific enthalpy, the density is determined through the equation of state. In equation (2), the source term $\mathbf{S}^g$ is due primarily by the particle-fluid drag interaction, which is computed from

$$\mathbf{S}^g = \frac{1}{V} \int_V \sum_{k=1}^{N_p} \frac{V_k^p \beta}{1 - \alpha^g} (\mathbf{U}^g - \mathbf{V}_k^p) \delta(\mathbf{r} - \mathbf{r}_k^p) dV \quad (5)$$

where V is the integral volume, N^p is the particle number, V^p is the particle volume, β is the inter-phase momentum transfer coefficient and δ is a function which ensures that the reaction force acts as a point force at the position of the particle.

It has been demonstrated by Lopez de Bertodano et al. [26,27] that the eddy viscosity hypothesis in describing the turbulence in single-phase flow can be extended to describe the Reynolds stress flux and Reynolds flux terms in two-phase flow. The Reynolds stress term in equation (2) can be expressed as

$$-\tau^{g''} = \mu_T^g \left(\nabla \mathbf{U}^g + (\nabla \mathbf{U}^g)^T \right) - \frac{2}{3} \mu_T^g \nabla \cdot \mathbf{U}^g \mathbf{I} - \frac{2}{3} \rho^g k^g \mathbf{I} \quad (6)$$

while the Reynolds flux term is modelled as

$$-\mathbf{q}_H^{g''} = \frac{\mu_T^g}{Pr_T^g} \nabla H^g \quad (7)$$

where k^g is the turbulent kinetic energy of the gas phase and μ_T^g is the turbulent or eddy viscosity of the gas phase and Pr_T^g is the turbulent Prandtl number. The turbulent or eddy viscosity can be calculated according to

$$\mu_T^g = C_\mu \rho^g \frac{(k^g)^2}{\varepsilon^g} \quad (8)$$

where C_μ is evaluated based on the expression defined in equation (16) and ε^g is the dissipation of turbulent kinetic energy of the gas phase. In this study, the reliable k - ε model is adopted [28]. The term “reliable” means that the model satisfies certain mathematical

constraints on the normal stresses, consistent with the physics of turbulent flows. In order to determine the turbulent or eddy viscosity, the values of k^g and ε^g are solved according to their respective transport equations:

$$\frac{\partial}{\partial t}(\alpha^g \rho^g k^g) + \nabla \cdot (\alpha^g \rho^g \mathbf{U}^g k^g) = \nabla \cdot \left(\alpha^g \frac{\mu_T^g}{\sigma_k} \nabla k^g \right) + \alpha^g (P^g + G^g - \rho^g \varepsilon^g - S_M^g) \quad (9)$$

$$\begin{aligned} \frac{\partial}{\partial t}(\alpha^g \rho^g \varepsilon^g) + \nabla \cdot (\alpha^g \rho^g \mathbf{U}^g \varepsilon^g) = & \nabla \cdot \left(\alpha^g \frac{\mu_T^g}{\sigma_\varepsilon} \nabla \varepsilon^g \right) \\ & + \alpha^g \left(\rho^g C_1 S \varepsilon^g - \rho^g C_2 \frac{(\varepsilon^g)^2}{k^g + \sqrt{\mu_T^g \varepsilon^g / \rho^g}} + C_{1\varepsilon} \frac{\varepsilon^g}{k^g} C_{3\varepsilon} G^g \right) \end{aligned} \quad (10)$$

In equation (9), the shear production P^g of the gas phase is given by

$$P^g = \mu_T^g \nabla \mathbf{U}^g \cdot (\nabla \mathbf{U}^g + (\nabla \mathbf{U}^g)^T) - \frac{2}{3} \nabla \cdot \mathbf{U}^g (\mu_T^g \nabla \cdot \mathbf{U}^g - \rho^g \mathbf{U}^g) \quad (11)$$

while G^g , which is the generation of turbulent kinetic energy due to buoyancy of the gas phase in equations (9) and (10), can be expressed as

$$G^g = -\frac{\mu_T^g}{\rho^g \sigma_\rho} \mathbf{g} \cdot \nabla \rho^g \quad (12)$$

For high speed air flow, the contribution of the fluctuating dilatation in compressible turbulence to the overall dissipation rate of the gas phase S_M^g is modelled according to a proposal by Sarkar and Balakrishnan [29], i.e.

$$S_M^g = 2\rho^g \varepsilon^g \frac{k^g}{a^2} \quad (13)$$

where a is the speed of sound. In equation (10), S is denoted by

$$S = \sqrt{2S_{ij}S_{ij}}, \quad S_{ij} = \frac{1}{2} \left(\frac{\partial u_i^g}{\partial x_j} + \frac{\partial u_j^g}{\partial x_i} \right) - \frac{1}{3} \frac{\partial u_k^g}{\partial x_k} I \quad (14)$$

The variable constant C_1 can be expressed as

$$C_1 = \max \left[0.43, \frac{\eta}{\eta + 5} \right], \quad \eta = \frac{k}{\varepsilon} S \quad (15)$$

while the variable C_μ is evaluated from

$$C_\mu = \frac{1}{A_o + A_s \frac{kU^*}{\varepsilon}} \quad (16)$$

The model constants A_o and A_s in [equation \(16\)](#) are determined as

$$A_o = 4.04, \quad A_s = \sqrt{6} \cos \varphi, \quad \varphi = \frac{1}{3} \cos^{-1}(\sqrt{6}W), \quad W = \frac{S_{ij}S_{jk}S_{ki}}{\tilde{S}^3}, \quad \tilde{S} = \sqrt{S_{ij}^2}$$

and the parameter U^* is given by

$$U^* \equiv \sqrt{S_{ij}^2 + \tilde{\Omega}_{ij}^2}, \quad \tilde{\Omega}_{ij} = \Omega_{ij} - 2\varepsilon_{ijk}\omega_k, \quad \Omega_{ij} = \tilde{\Omega}_{ij} - \varepsilon_{ijk}\omega_k$$

The constants in the turbulent transport equations are: $\sigma_k = 1.0$, $\sigma_\varepsilon = 1.3$, $C_2 = 1.9$, $C_{1\varepsilon} = 1.44$, $C_{3\varepsilon} = 1.0$, $\sigma_\kappa = 1.0$ and $\sigma_\varepsilon = 1.2$ respectively [28]. Scalable logarithmic wall function is employed to bridge the near wall flow with the high Reynolds flow in the centre region of the cylindrical nozzle.

2.2 Solid phase governing equations

The coordinate system that is used to describe the time-driven discrete element method is illustrated in Figure 1. In the discrete element method, the trajectory of a particle can be determined by calculating all forces and moments acting on it. The mean particle velocity can be obtained through solution of the particle linear momentum equation (*Newton second law*), given by

$$m_i \frac{d\mathbf{V}_i^p}{dt} = m_i \frac{d^2 \mathbf{r}_i^p}{dt^2} = m_i \mathbf{g} + \mathbf{F}_i^{drag} + \mathbf{F}_i^{contact} \quad (17)$$

where m_i is the mass, $\mathbf{V}_i^p$ is the velocity vector, $\mathbf{r}_i^p$ is the position vector of a single solid particle of element i . The forces on the right hand side of equation (17) are the gravity, drag and contact forces (i.e. due to collisions).

The angular momentum of the mean particle rotation rate is computed from

$$I_i \frac{d\boldsymbol{\Omega}_i^p}{dt} = \mathbf{T}_i \quad (18)$$

where I is the moment of inertia and $\mathbf{T}$ is the torque vector. With regard to the assumption of a spherical particle with radius R_i , the moment of inertia is $I_i = \frac{2}{3} m_i R_i^2$.

The drag force in equation (17) is calculated as

$$\mathbf{F}_i^{drag} = \frac{V_i^p \beta}{1 - \alpha^g} (\mathbf{U}^g - \mathbf{V}_i^p) \quad (19)$$

In this study, the non-spherical characteristics of the abrasive particle (see Figure 4(b)) influencing the drag force are captured via the inter-phase momentum transfer coefficient:

$$\beta = \frac{18\mu^g}{\rho^p (d_i^p)^2} \frac{C_D Re_{spherical}}{24} \quad (20)$$

Based on Haider and Levenspiel [30], the drag coefficient C_D is given by

$$C_D = \frac{24}{Re_{spherical}} \left(1 + b_1 Re_{spherical}^{b_2} \right) + \frac{b_3 Re_{spherical}}{b_4 + Re_{spherical}} \quad (21)$$

where

$$\begin{aligned} b_1 &= \exp(2.3288 - 6.4581\phi + 2.4486\phi^2) \\ b_2 &= 0.0964 + 0.5565\phi \\ b_3 &= \exp(4.905 - 13.8944\phi + 18.4222\phi^2 - 10.2599\phi^3) \\ b_4 &= \exp(1.4681 - 12.2584\phi + 20.7322\phi^2 - 15.8855\phi^3) \end{aligned} \quad (22)$$

The particle Reynolds number in equation (21) is based on the equivalent diameter of a sphere: $Re_{spherical} = \rho^g |\mathbf{U}^g - \mathbf{V}_i^p| d_p / \mu^g$ where d_p represents the mean diameter of the abrasive solid particle while the particle shape factor or sphericity in equation (22), defined as $\phi = s/S$ where s is the surface area of a sphere having the same volume as the non-spherical solid particle and S is the actual surface area of the solid particle, depicts the different shapes

of the solid particle that it can possess. A value of unity constitutes a spherical solid particle. Values less than unity signify the increasing non-sphericity of the solid particle.

2.3 Soft sphere collision model

For ease of accounting the collisions of abrasive solid particles within the high speed air flow, the contact force is determined between sphere-like particles. In the soft sphere model described in Figure 1, the contact force on particle a is determined as the sum of the contact forces of all particles in contact with particle a , i.e. all particles b , including walls,

$$\mathbf{F}_a^{contact} = \sum \mathbf{F}_{ab,n} + \mathbf{F}_{ab,t} \quad (23)$$

where $\mathbf{F}_{ab,n}$ and $\mathbf{F}_{ab,t}$ represent the normal and tangential component of the contact forces between particles a and b . The torque in this particular study depends only on the tangential contact force:

$$\mathbf{T}_a = \sum R_a \mathbf{n}_{ab} \times \mathbf{F}_{ab,t} \quad (24)$$

The calculation of the contact force between two particles can generally involve complicated contact mechanics which can dramatically increase the computational burden. In this respect, the use of a simplified contact mechanics model to reduce calculation time while maintaining sufficient accuracy in characterising the collisions is adopted. The linear-spring and dashpot model, which is originally proposed by Cundall and Strack [23] and Tsuji et al. [24], is employed to calculate the contact forces.

The normal component of the contact force between two particles a and b can be calculated by

$$\mathbf{F}_{ab,n} = -k_n \delta_n \mathbf{n}_{ab} - \eta_n \mathbf{V}_{ab,n}^p \quad (25)$$

where k_n is the normal spring stiffness, $\mathbf{n}_{ab}$ is the normal unit vector, η_n is the normal damping coefficient and $\mathbf{V}_{ab,n}^p$ is the normal relative velocity which can be obtained by

$\mathbf{V}_{ab,n}^p = (\mathbf{V}_{ab}^p \cdot \mathbf{n}_{ab}) \mathbf{n}_{ab}$. The relative velocity $\mathbf{V}_{ab}$ is defined as

$$\mathbf{V}_{ab} = (\mathbf{V}_{a,c} - \mathbf{V}_{b,c}) = (\mathbf{V}_a - \mathbf{V}_b) - (R_a \boldsymbol{\Omega}_a - R_b \boldsymbol{\Omega}_b) \times \mathbf{n}_{ab} \quad (26)$$

with the normal unit vector defined as $\mathbf{n}_{ab} = (\mathbf{r}_a - \mathbf{r}_b) / |\mathbf{r}_a - \mathbf{r}_b|$. The overlap δ_n in equation (25) is given by

$$\delta_n = R_a + R_b - |\mathbf{r}_a - \mathbf{r}_b| \quad (27)$$

The normal damping coefficient is given by

$$\eta_n = \frac{-2 \ln e_n \sqrt{m_{ab} k_n}}{\sqrt{\pi^2 + \ln^2 e_n}} \quad (28)$$

where $m_{ab} = (1/m_a + 1/m_b)^{-1}$.

The tangential component of the contact force based on the consideration of the Coulomb friction law can be determined by

$$\mathbf{F}_{ab,t} = \begin{cases} -k_t \delta_t - \eta_t \mathbf{V}_{ab,t}^p & \text{if } |\mathbf{F}_{ab,t}| \leq \mu |\mathbf{F}_{ab,n}| \\ -\mu |\mathbf{F}_{ab,n}| \mathbf{t}_{ab} & \text{if } |\mathbf{F}_{ab,t}| > \mu |\mathbf{F}_{ab,n}| \end{cases} \quad (29)$$

where k_t is the tangential spring stiffness, δ_t is the tangential displacement, η_t is the tangential damping coefficient, μ is the coefficient of friction and $\mathbf{V}_{ab,t}^p$ is the tangential relative velocity which is defined as $\mathbf{V}_{ab,t}^p = \mathbf{V}_{ab}^p - \mathbf{V}_{ab,n}^p$. If the tangential force is limited by the Coulomb friction, the tangential displacement is given by

$$\delta_t = \frac{\mu |\mathbf{F}_{ab,n}| \mathbf{t}_{ab}}{k_t} \quad (30)$$

The particle's normal and tangential spring stiffness can be related to each other via a Poisson ratio ν :

$$\frac{k_t}{k_n} = 2 \frac{(1-\nu)}{(2-\nu)} \quad (31)$$

3. Numerical Details

A schematic diagram of the computational domain adopted in this work is shown in Figure 2. Finite volume method is employed to discretise the gas-phase governing equations. The advection terms in the equations governing the conservation of mass, momentum and energy and turbulence transport of turbulent kinetic energy and dissipation of turbulent kinetic energy are approximated by the second order upwind differencing scheme while the diffusion terms

are approximated via the second order central differencing scheme. Linkage between the pressure and velocity is achieved through the PISO method developed by Issa [31] to enhance the convergence of the pressure. The time derivative terms in all of the equations are approximated via a bounded second order differencing scheme. An implicit procedure is adopted to obtain the transient solution of the high speed air flow.

Translational and rotational motions of the solid particles are solved via an Euler implicit time integration. The coupling between the gas and solid phases **which is schematically shown in Figure 3** can be described in the following. In the beginning, the flow field of the gas phase is resolved by the numerical solver such as the Algebraic Multi-Grid (AMG). When the transient calculation of the gas phase governing equations is iterated to convergence for a time step, the solution of the flow field provides the necessary means of determining the particle-fluid forces acting on each particle. Before the particle positions can be updated, all possible collisions between particles are detected and collision dynamics is computed for each occurring collision via the soft sphere collision model. The new particle positions are subsequently used to ascertain the momentum exchange which is added to each of the mesh cells to represent the interphase interaction between the solid and gas phases.

Figure 4 shows the mesh of the computational domain which has been described in Figure 2 alongside with the cross-sectional plan view cutting through the cylindrical nozzle. A finer mesh is concentrated around the nozzle to resolve the high gradients of the fluid flow in the gas phase. The total number of polyhedral cells generated for the entire three-dimensional domain is 75,817. A grid sensitivity analysis is performed by doubling the number of cells to 151,634. The difference for the simulation results is found to be less than 3%. Thus, the computational domain containing 75,817 polyhedral cells has been chosen to determine the numerical results in this work.

Detailed physical and numerical parameters chosen in the simulation of AAJ are listed in Table 1. The contact time in the normal direction can be determined by

$$\Delta t_{contact,n} = \sqrt{m_{ab} \frac{\pi^2 + \ln^2 e_n}{k_n}} \quad (32)$$

For the numerical calculation of particle positions of the solid particles, the time step should be sufficiently small in order to ensure that the contact between particles persists over a number of time steps to overcome the problems concerning the energy conservation due to the numerical integration. In the current study, a particle time step of 5×10^{-8} s, which is much smaller than the contact time in the normal direction of 6.0×10^{-7} s, has been adopted. It should be noted that the normal spring stiffness can be determined from Young's modulus but since the Young's modulus for the abrasive particle yields a very high value, which implies the use of an even smaller particle time step of 5×10^{-8} s, it is thus found to be undesirable from a computational perspective. The value of the normal spring stiffness selected in this work is set much lower; the normal overlap is kept small, typically below 1% of the particle diameter, ensuring that the predicted dynamics are not affected. For the numerical calculation of the gas phase variables, a fluid flow time step of 10^{-6} s is adopted. Adequate accuracy is achieved by 20 steps of the numerical calculation of particle positions with one step of the numerical calculation of the gas phase variables. This problem that is solved through ANSYS Fluent v14.5 with attached User-Defined Functions (UDFs) for the soft sphere collision model reaches a pseudo steady state condition within 0.003 s; numerical results are thereafter extracted.

Abrasive particles of a mean size of $27 \mu\text{m}$ are introduced at the nozzle inlet of which they are injected through the nozzle with a speed of 40 m/s of which has been confirmed in the theoretical analysis carried out by Li et al. [2]. It should be noted that the nozzle that has been used in the experiment was simply a cylinder connected to a reservoir of abrasive particles. In order to reduce the computational effort, the nozzle is only included in the

simulation but not the reservoir. The injection of particles can be characterised in the following. The number of particles to be injected can be determined via $N_{injection} = \dot{m}_{particle} \Delta t_{injection} / m_{particle}$ where $\dot{m}_{particle}$ is the particle mass flow rate, $\Delta t_{injection}$ is the injection time step and $m_{particle}$ is the mass of single particle. Based on the mean diameter of $27 \mu\text{m}$, mass flow rate of 0.0001 kg/s and injection time step taken to be the fluid flow time step of $1 \times 10^{-6} \text{ s}$, $N_{injection} \approx 3$. The particles are released continuously at each fluid flow time interval of $1 \times 10^{-6} \text{ s}$ through the nozzle inlet such as described in Figure 2 with a prescribed angle of $\psi = 30^\circ$ from the vertical axis. Different angles of 45° and 60° are tested and the results show that no appreciable difference on the distributions of the axial centreline particle velocity at the pseudo steady state condition for nozzle diameters of 0.36 mm and 0.46 mm and operating pressures of 0.43 MPa and 0.69 MPa . The angle of $\psi = 30^\circ$ is thus chosen for the entire numerical calculations. These particles are subsequently accelerated by the air flow within the nozzle. Air pressures corresponding to operating pressures of 0.43 MPa and 0.69 MPa are imposed at the nozzle inlet for the high speed air flow. No-slip condition is imposed at the nozzle wall while Neumann's condition is applied at the open boundaries (see Figure 2), i.e. $\partial\phi/\partial n = 0$.

4. Experimental Details

A schematic layout of the experimental setup for the measurement of particle velocities in micro-abrasive air jet is shown in Figure 5(a). Utilising the abrasives as seed particles, visualisation of the flow patterns and structures such as the jet flow expansion characteristics, and quantitative full-field instantaneous velocity data in terms of the particle velocity distribution along the axial and radial directions of jet were obtained via the Particle Image Velocimetry (PIV) technique. PIV is considered to be a non-intrusive laser-optical method for

a fast acquisition of a flow field. A laser generates a thin light sheet inside the flow. With a pulsed laser, a cross-correlation camera acquires two consecutive images of particles transported with the flow. The abrasive particles have a displacement proportional to the flow velocity. With the known pulse distance and the calibrated scaling factor of the camera, the flow velocity is determined by a specialised software. A detailed description of the PIV measurement technique can be found in Fan et al. [32]. The PIV system used in this study comprised a PIV synchronizer by ILA GmbH, a Gemini PIV 15 Nd:YAG laser system, and a SensiCam imaging system. An S.S. White Model K Series II Airabrasive jet machine was used to generate a micro-abrasive air jet. The major parameters used in the experiment align with those used in the **CFD-DEM analysis** given in Table 1. Velocity measurements were determined across an axial length of 25 mm downstream from the nozzle exit. A photograph image illustrating the **complex** irregular shapes of abrasive particles utilised during the experiment can be seen in Figure 5(b).

5. Results and Discussion

5.1 Observed and predicted structure of jet flow

Structures of particle jet flow provide important information about the jet evolution and expansion as well as the distribution of abrasive particles. Figures 6 and 7 present the side views of the measurements of particle jet flow and the predictions made with different particle shape factors ranging from $\phi = 0.6$ to $\phi = 1.0$ for the two nozzle inner diameters under the operating pressure of 0.43 MPa. **Side views of the measured and predicted particle jet flow for the two nozzle diameters under the operating pressure of 0.69 MPa are similar to those shown in Figures 6 and 7 for the operating pressure of 0.43 MPa and thus not presented herein.** All of these images demonstrate a very similar pattern of the particle jet flow. After leaving the

nozzle, an almost symmetrical structure to the jet axis and a nearly linear expansion of the particle jet flow is observed downstream from the nozzle exit. This expansion is attributed by the free jet flow that is subjected to the entrainment of the surrounding air (see Figures 9-12) and the abrasive particles are being transported through the jet flow away from the nozzle.

An angle denoted by θ for an abrasive jet such as illustrated in Figure 2 is defined to characterise the particle jet expansion. Figures 8 and 9 depict the measured and predicted expansion angles under different test conditions and particle shape factors. For each nozzle diameter, the expansion angle exhibits an increasing trend with the increase of air pressure. For the measured data, the expansion angle is determined to be about 3.6° which is consistent with core jet angle of 3.5° ascertained by Achtsnick [33]. It should be noted that the effect of nozzle diameter has a very small effect on the expansion angle. For the predicted data, although an increasing trend with the increase of air pressure is exhibited for different particle shape factors, the assumption of perfectly spherical abrasive particles ($\phi = 1.0$) adopted in the Lagrangian tracking of solid particles yields a very wide expansion angle which is almost 1.5 times the measured expansion angle. By characterising the solid particles to be non-spherical, the expansion angle of the particle jet reduces dramatically with a decrease in the particle shape factor, which is comparable to the measured expansion angle. It can thus be inferred that the shape characterisation of solid particles plays an important role in the numerical calculation of abrasive jet flow.

Figures 10 and 11 illustrate the cross-sectional planes of normalised velocity vectors and velocity contours of the surrounding air flow under different test conditions for $\phi = 0.6$ and $\phi = 1.0$ and the nozzle diameter of 0.36 mm under two operating pressures. Cross-sectional planes of normalised velocity vectors and velocity contours of the surrounding air flow under different shape factors for the nozzle diameter of 0.46 mm under two operating pressures are similar to those shown in Figures 10 and 11 and thus not presented herein. Complex flow structures are observed near the cylindrical nozzle. The expansion of the free air jet and the

entrainment of the surrounding air are clearly captured through the numerical calculation of AAJ. Maximum air velocities are predicted to be 480 m/s for air pressure of 0.43 MPa and 510 m/s for air pressure of 0.69 MPa, respectively; the variation of nozzle diameter has a negligible effect on the predicted maximum air velocities. As the size of the free jet increases steadily from the nozzle exit, the entrainment of the surrounding air can be seen to exert a greater influence on the AAJ at some distance downstream from the nozzle. It is apparent that the shape factor of the solid particles has no discernible effect on the high speed air flow. The particle shape factor only influences the transport of solid particles which subsequently affects the particle jet expansion.

5.2 Analysis of measured and predicted particle velocities

Tables 3 and 4 present the statistical information of the measured and predicted particle velocities under different test conditions and particle shape factors for the two nozzle diameters considered. The analysis includes the arithmetic mean, median, minimum, maximum values of the particle velocity and the standard deviation. It can be seen that the measured particle velocities increase with the air pressure. For the predicted data, the increasing rate is more pronounced for the $\phi = 0.6$. While the predicted particle velocities for $\phi = 0.6$ are consistently higher than the measured particle velocities for the lower pressure of 0.43 MPa, the difference for the pressure of 0.69 MPa is only marginal for the two nozzle diameters. The discrepancy between the measured and predicted particle velocities could be attributed to the consideration of mean particle size for the abrasives used in the current numerical calculations. For $\phi = 0.8$ and $\phi = 1.0$, the predicted particle velocities can be seen to be much lower than the measured particle velocities under different test conditions.

The increase in particle sharpness from $\phi = 1$ to $\phi = 0.6$ can be seen to improve the prediction accuracy of the particle velocities. One possible explanation for this may be the

increased drag force for non-spherical particles. The drag force coefficient is related to the negative pressure gradient present across the particle surface, which is higher for non-spherical particles. Thus, the increase in negative pressure gradient on a non-spherical particle surface is related to the early boundary layer separation of the flow from the object surface. The more aerodynamic and smooth surface of a spherical particle encourages the boundary layer to remain attached to the surface for longer, which reduces the negative pressure gradient. In this work, the increase in drag coefficient due to particle shape factor is correlated to equations (21) and (22). As a result, the increased drag for less spherical particles enables them to reside more centrally where the velocity is higher, which gives a greater increase in momentum to the particles. Numerical results in [Tables 3](#) and [4](#) clearly attest to the requirement of accommodating the shape of solid particles into the numerical calculation of the AAJ in order to attain the appropriate velocities. The lower than expected particle velocities as a result of the assumption of spherical solid particles ($\phi = 1.0$) confirms the invalid consideration since the drag force is incorrectly determined. The increase in the drag force through the increasing particle sharpness from $\phi = 0.8$ to $\phi = 0.6$ results in higher particle velocities which are more comparable to the measured particle velocities. This also closely correlates to the prediction of the jet expansion angle through the consideration of non-spherical solid particles as discussed in the previous section.

Representative histograms of the predicted particle velocities for nozzle diameters of 0.36 mm and 0.46 mm under the air pressures of 0.43 MPa and 0.69 MPa for $\phi = 0.6$ are shown in [Figures 13](#) and [14](#). It can be seen that the distribution of the particle velocities is skewed towards the right so that the mean is always lower than median (see [Tables 4](#) and [5](#)). The distribution for the different nozzle diameters and air pressure considered is thus a negative skewed distribution rather than a Gaussian normal distribution. In comparison to the experiments carried out by Fan et al. [\[32\]](#), the histogram of predicted particle velocities is shown to be in good agreement with the histogram of measured particle velocities.

5.3 Measured and predicted particle velocity distributions along the jet axis

For the high speed flow of abrasive particles in air which is essentially a two-phase gas-particle flow, the measured distribution of particle velocities in the jet centreline demonstrate an initial acceleration stage whereby the velocities continue to increase in the vicinity of the nozzle exit, a transition stage in which the velocities peak and a final stage where the particles decelerate. Figures 13-16 illustrate the complete evolution of the measured particle velocities for different nozzle diameters and air pressures accompanied by predicted particles velocities of different shape factors from the point of nozzle exit through an axial length of 25 mm downstream. It is clear that particle velocity increases with air pressure which are reflected in the measurements as well as in the numerical predictions.

For the nozzle diameter of 0.36 mm, the acceleration stage persists about 4 mm while for the nozzle diameter of 0.46 mm, it is about 6 mm, as depicted by the measured centreline particle velocities. Nevertheless, it is found that the predicted centreline particle velocities increase more sharply when compared to the measured centreline particle velocities. For the nozzle diameter of 0.36 mm, the acceleration stage persists about 2 mm while for the nozzle diameter of 0.46 mm, it is about 3 mm. As can be seen from Figure 17(a) that the centreline air velocities for the two nozzle diameters increase dramatically and peak just after exiting the nozzle and drop beyond this point. Since the air velocities are much higher than those of the particle velocities in this region, large slip velocities are introduced which in turn result in large drag forces acting on the particles (see equation (19)). The particles are accelerated until the air and particle velocities are comparable beyond the axial distance of 3 mm of which the transition stage becomes prevalent thereafter. By decreasing the particle sphericity from $\phi = 1$ to $\phi = 0.6$, it can be seen that the drag coefficient C_D increases as a function of the particle Reynolds number $Re_{spherical}$ such as depicted in Figure 17(b). A small value of the particle

sphericity results in the significant increase of the drag forces acting on the particles; higher particle velocities are thus obtained. One possible explanation of the discrepancy between the measured and predicted centreline particle velocities could be due to the early peaking of the predicted air velocities just after the nozzle exit during the numerical calculation which subsequently promotes the early occurrence of the acceleration stage. Another possible explanation could be the consideration of a mean particle size adopted in the numerical calculation. For cases of $\phi = 0.6$, in which the centreline particle velocity distributions are more comparable to the the measured velocities, it would be expected that a particle with size below the mean value when collides with a particle larger than the mean value (with a lower velocity) could be decelerated which may result in a much lower velocity and an extension of the acceleration stage.

It can be seen that the measured centreline particle velocities peak after the acceleration stage. This could be explained by the particle and air velocities being close to each other so that the air jet could not provide any acceleration to the particle flow at this transition stage. A similar behaviour is observed for the predicted centreline particle velocities at different particle shape factors. In the final stage, the measured as well as the predicted centreline particle velocities decrease due to the surrounding air entrainment. It can be seen that for the air pressure of 0.69 MPa and $\phi = 0.6$, the predicted centreline particle velocities are marginally lower than the measured centreline particle velocities at the axial length of 25 mm downstream from the nozzle exit for the two nozzle diameters. Here again, the discrepancy could be due to the consideration of a mean particle size adopted in the numerical calculation.

A comparison of particle velocity distributions along the jet radial direction at different axial distances is shown for the nozzle diameter of 0.36 mm for $\phi = 0.6$ under the operating pressures of 0.43 MPa and 0.69 MPa in Figures 18 and 19 respectively. Measured and predicted radial velocity profiles at six axial distances (1, 5, 10, 15, 20, 24.5mm) are shown. Overall, the variation of particle velocities for the numerical calculation along the jet radial

direction is predicted to be rather similar to the experimental particles velocity profiles across the six axial distances. The trend of the jet expansion is well replicated by the numerical calculation when compared to the experimental data. On the jet cross-section at a smaller axial distances especially near the nozzle exit, the variation of particle velocities along the jet radial direction is small. Nevertheless, larger variation of the particle velocities is experienced at large axial distances especially near the bottom open boundary.

5.4 Phenomenological discussion of the characteristics of solid particles in abrasive jet flow

The determination of an optimum abrasive jet property suitable to machining applications requires the understanding of the characteristics of the solid particles. In general, the particle size, shape, surface roughness and porosity are the key properties. The particle shape as has been demonstrated in the numerical results has been found to play an important role in affecting the drag characteristics on the aerodynamic behaviour of the solid particles by allowing them to remain more centrally along the jet axis. In practice, abrasive particles exhibit distinct irregular shapes, which make the characteristic measure of particles extremely difficult. Table 6 shows the different sphericity values of regular and irregular particles [34]. It can be seen that shape factors of 0.6, 0.8 and 1.0 investigated in this work correspond to the sphericity of “edged”, “rounded” and “sphere” solid particles. It should be noted that the drag characteristics of the abrasive particles in this work retain the equivalent spherical shape for the ease of accommodating the particle-particle collisions via the soft sphere collision model. The challenge is to directly correlate the actual particle shape of the abrasive particle to the sphericity definition in the determination of the drag coefficient. One possible way to design the shape of an abrasive particle is through the use of a technique characterising the clumping of a number of different sizes of spherical particles together to form the approximate shape [35]. In this way, the soft sphere collision model remains applicable for the particle-particle

collisions but the irregular shape of the abrasive particle is now accounted in the proper determination of the drag force. In addition, the consideration of the distribution of particle sizes is imperative in determining the particle jet characteristics instead of the mean particle size adopted in this investigation. A wide range of the size distribution of the abrasive particles has the advantage of which the porosity can be reduced and the distribution in the dispersed system yields the desired uniform pattern during the deburring of micro workpieces. The challenge in the numerical calculation of the AAJ is to associate the distinct shape with the particular size of the abrasive particle. Essential properties of the abrasive medium and particle jet characteristics will be further investigated and analysed in our future work with respect to the particle size distribution and different particle shapes.

6. Conclusions

Investigating the relevant process parameters governing the dispersed flow system in AJM requires an understanding of the properties of the abrasive particles. A DEM-CFD analysis has been carried out to probe into the dynamic behaviour of abrasive particles in high speed air flows. Particle sphericity which can be characterised via the consideration of shape factor has been found to play a crucial role in dramatically affecting the drag characteristics on the aerodynamic behaviour of the solid particles. The increase in sharpness of the shape factor from $\phi = 1$ to $\phi = 0.6$ has been seen to improve the predicted particle velocities when compared to the measured velocities along the jet centreline axis. This was mainly due to the increased drag force and resulted in the negative pressure gradient present across the particle surface that was higher for non-spherical particles rather than for spherical particles. However, it has been shown that particle sphericity did not significantly influence the flow field of the air jet and the variation of nozzle sizes has no effect on the maximum air velocities being predicted by the DEM-CFD simulation. Future work in the DEM-CFD

modelling of AAJ will include the association of appropriate distinctive shape with the size distribution of the abrasive particles to improve the prediction of the particle velocities during the initial acceleration stage and the transition stage in which the velocities peak and decelerate thereafter.

Nomenclature

a	speed of sound
A_o, A_s	model constants
C_p	specific heat of constant pressure
C_μ	variable constant in equation (16)
C_1	variable constant in equation (15)
C_D	drag coefficient
d	mean diameter
D	domain diameter
D_{nozzle}	nozzle inner diameter
e_n	coefficient of normal restitution
e	specific internal energy
$\mathbf{F}_i^{contact}$	contact force
$\mathbf{F}_i^{drag}$	drag force
$\mathbf{F}_{ab,n}$	normal component of contact forces between particles a and b
$\mathbf{F}_{ab,t}$	tangential component of contact forces between particles a and b
$\mathbf{g}$	gravitational vector
G	generation of kinetic energy due to buoyancy
H	domain height, specific enthalpy
H_{nozzle}	nozzle length
I	moment of inertia
k	turbulent kinetic energy
k_n	normal spring stiffness
k_t	tangential spring stiffness
$\dot{m}$	flow rate
$\mathbf{n}_{ab}$	normal unit vector
N^p	particle number
Pr_T	turbulent Prandtl number
p_{inlet}	nozzle inlet pressure
p	pressure
P	shear production term
$\mathbf{q}_H''$	Reynolds flux term
$\mathbf{r}_i$	position vector of a single particle of element i
$Re_{spherical}$	particle Reynolds number based on the equivalent diameter of a sphere
R	radius of spherical particle

S_M^g	fluctuating dilatation term in compressible turbulence
S	actual surface area of the solid particle
s	surface area of a sphere having the same volume as the non-spherical solid particle
$\mathbf{S}$	source term due primarily to particle-fluid drag interaction
$\mathbf{T}$	torque vector
U^*	parameter evaluated by $\sqrt{S_{ij}^2 + \tilde{Q}_{ij}^2}$
$\mathbf{U}$	velocity vector
$\mathbf{V}_{ab}$	relative velocity between particles a and b
$\mathbf{V}_i$	velocity vector for element i
$\mathbf{V}_{ab,t}^p$	tangential relative velocity between particles a and b
$\mathbf{V}_{ab,n}^p$	normal relative velocity between particles a and b
V	volume

Greek symbols

α	volume fraction
β	inter-phase momentum transfer coefficient
θ	particle jet expansion angle
ϕ	particle shape factor or sphericity defined as $\phi = s/S$
δ	delta function
δ_n	overlapping distance between particles a and b
$\Delta t_{contact,n}$	contact time in the normal direction
η_n	normal damping coefficient
η_t	tangential damping coefficient
μ	coefficient of friction
μ_T	turbulent or eddy viscosity
ν	Poisson ratio
τ''	Reynolds stress flux term
ρ	density
ε	dissipation of turbulent kinetic energy
λ	thermal conductivity

Subscripts

i	index
n	normal
t	tangential

Superscripts

g	gas
p	abrasive particle

References

- [1] J.M. Fan, C.Y. Wang, J. Wang, Modelling the erosion rate in micro abrasive air jet machining of glasses, *Wear* 266 (2009) 968–974.
- [2] H.Z. Li, J. Wang, J.M. Fan, Analysis and modelling of particle velocities in micro-abrasive air jet, *International Journal of Machine Tools and Manufacturing*, 49 (2009) 850-858.
- [3] J. Wang, *Abrasive Waterjet Machining of Engineering Materials*, Trans Tech Publications, Uetikon-Zuerich, Switzerland. 2003.
- [4] M.A. Verspui, G. de With, A. Corbijn, P.J. Slikkerveer, Simulation model for the erosion of brittle materials, *Wear* 233–235 (1999) 436–443.
- [5] M. Achtsnick, P.F. Geelhoed, A.M. Hoogstrate, B. Karpuschewski, Modelling and evaluation of the micro abrasive blasting process, *Wear* 259 (2005) 84–94.
- [6] A. Ghobeity, M. Papini, J.K. Spelt, Computer simulation of particle interference in abrasive jet micromachining, *Wear* 263 (2007) 265–269.
- [7] B. Karpuschewski, A.M. Hoogstrate, M. Achtsnick, Simulation and improvement of the micro abrasive blasting process, *CIRP Annals* 53 (1) (2004) 251–254.
- [8] A.P. Verma, G.K. Lal, A theoretical study of erosion phenomenon in abrasive jet machining, *Transactions of ASME, Journal of Manufacturing Science and Engineering*, 118 (1996) 564–570.
- [9] S., Ally, J.K., Spelt, and M., Papini, Prediction of Machined Surface Evolution in the Abrasive Jet Micro-machining of Metals, *Wear* 292-293 (2012) 89-99.
- [10] R. Haj Mohammad Jafar, M., Papini and J.K., Spelt, Simulation of Erosive Smoothing in the Abrasive Jet Micro-machining of Glass, *Journal of Materials Processing Technology*, 213 (2013) 2254-2261.

- [11] R. Haj Mohammad Jafar, J.K., Spelt and M., Papini, Surface Roughness and Erosion Rate of Abrasive Jet Micro-machined Channels: Experiments and Analytical Model, *Wear*, 303 (2013) 138-145.
- [12] A. Momber, Blast Cleaning Technology, Springer-Verlag, Berlin, Heidelberg, 2008.
- [13] J.A.C. Humphrey, Fundamentals of fluid motion in erosion by solid particle impact, *International Journal of Heat and Fluid Flow*, 11 (3) (1990) 170–195.
- [14] K. Yanaida, Flow characteristics of water Jets, *Proceedings of the Symposium on Jet Cutting*, (1974) 20–32.
- [15] M. Hashish, M.P. du Plessis, Prediction equations relating high velocity jet cutting performance to stand off distance and multipasses, *ASME Journal of Engineering for Industry*, 101 (1979) 311–319.
- [16] N.F. Neusen, T.J. Gores, R.S Amano, Axial variation of particle and drop velocities downstream from an abrasive water jet mixing tube, *Proceedings of the 12th Conference on Jet Cutting Technology*, (1994) 93–103.
- [17] K.F. Neusen, D.G. Alberts, T.J. Gores, Distribution of mass in a three-phase abrasive water jet using scanning X-ray densitometry, *Proceedings of the 10th Conference on Jet Cutting Technology*, (1990) 83–98.
- [18] J. Ye, R. Kovacevic, Turbulent solid–liquid flow through the nozzle of premixed abrasive water jet, *Proceedings of the Institute of Mechanical Engineering, Part B*, 213 (1999) 59–67.
- [19] H. Liu, J. Wang, N. Kelson and R.J. Brown, A study of abrasive waterjet characteristics by CFD simulation, *Journal of Materials Processing Technology*, 153-154 (2004) 488-493.

- [20] T. Burzynski and M. Papini, Measurement of the particle spatial and velocity distributions in micro-abrasive jets. *Measurement Science and Technology*, 22 (2011) 02510415.
- [21] T. Burzynski and M. Papini, Analytical model of particle interference effects in divergent erosive jets, *Tribology International*, 43 (2010) 554-567.
- [22] G.H. Yeoh, J.Y. Tu, *Computational techniques for multiphase flows – basics and applications*, Butterworth-Heinemann, Elsevier Science and Technology, 2009.
- [23] P.A. Cundall, O.D.L. Strack, A discrete numerical model for granular assemblies, *Geotechnique*, 29 (1979) 47–65.
- [24] Y. Tsuji, Y. Kawaguchi, T. Tanaka, Discrete particle simulation of two dimensional fluidized bed, *Powder Technology*, 77 (1993) 79-81.
- [25] J.D. Anderson, *Computational fluid dynamics – the basics with applications*, McGraw-Hill, 1995.
- [26] M. Lopez de Bertodano, R.T.Jr. Lahey, O.C. Jones, Phase distribution in bubbly two-phase flow in vertical ducts, *International Journal of Multiphase Flow*, 20 (1994) 805-818.
- [27] M. Lopez de Bertodano, R.T.Jr. Lahey, O.C. Jones, Development of a $k-\varepsilon$ model for bubbly two-phase flow, *ASME Journal of Fluids Engineering*, 116 (1994) 128-134.
- [28] T.-H. Shih, W.W. Liou, A. Shabbir, Z. Yang, J. Zhu, A new k-eddy viscosity model for high Reynolds number turbulent flows - model development and validation, *computers and fluids*, 24 (3) (1995), 227-238.
- [29] S. Sarkar, L. Balakrishnan, Application of a Reynolds-stress turbulence model to the compressible shear layer, ICASE Report 90-18NASA CR 182002, 1990.

- [30] A. Haider, O. Levenspiel, Drag coefficient and thermal velocity of spherical and nonspherical particles, *Powder Technology*, 58 (1989) 63-70.
- [31] R.I. Issa, Solution of the Implicitly Discretised Fluid Flow Equations by Operator-Splitting, *Journal of Computational Physics*, 62 (1986) 40-65.
- [32] J.M. Fan, H.Z. Li, J. Wang, C.Y. Wang, A study of the flow characteristics in micro-abrasive jets, *Experimental Thermal and Fluid Science* 35 (2011) 1097–1106.
- [33] M. Achtsnick, P.F. Geelhoed, A.M. Hoogstrate, B. Karpuschewski, Modelling and evaluation of the micro abrasive blasting process, *Wear* 259 (2005) 84–94.
- [34] W.-C. Yang, Particle characterization and dynamics, in *Handbook of Fluidization and Fluid-Particle System*, W.-C. Yang (Ed.), Marcel Dekker, New York, 2003.
- [35] J.-P. Latham, A. Munjiza. The modelling of particle systems with real shapes. Theme issue of *Philosophical Transactions of the Royal Society*, 362 (1822) (2004) 1953–1972.

Figure captions

- Figure 1 Coordinate system used in the description of particle-particle collisions in the soft sphere collision model
- Figure 2 Schematic diagram of the computational domain for the abrasive air jet simulation
- Figure 3 Coupling and information exchange between CFD and DEM
- Figure 4 Mesh distribution: (a) Three-dimensional view of the computational domain and (b) cross-sectional view
- Figure 5 (a) Schematic diagram of the experimental setup and (b) Typical shapes of abrasive medium
- Figure 6 Images of measured and predicted particle jet flow for air pressure of 0.43 MPa and nozzle diameter of 0.36 mm
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- Figure 8 Comparison between measured and predicted jet expansion angle under different air pressures: (a) nozzle diameter of 0.36 mm and (b) nozzle diameter of 0.46 mm
- Figure 9 Predicted normalised velocity vectors and velocity contours for air pressure of 0.43 MPa and nozzle diameter of 0.36 mm
- Figure 10 Predicted normalised velocity vectors and velocity contours for air pressure of 0.69 MPa and nozzle diameter of 0.36 mm
- Figure 11 Histogram of predicted particle velocities for $\phi = 0.6$ and nozzle diameter of 0.36 mm: (a) 0.43 MPa (Mean velocity of 127.77 m/ with standard deviation of 9.76) and (b) 0.69 MPa (Mean velocity of 138.44 m/ with standard deviation of 11.01)

Figure 12 Histogram of predicted particle velocities for $\phi = 0.6$ and nozzle diameter of 0.46 mm: (a) 0.43 MPa (Mean velocity of 137.48 m/ with standard deviation of 9.08) and (b) 0.69 MPa (Mean velocity of 150.64 m/ with standard deviation of 9.39)

Figure 13 Comparison of measured and predicted centreline particle velocities for air pressure of 0.43 MPa and nozzle diameter of 0.36 mm: (a) $\phi = 0.6$, (b) $\phi = 0.8$ and $\phi = 1.0$

Figure 14 Comparison of measured and predicted centreline particle velocities for air pressure of 0.69 MPa and nozzle diameter of 0.36 mm: (a) $\phi = 0.6$, (b) $\phi = 0.8$ and $\phi = 1.0$

Figure 15 Comparison of measured and predicted centreline particle velocities for air pressure of 0.43 MPa and nozzle diameter of 0.46 mm: (a) $\phi = 0.6$, (b) $\phi = 0.8$ and $\phi = 1.0$

Figure 16 Comparison of measured and predicted centreline particle velocities for air pressure of 0.69 MPa and nozzle diameter of 0.46 mm: (a) $\phi = 0.6$, (b) $\phi = 0.8$ and $\phi = 1.0$

Figure 17 (a) Predicted centreline air velocities for air pressures of 0.43 MPa and 0.69 MPa and (b) Drag coefficient C_D as function of $Re_{spherical}$ for different shape factors

Figure 18 Comparison of measured and predicted centreline particle velocities for air pressure of 0.43 MPa with nozzle diameter of 0.36 m for $\phi = 0.6$.

Figure 19 Comparison of measured and predicted centreline particle velocities for air pressure of 0.69 MPa with nozzle diameter of 0.36 m for $\phi = 0.6$.

Figure 1

Figure 2

Figure 3

(a)

(b)

Figure 4

(a)

(b)

Figure 5

Figure 6

Figure 7

(a)

(b)

Figure 8

$$\phi = 0.6$$

$$\phi = 1.0$$

Figure 9

$\phi = 0.6$

$\phi = 1.0$

Figure 10

(a)

(b)

Figure 11

(a)

(b)

Figure 12

Figure 13

Figure 14

Figure 15

Figure 16

(a)

(b)

Figure 17

Figure 18

Figure 19

Table captions

Table 1 Physical and numerical parameters of abrasive air jet

Table 2 Statistical summary of measured and predicted particle velocities for nozzle diameter of 0.36 mm

Table 3 Statistical summary of measured and predicted particle velocities for nozzle diameter of 0.46 mm

Table 4 Shape factors of irregular and regular particles

Table 1

Nozzle inlet air pressure, p_{inlet}^g (MPa)	0.43 and 0.69
Nozzle inner diameter, D_{nozzle} (mm)	0.36 and 0.46
Nozzle length, H_{nozzle} (mm)	7
Domain height, H (mm)	37
Domain diameter, D (mm)	15
Gas viscosity, μ^g (Pa/s)	17.894×10^{-6}
Gas specific heat, C_p^g (J/kg K)	1006
Gas thermal conductivity, λ^g (W/m K)	0.0242
Abrasive	Alumina
Abrasive mean diameter, d^p (μm)	27
Abrasive flow rate, $\dot{m}^p$ (g/min)	6
Abrasive density, ρ^p (kg/m^3)	3970
Abrasive Poisson ratio, ν	0.21
Abrasive specific heat, C_p^p (J/kg K)	880
Normal spring stiffness, k_n (N/m)	1000
Coefficient of normal restitution, e_n	0.92
Coefficient of friction, μ	0.5
Room temperature, ($^{\circ}\text{C}$)	17

Table 2

Nozzle ID 0.36 mm				
Pressure 0.43 MPa				
	Measurement	$\phi = 0.6$	$\phi = 0.8$	$\phi = 1.0$
Mean velocity (m/s)	115.98	127.77	98.48	76.00
Median velocity (m/s)	117.18	128.68	107.38	76.68
Min velocity (m/s)	91.05	100.49	95.45	31.46
Max velocity (m/s)	133.31	146.29	120.09	88.39
Standard deviation	7.15	9.76	5.48	5.70
Pressure 0.69 MPa				
	Measurement	$\phi = 0.6$	$\phi = 0.8$	$\phi = 1.0$
Mean velocity (m/s)	142.30	138.44	117.94	83.83
Median velocity (m/s)	142.99	139.18	118.29	84.13
Min velocity (m/s)	107.51	109.86	98.16	54.18
Max velocity (m/s)	165.12	159.37	131.14	95.87
Standard deviation	8.02	11.01	5.80	5.39

Table 3

Nozzle ID 0.46 mm				
Pressure 0.43 MPa				
	Measurement	$\phi = 0.6$	$\phi = 0.8$	$\phi = 1.0$
Mean velocity (m/s)	118.34	137.38	110.39	84.71
Median velocity (m/s)	120.34	137.84	111.25	85.93
Min velocity (m/s)	85.76	99.95	79.30	53.09
Max velocity (m/s)	136.91	154.51	125.87	95.13
Standard deviation	8.26	9.08	7.33	6.44
Pressure 0.69 MPa				
	Measurement	$\phi = 0.6$	$\phi = 0.8$	$\phi = 1.0$
Mean velocity (m/s)	151.89	150.64	122.62	86.78
Median velocity (m/s)	154.52	151.78	123.98	88.42
Min velocity (m/s)	98.08	99.41	91.97	44.11
Max velocity (m/s)	179.80	166.44	134.83	100/56
Standard deviation	12.87	9.39	6.77	8.56

Table 4

Irregular particles				
ϕ	rounded ~ 0.8	angular ~ 0.7	edged ~ 0.6	flat ~ 0.5
Regular particles				
ϕ	sphere = 1.0	cube = 0.806	tetrahedral = 0.67	

Abstract

In production technology, abrasive particles are increasingly being utilised in abrasive jet micromachining. Despite of the many phenomenological investigations, the resolution of the flow field and the description of physical interactions between the carrying and abrasive media have not been thoroughly understood. In this paper, a discrete element method-computational fluid dynamics (DEM-CFD) analysis is performed to better understand the dynamic characteristics of high speed abrasive air jet. Abrasive particles of micron sizes within the air jet are examined to determine the jet evolution and expansion as well as the particle distribution within the flow. In particular, particle sphericity (shape factor) is considered for the particle-fluid interaction in addition to the particle-particle collision. Air and particle velocities for the flow downstream from the nozzle exit are simulated under transient, turbulent, two-phase flow conditions and different inlet conditions. Simulations of the particle flow characteristics using a range of particle shape factors are compared against experimental data with different inlet pressures and nozzle diameters. It is found that the predicted results agree well with the experimental data when particle shape factors of 0.6 and 0.8, which correspond to “edged” and “rounded” abrasive particles. With the consideration of lower shape factors, the increased in the aerodynamic drag resulted in higher particle velocities that allows the abrasive particles to remain more centrally along the centreline of the jet axis, and in turn affects the jet expansion. Nevertheless, the shape factors of the abrasive particles have been found to exert no discernible effect on the high speed air flow. It is also shown that the variation of nozzle sizes has a negligible effect on the maximum air velocities.

Keywords: Micro abrasive air jet, DEM-CFD, particle sphericity, particle velocity