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Wear Prediction in a Fluidized Bed

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ABSTRACT

A procedure to model the wear of surfaces exposed to a fluidized bed is formulated. A stochastic methodology adapting the kinetic theory of gases to granular flows is used to develop an impact wear model. This uses a single-particle wear model to account for impact wear from all possible particle collisions. An adaptation of a single-particle abrasion model to describe the effects of many abrading particles is used to account for abrasive wear. Parameters describing granular flow within the fluidized bed, necessary for evaluation of the wear expressions, are determined by numerical solution of the fluidized bed hydrodynamic equations. Additional parameters, describing the contact between fluidized particles and the wearing surface, are determined by optimization based on wear measurements. The modeling procedure was used to analyze several bubbling and turbulent fluidized bed experiments with single-tube and tube bundle configurations. Quantitative agreement between the measured and predicted wear rates was found, with some exceptions for local wear predictions. This work demonstrates a methodology for wear predictions in fluidized beds.

NOMENCLATURE

d_p	- Particle diameter; m
K	- Ratio of vertical to horizontal components of force during a single particle impact (Finnie 1958)
k_a	- Parameter in abrasion model of Equation (5)
m_p	- Mass of a single wearing particle; kg
rms	- Root-mean-square
u_{mf}	- Minimum fluidization velocity of granular phase; m/s
$\underline{u}_s, u_s$	- Slip velocity vector = $\underline{U} - \underline{u}_w$, magnitude of $\underline{u}_s$; m/s
$\underline{u}_0, u_0$	- Superficial velocity vector of fluidizing medium, magnitude of $\underline{u}_0$; m/s
VHN	- Vicker's hardness number; Pa
W''	- Total wear rate per unit area at a point; kg/s/m ²
W_a''	- Instantaneous mass rate of wear per unit area at a point (over a computational cell face in the WMP) caused by all abrading particles; kg/m ² /s
W_i''	- Instantaneous mass rate of wear per unit area at a point (over a computational cell face in the WMP) due to all possible particle impacts; kg/m ² /s
ρ_w	- Density of wearing wall material; kg/m ³
Ψ	- Ratio of length-to-depth of a single particle cut (Finnie 1958)
Θ	- Granular "temperature", measure of kinetic energy due to fluctuating component of particle velocity; m ² /s ²
$\underline{\tau}_s$	- Granular-phase stress tensor; Pa

INTRODUCTION

Fluidized-bed combustors (FBC's) possess many favorable attributes including flexibility in the choice of solid fuels, reduced thermal formation of nitrogen oxides, reduced ash agglomeration problems, SO₂ capture using limestone or dolomite sorbents in the bed material, high volumetric heat release rates, and high heat transfer rates.

However, the same mechanisms that lead to these advantages may cause the wear of surfaces submerged in the bed resulting from particle-surface collisions. Wear can result from impact or erosion produced by short duration collisions where a portion of the particle kinetic energy causes removal of material, and from abrasion or rubbing where the particle is forced against the surface with relative movement causing material removal (Zhu, 1988; Stringer and others, 1989; Witherell, 1989; Rathbone and others 1989; Zhu and others, 1989).

An expression that predicts wear in a fluidized bed is presented which accounts for both impact and abrasion contributions to wear. A single-particle model due to Finnie (1958) was used to derive the impact contribution using a stochastic methodology based on the kinetic theory of gases applied to granular flows. A simple empirical abrasion model due to Rabinowicz and others (1961) was adapted to model the abrasive contribution.

The resultant wear expression incorporates parameters which characterize the flow of the granular material within the

fluidized bed. A Wear Modeling Procedure (WMP) is used to predict local wear rates in a fluidized bed (Rogers and Boyle, 1991) in which this wear expression is evaluated using granular flow predictions made using the MFIX computer code (Syamlal, 1985; Gidaspow, 1986; Syamlal and O'Brien, 1989). MFIX solves the transport equations governing the flows of solid-fluid mixtures, which are described as interpenetrating continua. This work parallels the extensive fluidized-bed wear modeling work performed by researchers at Argonne National Laboratory who have coupled wear models with hydrodynamic code predictions (Bouillard and Lyczkowski, 1991).

The fluidized bed wear expression also contains three parameters, which are assumed independent of surface material properties, that describe the interaction of particles with the wearing surface in a fluidized bed environment. These parameters were estimated using an optimization procedure minimizing the difference between measured and predicted wear.

Using MFIX simulations in two dimensions and optimized wear parameters, fluidized bed wear was modeled for several non-reacting bed configurations and fluidization regimes. The WMP was able to provide accurate predictions of both local and circumferentially averaged wear rates for a square brass tube in a bubbling bed of sand particles (Zhu, 1988). The WMP yielded accurate predictions of both local and circumferentially averaged wear rates for several tubes within a 17-tube bundle (Wood and Woodford, 1979), except for the tube located highest in the bed.

FLUIDIZED BED WEAR EXPRESSION

Separate methodologies were used to account for impact and abrasive contributions since they result from different particle-surface contacts, i.e., impact wear occurs during short-duration single particle collisions and abrasion results from enduring, multiple particle contacts. A detailed derivation of the fluidized bed wear expression can be found in Rogers and Boyle (1991) and Rogers (1991).

Intuitively, it is expected that impact wear would contribute most during rapid flows of the granular phase; particles strike surfaces with sufficient kinetic energy to cause deformation and material removal. An adaptation of the kinetic theory of gases to granular flows was used to account for the contributions of all individual impacting particles. Use of kinetic theory involves convoluting the wear predicted by the single-particle impact wear model of Finnie (1958) with a distribution of particle velocities in the region of the wearing surface. The resulting expression describes the instantaneous local mass rate of impact wear per unit area of a wearing surface

$$W_i'' = \frac{n_w \Gamma}{(2 \pi \Theta_w)^{\frac{3}{2}}} \exp\left(\frac{-u_s^2}{2 \Theta_w}\right) \left(16 \pi \Theta_w^3 \frac{C_1}{\Psi} + 24 \pi u_s^2 \Theta_w^2 \frac{C_2}{\Psi}\right) , \quad (1)$$

where

$$\Gamma = \frac{\rho_w m_p}{VHN} , \quad (2)$$

with

$$C_1 = \frac{\pi - 2 \operatorname{arccot}\left(\frac{K}{6}\right)}{8 K}, \quad (3)$$

and

$$C_2 = \frac{1}{4 (K^2 + 36)} + \frac{C_1}{2}. \quad (4)$$

The parameter K represents the average ratio of vertical-to-horizontal forces on each particle during a collision, and Ψ is the average ratio of length-to-depth for the particle cuts. Ding and Lyczkowski (1992) have also used kinetic theory and Finnie's single particle wear model to develop an erosion model which they have used to predict wear using three-dimensional fluidized bed simulations.

The assumption of rapid granular flow inherent in kinetic theory disallows its use in the granular flow regime where abrasive wear is significant because of the enduring nature of the particle-surface contacts comprising the abrasion mechanism. Therefore a 'continuum averaging' procedure was used to derive the abrasion wear model where a single-particle abrasive wear model specifies an average abrasion rate per particle over a given region. This average single-particle abrasion rate, multiplied by a known concentration of particles at a point on

the surface, yields an instantaneous mass rate of abrasion wear per unit area

$$W_a'' = k_a \frac{\rho_w}{VHN} \tau_{sN} u_s \quad . \quad (5)$$

Impact wear dominates in beds subjected to high superficial velocity, in bed regions of high interstitial gas velocity such as inlet jets, in regions of dilute granular phase, and in regions where particles are accelerated by voidages traversing the bed. Abrasive wear dominates in ordered beds with low superficial gas velocity and in regions of a fluidized bed with dense particle concentrations. Since these conditions for impact and abrasion wear can exist simultaneously at different locations in a fluidized bed, a model must predict wear based on instantaneous, local bed conditions. Therefore the total instantaneous wear rate at a point, denoted by W'' , is assumed to be the sum of these two contributions,

$$W'' = W_i'' + W_a'' \quad . \quad (6)$$

THE WEAR MODELING PROCEDURE

The analytical wear expression in Equation (6) is numerically solved using a wear modeling procedure (WMP). The WMP assumes that the parameters ρ_w , m_p , and VHN represent easily measured surface and particle material properties. The terms n_w , θ_w , u_s , and τ_s representing instantaneous granular flow conditions

adjacent to the wearing surface are found using numerical predictions of fluidized bed hydrodynamics employing a multiphase fluid dynamics computer program called MFIX. The remaining parameters K , Ψ , and k_s describing particle-surface interactions in a fluidized-bed environment were chosen using an optimization procedure to minimize the difference between predicted and measured fluidized wear results.

The fluidized-bed wear rate measurements obtained by Zhu (1988) for horizontal tubes submerged in the rectangular, three-dimensional, room temperature, air-fluidized bed of sand particles sketched in Figure 1 were used for this optimization (Rogers and Boyle, 1991). Wear measurements for C36000 brass

Figure 1. Schematic of Zhu (1988) Fluidized Bed

(brass), C14500 copper (copper), aluminum alloy 2011-T3 (Al2011), stainless steel AISI type 304 (SS304), and carbon steel 1050 (CS1050) were subjected to 11 fluidized bed flow conditions to produce the 55 wear rate measurements used for the optimization. MFIX simulations were performed to provide granular flow predictions for the bed configuration of Figure 1 for each of the 11 bed operating conditions. The resulting optimized values of K , Ψ , and k_a are shown in Table I where they are compared to Table I. Optimized values and empirical estimates of Ψ , K , and k_a .

Ψ	K	k_a	Comments
3.033	8.305×10^{-9}	2.751×10^{-3}	Estimates from optimization 1) lower bound, $\Psi \geq 1.0$ 2) analyze 2-10 sec of each simulation 3) 11 simulations x 5 materials = 55 experimental points
2 - 10 (Note 1)	1.6 - 2.4; 6.0 maximum (Note 2) 2.0 (Note 3)	10^{-3} (Note 4)	Empirical estimates of parameter values.
Note 1: Finnie (1958), metal cutting experiments Note 2: Finnie (1958), single-particle scratch tests Note 3: Finnie (1958), many-particle grinding experiments Note 4: Rabinowicz and others (1961), 3-body, many-particle, abrasion testing apparatus using 40 micron Al_2O_3 particles			

empirical estimates from the literature. The optimized values of Ψ and k_a are very similar to the empirically-estimated values. However, a large difference between optimized and estimated K values exists. This large difference can be attributed to the

behavior of constants C_1 and C_2 in Equation (1) as a function of K . The behaviors of C_1 and C_2 at the physical limits of K yield the following values

$$\lim_{K \rightarrow \infty} C_1 = 0; \quad \lim_{K \rightarrow 0} C_1 = 1/24 \quad , \quad (7)$$

and

$$\lim_{K \rightarrow \infty} C_2 = 0; \quad \lim_{K \rightarrow 0} C_2 = 1/36 \quad . \quad (8)$$

The dependence of C_1 and C_2 on K , as defined by equation (3) and equation (4) respectively, is shown in Figure 2 indicating that C_1 and C_2 are weak functions of K . Consequently, the value of K does not greatly affect the rate of wear.

Figure 3 - Figure 7 present predicted wear as a function of the superficial gas velocity. As quantified in Table II, these figures show poor agreement between measured and predicted wear for brass and Al2011 samples with much better agreement

Figure 2. Variation in C_1 and C_2 with K

exhibited for copper, SS304, and CS1050. Predictions seem to be especially poor for the higher superficial gas velocities ($u_0 > 1.87$ m/s). The measured trend of wear rate increasing with u_0 was exhibited by the predictions for all materials except for u_0

Figure 3. Brass wear vs. u_0

Figure 4. Copper wear vs. u_0

Figure 5. Al2011 wear vs. u_0

Figure 6. SS304 wear vs. u_0

Figure 7. CS1050 wear vs. u_0

Figure 8. Brass wear vs. d_p

Figure 9. Copper wear vs. d_p

Figure 10. Al2011 wear vs. d_p

Figure 11. SS304 wear vs. d_p

Figure 12. CS1050 wear vs. d_p

= 2.03 m/s with a dip in the upward trend. This suggests that differences exist between the MFIX predictions and the actual granular flow field which cannot be compensated for by the parameter optimization. For example, the wear experiments were performed in 3 dimensional space and used tubes that were circular in cross section, whereas limitations of MFIX required that the simulations be performed in 2 dimensional space and use tubes that were square in cross section. These limitations are not insignificant because the rate of wear of a surface depends on the flow immediately adjacent to the surface.

Figure 8 - Figure 12 compare predicted and measured wear variation with mean particle diameter. Again, agreement between measured wear rate and wear rates was poor for brass and Al2011, with rms relative errors of 86% and 84% respectively. This was especially true for the larger particle diameters ($d_p > 0.67\text{mm}$) where wear rate was under predicted. Improved agreement is exhibited for copper, SS304, and CS1050 with rms relative errors of 60%, 66%, and 52%, respectively. In contrast to brass and Al2011, the figures show that the wear rates for copper, SS304, and CS1050 are under predicted for the smaller particle diameters ($d_p < 1.0\text{mm}$). The measured trend of wear rate increasing with mean d_p was exhibited by the predictions, except for $d_p = 1.10\text{mm}$.

RESULTS AND DISCUSSION: COMPARISON OF MODEL PREDICTIONS WITH MEASURED WEAR

The values of K , Ψ , and k_a in Table I were used to predict wear in fluidized beds that vary tube geometry, tube shape, tube

material, particle size, and fluidization regime from those values used for their determination.

Table II. RMS relative errors between measurement and prediction for Figure 3 - Figure 12

RMS Relative Error		
Tube Material	From Figure 3 - Figure 7	From Figure 8 - Figure 12
Brass	0.79	0.86
Copper	0.45	0.60
Al2011	0.76	0.84
SS304	0.56	0.66
CS1050	0.32	0.52

Square Tube Wear, Zhu (1988)

Zhu (1988) placed a tube with a square cross section in the experimental fluidized bed of Figure 1 and measured the wear of brass pieces mounted in the center of each tube face. Figure 13 shows accurate predictions were obtained for both local and circumferentially averaged square tube wear rates, with a 15% relative error between prediction and measurement at the bottom face, 32% relative error at the tube sides, 130% relative error on the top face, and 15% relative error in the tube average wear rate. Zhu attributes wear on the bottom tube face to particle impacts and wear on the tube sides to abrasion. Figure 14 shows that the WMP predicts only an 11% impact contribution to total

Figure 13. Comparison of predicted and measured square brass tube wear rates, measurements from Zhu (1988).

Figure 14. Abrasion and impact contributions to predicted square brass tube wear rates

wear rate on the bottom tube face with virtually 100% of predicted wear on the tube sides attributed to abrasion.

Turbulent Bed, Wood and Woodford (1979)

Wear prediction in the experimental fluidized bed of Wood and Woodford (1979) was studied using the configuration illustrated in Figure 15. In the experiment, 5 mm diameter wear specimens made of ferric alloy SA213-T11 were mounted on 2.5 cm diameter tubes at the positions shown in Figure 15.

Figure 15. Experimental fluidized bed of Wood and Woodford (1979) schematic showing tube bank geometry and locations of specimen disks

A ring-shaped wear specimen was mounted on Tubes 1 and 2 to determine a circumferential-average wear. Accumulated mass loss by each specimen was monitored periodically during a 150 hour period while they were immersed in a bed of 1mm sand particles, air-fluidized at $u_0 = 3$ m/s.

Measured wear rates were essentially constant over the 150 hour measuring period, so the measured time-average wear rates reported below were determined by dividing the total measured wear for each specimen by the 150 hour duration of the experiment. The time-average wear rate predictions are compared to measurements in Table III.

Table III. Comparison of measured and predicted wear rates for Wood and Woodford (1979) experiment

Measured and Predicted Wear Rates (kg/m ² /s)				
Tube Number	Local Wear Rate		Average Wear Rate	
	Measured	Predicted	Measured	Predicted
Tube 2	4.2x10 ⁻⁸ (1)	2.0x10 ⁻⁷ (1)	1.4x10 ⁻⁸ (5)	6.8x10 ⁻⁸ (4)
	7.8x10 ⁻⁸ (2)			
Tube 1	6.0x10 ⁻⁸ (2)	1.2x10 ⁻⁸ (1)	1.0x10 ⁻⁸ (5)	2.0x10 ⁻⁸ (4)
Tube 3	2.2x10 ⁻⁸ (2)	2.5x10 ⁻⁸ (1)	(3)	1.9x10 ⁻⁸ (4)
Tube 4	1.0x10 ⁻⁸ (1)	7.9x10 ⁻⁹ (1)	(3)	6.2x10 ⁻⁷ (4)
Notes: (1) - Measurement and Prediction made at tube bottom, 0° (2) - Measurement made at 25° from tube bottom (3) - Measurements not performed in experiment (4) - Time Averaged and averaged over square tube faces (5) - Time Averaged and Circumferentially averaged				

Overall, agreement between measurement and prediction ranged from a 486% relative error between measured and predicted circumferential average wear rates for Tube 2, to a 21% relative error for local wear rate for Tube 4. Predicted local wear rate at the square tube bottom face is only 14% higher than measured wear rate for Tube 3 at the 25 degree location, although these values are not from directly comparable tube positions.

CONCLUSIONS AND RECOMMENDATIONS

An expression has been derived that predicts the rate of wear at a point on a surface submerged in a fluidized bed which includes impact wear and abrasion wear contributions. The fluidized bed wear expression incorporates the following approximations and assumptions: impact wear results from cutting impacts only, deformation impact wear has been neglected; the granular velocity distribution is assumed to be Maxwellian about the local mean velocity; and the abrasion model assumes that the particles are angular.

A wear modeling procedure (WMP) has been described to evaluate this fluidized bed wear expression that makes use of measurable particle and wearing surface properties along with granular flow predictions made by the multiphase fluid dynamics program called MFIX. Parameters in the wear expression, denoted by Ψ , K , and k_a , which determine the relative contributions of the impact and abrasion modes of wear were determined using an optimization procedure to minimize the difference between 55 measured and predicted wear rates.

The WMP was used to predict wear in fluidized bed configurations different from those used to determine Ψ , K , and k_a : The predicted rate of wear agreed within an order of magnitude with measured local and circumferentially averaged wear in the turbulent fluidized bed of Wood and Woodford (1979). The average relative error in the predicted wear rate for a square brass tube (Zhu, 1988) was less than 20%. Significantly, the

square cross section of the brass tube was perfectly modeled by MFIIX, which then afforded an accurate description of the flow field near the tube. Thus, accurate inputs into the wear model were available and an accurate prediction for the rate of wear resulted.

Future work should improve both the fluidized bed wear expression and the WMP. The wear expression should include a deformation impact wear contribution, where a more sophisticated single-particle model such as the Bitter (1963a, 1963b) or Neilson-Gilchrist (1968) models, would be required. To improve the WMP, three-dimensional effects on granular flow and resultant wear must be modeled by the MFIIX simulations.

Despite the confounding effects caused by wear expression and WMP approximations, a viable method to predict wear in a fluidized bed has been developed.

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