

Particle-Surface Interactions in Heat Exchanger Fouling

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The problem of fouling is of vital importance to heat exchanger efficiency and should be considered during the design phase of the heat exchanger. The purpose of the present paper is to introduce a novel method to aid in the evaluation of the various parameters that affect the fouling phenomenon. The method considers the particle-surface interaction from the energy balance at the point of impaction and takes into consideration the material properties of the particle and surface as well as the effect of the surrounding flow field on the particle movement and impaction. The calculated deposition flux is used to form the deposit evolution in time considering the removal mechanisms resulting from fluid shear stress and the eroding impacts of the particles. The model is validated against experimental measurements of particle deposition from a two phase flow of hot gases around a circular cylinder. The experimentally measured flow field around a staggered tube bundle is also predicted and a fouling analysis is performed regarding the validity of fouling studies in scaled down model configurations. It is concluded that the actual dimensions of the heat exchanger configuration are of vital importance to its fouling behavior and scaling laws are difficult to apply.

I Introduction

Fouling is a major problem in many industrial applications such as turbomachinery (Tabakoff, 1984) and heat transfer (Taborek, 1972). The purpose of the present paper is to introduce a numerical method that will serve as an engineering tool to study the fouling phenomenon. More specifically, the method will be applied to heat exchanger fouling in lignite utility boilers; here the flue gases carry ash particles up to the superheater area and deposit them onto the tube surfaces thereby reducing heat transfer efficiency.

There have been many numerical models developed to predict the fouling phenomenon. The basic concept used to describe fouling is due to Kern and Seaton (1959) who define the growth of the deposit as the result of a deposition flux and a removal flux which will eventually reach equilibrium. They expressed this through an exponential equation. Through a series of papers, Cleaver and Yates (1973, 1976) formed a deposition model where deposition is expressed using the friction velocity and the particle concentration and the expression for the removal rate is from their turbulent burst theory using a critical wall shear stress. Though the approach is quite different, they develop an exponential equation similar in form to the one by Kern and Seaton. Papavergos and Hedley (1984) examined a number of deposition models but limited their comparisons to simple flow fields where the models took the flow into account through the particle relaxation time or the flow Reynolds number. Local surface conditions were most often ignored.

"Global" models, such as those previously mentioned, have shown acceptable agreement with experimental measurements as far as deposit build up in time and overall exchanger efficiency is concerned. However, they don't provide information regarding the deposit distribution and the effect of flow field parameters, particle material, and heat exchanger configuration on the overall phenomenon. This would significantly help the design stage due to the importance of these parameters (Hein, 1977).

One of the most important mechanisms in particle deposition is the impaction mechanism. Rogers and Reed (1984) and Fich-

man and Pnueli (1985) both introduced an elastic-plastic impact model that provides a criterion for the adhesion or rebound of a particle impacting a solid surface based on the particle impact velocity and the material properties of both the particle and the surface. Although the models were much alike, the papers were directed toward different applications (surface coating and filtration, respectively). Wall et al. (1988) improved the original model by taking into account a dynamic yield limit to distinguish elastic from elastic-plastic impactions.

In heat exchanger fouling, the flow field is highly complex (Dowlati et al., 1992; Braun and Kudriavtsev, 1995) and should not be neglected. In the methodology to be presented here, the flow field (velocities and temperature) will be numerically predicted through a solution of the Navier Stokes and energy equations using a collocated control volume method based on the SIMPLE pressure correction algorithm, while the $k-\epsilon$ turbulence model will account for the effects of turbulence. A Lagrangian particle tracking method will be used (Burry and Bergeles, 1993) that takes into account the anisotropy of the turbulence. Knowledge of the flow field characteristics allows the direct evaluation of the effect of lift forces, thermophoretic forces, and turbulent fluctuations on the particle behavior. The impact-adhesion theory will be used in combination with the particle tracking method to form a novel deposition model, which will determine particle sticking or rebound after impaction. Material properties of the particles and tube surfaces are influenced by the local temperatures and will be considered due to the nature of the impact-adhesion model. After the deposition rate has been locally calculated, an exponential equation is used to calculate the evolution of the deposit in time, while the removal rate is calculated using the local fluid shear stress as well as an eroding shear stress which is calculated from the particle impactions. This leads to the calculation of the deposit distribution on the heat exchanger surfaces, making the method particularly well suited for heat exchanger design methodologies.

The full methodology will be used to predict the two-phase flow of combustion gases and particles around a circular cylinder in crossflow. The particle deposition rates to the surface of the cylinder will be compared with experimental measurements for various particle diameters. Finally, the fouling phenomenon on a staggered tube bundle arrangement will be studied. As will be shown, conflicting factors between the flow field and parti-

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cles make it difficult to study the fouling phenomenon in scaled-down geometries since known similarity laws do not apply. This makes numerical studies essential for this type of engineering problem.

II The Impact-Adhesion Model

Theoretical Analysis. The novelty of the method presented here to calculate the deposition rate of particles impacting a surface rests in the fact that the calculation takes into account the local conditions before and during impact. These conditions are determined by the initial impact velocity, the material properties of the particle, and the surface and the influence of the flow field through the lift force and the thermophoretic force acting on the particle. The local temperature also affects the material properties of the particle and the surface.

The first part of the model is based on the original concept of Bitter (1963a) that the energy available to a particle just before it impacts onto a surface is the kinetic energy due to its velocity, while the most important energy loss mechanisms are plastic deformation (provided the impact velocity exceeds a certain limit defined by the material properties of the particle and surface) and elastic wave propagation during the first phase of the impact. The energy stored as elastic energy will be returned to the particle during the second phase which begins after the particle reaches the point where its velocity is zero. For a spherical particle with mass (m_p), radius ($R = d/2$), moving with a velocity (V) normal to a plane surface and impacting on to the surface, the following energy balance equation can be written (Wall et al., 1988):

$$Q_i - Q_L - Q_P = Q_E \quad (1)$$

where the initial kinetic energy of the particle $Q_i = \frac{1}{2}m_p V^2$ is lost to plastic deformation and dissipated due to elastic wave propagation (a modification to Wall et al., 1988, based on Reed, 1985). The remaining stored elastic energy is then returned to the particle as kinetic energy. If this is larger than the energy due to attractive forces between particle and surface Q_A , then the particle will rebound, otherwise it will stick to the surface. The attractive forces are mostly due to dispersion forces that are dependant on the electrical properties of the volume elements of the materials and the distance between them. They

are present in all types of matter and are sometimes cited as the London dispersion forces (Fowkes, 1964).

For a particle of known size and velocity the full expressions for the terms in (1) are functions of material properties of the particle and the surface to which it impacts: E (Young's modulus), ν^p (Poisson's ratio), Y (Elastic yield limit of the softer of the two materials), ρ (particle density), and $\Delta\gamma = 2\sqrt{\gamma_p^d \gamma_s^d}$ (interfacial surface energy derived from the dispersive surface energies of the two materials, Fowkes, 1964). These expressions used by Rogers and Reed (1984) and then by Wall et al. (1988) were originally introduced by Bitter (1963a) based on the Hertz theory. However, they are rather complex and therefore only our modifications to the basic theory will be presented here. The impact-adhesion model without flow field effects has already been validated in Wall et al. (1988) for particles impacting surfaces of various materials. The only other addition presented to this part of the model is the energy loss due to elastic waves propagating away from the point of impact that is important only in purely elastic impacts since it is the only energy loss mechanism present. Validation of this form of the model has been presented in Bouris-Buray and Bergeles (1994).

The novelty presented in this paper is the extension of the model to situations with a surrounding flow field. In this case, the shear stresses of the surrounding flow result in a force that acts on the particle in the direction of the velocity gradient and therefore becomes a lift force in the area near a wall. This lift force (Saffman, 1965) can be expected to play an important role in particle resuspension from the wall since shear stress is significant here. As stated by Cleaver and Yates (1973) the following expression can be used:

$$F_L = 0.83 \frac{\rho}{\nu} d^3 \left(\frac{\tau_w}{\rho} \right)^{(3/2)} \quad (2)$$

If the lift force is assumed to be constant over the second phase of the impact when the particle begins to move away from the wall then the energy transferred to the particle will be:

$$Q_{\text{lift}} = F_L \delta, \quad \delta = \frac{r_i^2}{2R} \quad (3)$$

where δ is defined by Bitter (1963a) and r_i is calculated from (Wall et al., 1988).

Nomenclature

A = area (m^2)	R = universal gas constant (8.315 kJ/kmol/K)	$\Delta\gamma, \gamma^d$ = interfacial surface energy and dispersive surface energy (J/ m^2)
C_s, C_m, C_i, K_2 = constants	$R = d/2$ = particle radius and diameter (m)	λ = molecular mean free path (m)
D_{cyl} = heat exchanger cylinder diameter (m)	r_i = radius of total contact area between particle and surface (m)	ν^p = Poisson's ratio
E = Young's modulus (Pa)	Stk = particle Stokes number	μ, ν = dynamic ($\text{kg/m}\cdot\text{s}$) and kinematic (m^2/s) viscosity of fluid
F_L, Q_{lift} = lift force (N) and lift energy (J)	Stm, j_p'' = nondimensional deposition rate and particle deposition flux ($\text{kg}/\text{m}^2/\text{s}$)	ρ = density (kg/m^3)
k = thermal conductivity ($\text{W}/\text{m}\cdot\text{K}$)	t = time (s)	$\rho_{p,\infty}$ = particle mass concentration at free stream (kg/m^3)
$m, \dot{m}$ = mass (kg) and mass flux (kg/s)	T_0 = local temperature (K)	τ_{er}, τ_w = eroding particle shear stress and wall shear stress ($\text{kg}/\text{m}\cdot\text{s}^2$)
Q_A = attractive energy between particle and surface (J)	U_∞ = freestream velocity (m/s)	
Q_E = stored elastic energy (J)	$u_r = (\tau_w/\rho)^{1/2}$ = friction velocity (m/s)	Subscripts
Q_i = initial kinetic energy of particle (J)	V = particle velocity (m/s)	p = particle
Q_L = energy dissipated due to elastic wave propagation (J)	Y = elastic yield limit (Pa)	∞ = freestream
Q_P = plastic deformation energy (J)	δ = total approach between particle and surface (m)	g = gas, fluid
		d = deposit
		r = rebound

It should be stated here that the range of (2) has been a matter of research lately. Hall (1988) measured the lift force acting on a particle near a wall and concluded that the Saffman expression is reasonably accurate only for particles with $\alpha^+ = Ru_p/\nu < 20$. In the present work, the lift force is considered negligible during the first phase of the impaction because the plastic deformation is almost always several orders of magnitude larger than the energy due to the lift force. On the other hand, during the second phase, the elastic energy remaining, the surface energy of adhesion and the energy due to the aerodynamic lift force are all influential to the rebound or adhesion of the particle. Furthermore, in the cases to be studied $\alpha^+ < 14$ which warrants the use of (2). Finally, the unsteadiness of the lift force has not been considered since its complexity is not in the scope of the model but it would provide an interesting topic for investigation.

After the energy balance, the normal rebound velocity can be calculated:

$$V_r = \left[\frac{2}{m_p} (Q_i - Q_p - Q'_A - Q_L + Q_{\text{lift}}) \right]^{(1/2)} \quad (4)$$

where Q_L is significant only for purely elastic deformations.

The impact adhesion model as presented above determines whether a particle of given impact velocity and material properties will stick or rebound from a surface whose material properties are also known. If rebound occurs then the normal rebound velocity is also calculated. At this stage it is assumed that the tangential velocity does not significantly influence rebound or adhesion of the particle but its influence will be considered as an eroding shear stress on the deposit in the next part of the model.

Prediction of Particle Deposition Onto a Cylinder in Crossflow. The impact adhesion model is now incorporated into the two-phase flow numerical procedure that has already been extensively tested (Burry and Bergeles, 1993; Balabani et al., 1994). The calculation of the flow field is accomplished through a collocated control volume procedure based on the SIMPLE pressure correction algorithm of Patankar and Spalding (1972) with the $k - \epsilon$ turbulence model and a stochastic Lagrangian particle tracking procedure (Burry and Bergeles, 1993) that takes the anisotropy of the turbulence into account through an algebraic Reynolds stress model. All of the equations are solved in orthogonal curvilinear coordinates.

The particle deposition model developed so far was validated against experimental measurements of particle deposition onto a circular cylinder in high temperature particle laden cross flow (Kim and Kim, 1992). In the experimental configuration the combustion gases flowing past the cylinder were laden with TiO_2 particles and the cylinder was covered with a platinum ribbon, which complies with the rigid body assumption since it was six times thicker than the maximum particle diameter. The Reynolds number of the flow past the cylinder was $Re = 54$ based on freestream velocity and cylinder diameter. The temperature of the flow and cylinder surface was 1603 K and 1171 K, respectively ($T_w/T_\infty = 0.731$) while the freestream turbulence before the cylinder was measured at 2.5 percent (r.m.s. velocity over mean flow velocity). Particle concentration at freestream was monitored not to overcome 10^{10} particles/ m^3 to avoid effects of turbulence or Brownian coagulation. For the numerical predictions the concentration was kept at this limit for all particle sizes. The particles that had deposited onto the cylinder after a given time were counted and a particle deposition rate was calculated and nondimensionalized:

$$St_m = \frac{j_p''}{\rho_p \alpha U_\infty} \quad (5)$$

For large particles the most important deposition mechanism

is inertial impaction but small particle diameters are strongly influenced by large temperature gradients in the flow field. Molecular impactions on the hotter side of the particle deliver more momentum and result in a force acting in the opposite direction of the temperature gradient. This is significant only if the mean free path of the molecules is not negligible in relation to the dimension of the particle. The phenomenon is called thermophoresis and to account for this the thermophoretic force was added to the existing particle tracking method by introducing the new term into the equation used for particle tracking. The force due to thermophoresis is (Talbot et al., 1980):

$$F_T = - \frac{12\pi\mu RC_s(k_g/k_p + C_t\lambda/R)}{(1 + 3C_m\lambda/R)(1 + 2k_g/k_p + 2C_t\lambda/R)} \frac{\nabla T}{T_o} \quad (6)$$

where the mean free path is $\lambda = 0.43\nu/\sqrt{T}$ (λ/R is the Knudsen number), $C_s = 1.17$, $C_m = 1.14$ and $C_t = 2.18$ are empirically determined constants.

The flow field around the cylinder and the deposition onto its surface was numerically calculated with the impact adhesion model giving the distribution of particle mass flux depositing onto the cylinder. The fact that the Reynolds number of 54 is laminar raises the problem of stochastically modeling the particle trajectories since the turbulent fluctuations are needed for their statistical representation. Instead of arbitrarily imposing a turbulent fluctuation at each point based on the inlet turbulence intensity of 2.5 percent, it was considered preferable to model the transport of the inlet turbulence with the $k - \epsilon$ turbulence model. Parametric computations showed that the effect of the turbulence model on the mean velocities in the front of the cylinder (stagnation point to 90 deg) is negligible and will therefore not introduce significant deviations in the calculations since it is this region that mostly influences particle movement near the cylinder.

The material properties of the particle and surface are essential to the correct performance of the impact-adhesion model and are also influenced by the local temperature. Unfortunately, precise values are not always available. For the case presented here the values for the density, Young's modulus, and Poisson ratio were taken to be $\rho_{\text{TiO}_2} = 4170 \text{ kg/m}^3$, $\rho_{\text{Pt}} = 21450 \text{ kg/m}^3$, $E_{\text{TiO}_2} = 17 \text{ GPa}$, $E_{\text{Pt}} = 67 \text{ GPa}$, $\nu_{\text{TiO}_2}^p = 0.47$, and $\nu_{\text{Pt}}^p = 0.45$, which were obtained from linear interpolation to their respective temperatures from the values at 20°C (Mark's Handbook, 1978) and the respective melting point temperatures. $\Delta\gamma = 0.166 \text{ J/m}^2$ from Fowkes (1964), the elastic yield limit of Pt was also interpolated to the required temperature $Y = 38.7 \text{ MPa}$ and the thermal conductivities needed for the thermophoretic force were $k_{\text{TiO}_2} = 3.78 \text{ W/m/K}$ and $k_{\text{gas}} = 0.091 \text{ W/m/K}$ at their respective temperatures.

The nondimensional deposition rates are shown in Fig. 1 and it is interesting to note that the values of the deposition rate are an order of magnitude larger for the 20 μm particles. This is mostly due to the fact that the smaller particles tend to follow the streamlines of the flow and do not impact onto the cylinder as easily as the larger ones. The thermophoretic force is more prominent in the smaller particle sizes and is responsible for their impaction onto the cylinder surface. This is evident from Fig. 2(a) where the mean impact angle is shown for the three particle sizes. The two smaller sizes impact at about 60 deg independent of the position on the cylinder whereas the 20 μm particles tend to reach a tangential impaction near the 90 deg point from stagnation. This proves that the impact angle for the 20 μm particles is determined by the angle of their approach to the cylinder while the smaller particles which follow the streamlines are pulled toward the cylinder surface by thermophoresis. It should also be noted that in the smaller particle sizes deposition is also found on the back of the cylinder which is due to particles entering the recirculation zone behind the cylinder as opposed to the larger particles, which simply fly out of the recirculation area due to their momentum.

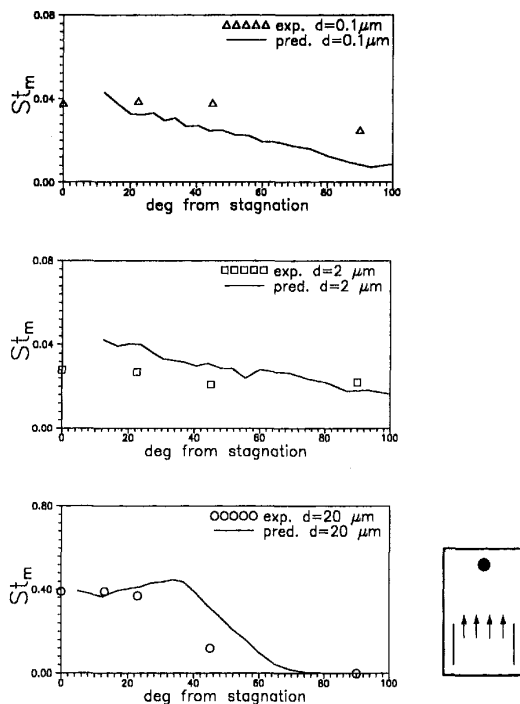


Fig. 1 Numerical prediction of the nondimensional deposition rate of various sizes of TiO_2 particles onto a Pt cylinder in crossflow ($\text{Re} = 54$) with a temperature difference between surface and fluid $T_w/T_\infty = 0.731$ (Kim and Kim, 1992)

In Fig. 2(b) another interesting fact can be seen, the smaller particles stick almost immediately upon impacting the surface while the larger particles have a tendency to bounce after their first contact with the cylinder surface. This can be explained by considering that the smaller particles are actually pulled toward the surface by thermophoresis while the larger particles impact onto the cylinder mostly due to their inertia and will therefore have a normal velocity large enough to result in one or two bounces before it is lost and can be overcome by the surface adhesion forces.

The discrepancies in Fig. 1 can be attributed to the uncertainties mentioned before regarding the flow field and the turbulence fluctuations that influence the particle trajectories. There is also a degree of uncertainty regarding the assumed linear variation of the material properties as a function of temperature. The variation of these properties is actually rather complex but it would be interesting to see the effect of the precise values with respect to temperature on the performance of the model. Finally, it should be noted that the reported experimental uncertainty was 5.3 percent for the measurement of the deposited particle size and number distribution and 4.5 percent for the same quantities at the inlet (Kim and Kim, 1992).

III Numerical Study of Heat Exchanger Fouling

In this section, the numerical model will be extended to account for the evolution of the deposit in time by using an exponential equation to relate the deposition rate that has been calculated to the parameters that will influence deposit removal (resuspension).

Evolution of the Deposit in Time. The expression that will be used to describe the evolution of the deposit in time is based on the Kern and Seaton (1959) concept of a deposit and a removal flux. If the removal flux is considered dependant on

the local fluid shear stress and on the amount of mass already deposited, then integration will give the deposit mass as an exponential function of time:

$$m = \frac{m_d^*}{K_2 \tau_w} [1 - \exp(-K_2 \tau_w t)] \quad (7)$$

The above equation shows that for a given deposition rate and flow field conditions a maximum deposit (asymptotic deposit) is reached after a certain time and its mean value remains constant from then on. This is in accord with experimental and operational observations that confirm the existence of an asymptotic deposit. The term outside the brackets in (7) is the asymptotic deposit mass at which the mechanisms of deposition and removal will be in equilibrium.

The method used here is based on (7), where the deposition rate is calculated from the impact-adhesion model presented above, and the shear stress is calculated from the solution of the Navier-Stokes equations around the tubes. Experimental and industrial observations can help to determine the value of K_2 , which is a parameter of the strength of the deposit. If the asymptotic deposit volume (and therefore mass) is known at a given position of the tube then the value of the constant can be determined and the deposit build up on the rest of the tube or on the other tubes of the bank can be predicted.

Due to the tangential velocity component of particles impacting a surface there will be a certain amount of "erosion" of the deposit and reentrainment back into the flow. This can be expressed as an eroding shear stress that acts along with the fluid shear stress, although it will have a different distribution on the surface. This shearing action can be expressed as:

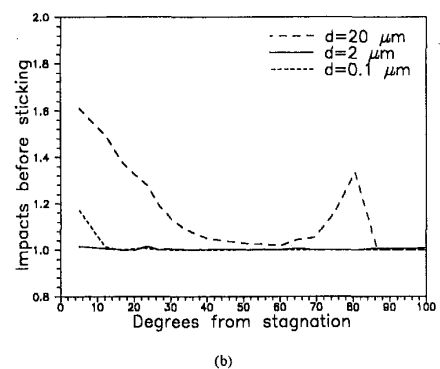
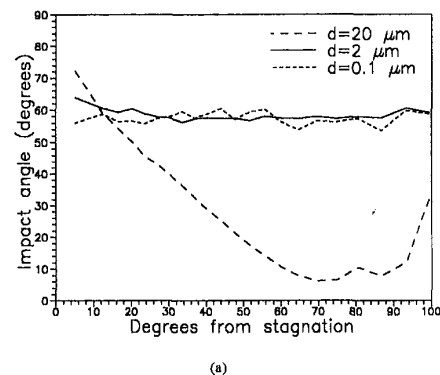


Fig. 2 (a) Mean impact angle and (b) Mean number of impacts before adhesion of various sizes of TiO_2 particles onto a Pt cylinder in crossflow ($\text{Re} = 54$) with a temperature difference between surface and fluid $T_w/T_\infty = 0.731$ (Kim and Kim, 1992)

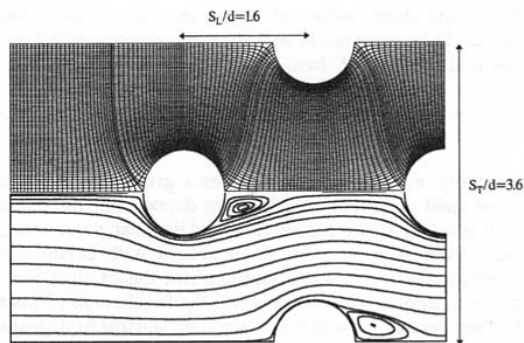


Fig. 3 Orthogonal curvilinear computational grid (129 × 71) and streamlines of the flow for $Re = 13000$ for a staggered tube bundle

$$\tau_{er} = \frac{\sum (m_{imp} U_{tang})}{A} \quad (8)$$

where A is the area of the surface where the impacting mass flux is considered and the summation refers to all particles that impact onto that area. The above equation is of course an over simplification of the erosion process, however, it is not intended to play the part of an erosion model, only to represent the shearing action that the tangentially impacting particle mass flux will add to the fluid shear stress. The variation of the calculated erosion with impact angle is a rough approximation of the erosion of a soft and ductile material (Bitter, 1963b) (the high temperatures observed in utility boiler heat exchangers allow the deposit to be treated as such) which would normally have a maximum at about 10 deg impact angle instead of a perfectly tangential impact that is implied by (8). Considering the nature of the constant K_2 it can be taken to be the same for the shear stress due to the fluid and the eroding shear stress so (7) becomes:

$$m = \frac{m_d}{K_2(\tau_w + \tau_{er})} \{1 - \exp[-K_2(\tau_w + \tau_{er})t]\} \quad (9)$$

At a given location of the surface and for a given deposition flux, the evolution of the deposit in time is dependent on the mass flux impacting at that location, the velocity of the impacting particles, and the shear stress of the fluid. The distribution of each of these properties on the surface is different. For the test cases that will be presented in the next section, the shear stress due to erosion and the fluid shear stress were of the same order of magnitude for a mass loading of 1 percent but for a mass loading of about 50 percent the shear stress due to erosion would be 1–2 orders of magnitude larger than the fluid shear stress (depending on the location on the surface of the tube where they are being compared).

Fouling in a Staggered Arrangement Heat Exchanger. The flow field in the staggered tube bundle arrangement of Fig. 3 was experimentally and numerically studied in Balabani et

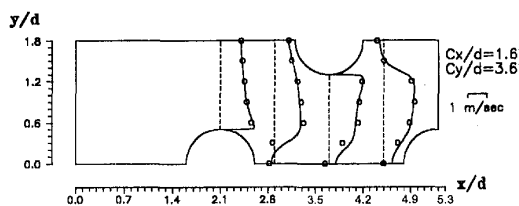


Fig. 4 Numerical predictions of the experimental measurements of the mean axial velocities (Balabani et al., 1994) in the staggered tube bundle of Fig. 3

Table 1 Particle size distribution for a typical lignite utility boiler

Particle diameter d_p (μm)	700	300	145	45
Percentage of total mass (%)	12.8	23.3	27.2	36.7

al. (1994). The numerical procedure is the same as the one used in Section 2.2 and the orthogonal curvilinear computational grid and the streamlines of the flow for a Reynolds number of 12858 (based on the tube diameter and the maximum velocity in the tube bank) are shown in Fig. 3. Comparison of the experimental measurements and the numerical calculations are presented in Fig. 4 for the mean axial velocities of the flow.

For the two-phase flow calculation the particles were taken to have the material properties of Al_2O_3 and a density of 2300 kg/m^3 , which is closer to the actual density of the depositing ash particles in a lignite utility boiler superheater. The fluid and particle properties were taken at a temperature of $900^\circ C$ and the material properties of the surface were those of steel at a temperature of $600^\circ C$.

The particle size distribution was taken from a typical lignite utility boiler size distribution and is presented in Table 1, while their inflow distribution was considered uniform. The total mass loading was 1 percent while the fluid velocity was taken to be 12 m/s, a typical value for utility boilers. The numerical procedure gave the distribution of the particle mass deposition flux on the tubes, the fluid shear stress on the tubes, and the eroding shear stress from the particle impactations. The K_2 constant was evaluated based on the industrial observation of the Public Power Corporation (PPC) of Greece that the maximum deposit height in a lignite utility boiler heat exchanger is about 1–2 tube diameters (Bouris-Burry and Bergeles, 1994). This was considered to be the maximum height for the stagnation point of the first tube and from this value the K_2 constant was calculated allowing the use of (9) for the calculation of the evolution of the deposit at all the other positions in the tube bundle.

Ideally, the deposition should be calculated for a short time period i.e., until the cylinder's shape begins to change, then the flow field should be solved again using the new tube shape and so on. This was neglected in the present calculation. Time dependence of the deposit is expected to lead to slower fouling rates than those predicted here due to the streamlining of the tube shape by the deposit and easier particle bypass of the tubes. With this in mind, the results indicate that the maximum (asymptotic) deposit is reached in about two weeks and the deposit formation can be seen in Fig. 5. It should be mentioned that at this stage of the computations the temperature variation (and therefore fluid density variation) of the fluid was neglected in order to limit the influential parameters to the particle Stokes numbers and the tube bundle arrangement for the analysis that will follow.

Table 2 allows a better understanding of the mechanisms involved in the adhesion process by presenting the values of the various energies involved in an impact. The values refer to $90 \mu m$ Al_2O_3 particles impacting a steel surface and the interfacial surface adhesion energy was taken to be 0.15 J/m^2 , which is the value for the two substances slightly increased to account

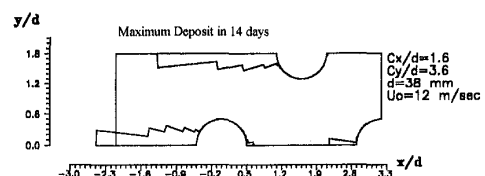


Fig. 5 Fouling in a full scale staggered tube bundle without temperature variation

Table 2 Characteristic values of energy time during particle impact to a surface

Impact velocity (m/s)	Initial kinetic energy (J)	Plastic deformation energy (J)	Surface adhesion energy (J)	Aerodynamic lift energy (J)	Rebound velocity (m/s)
15	.987658 10^{-7}	.987182 10^{-7}	.256579 10^{-11}	.146483 10^{-11}	0.32544
9.9	.430224 10^{-7}	.429909 10^{-7}	.194538 10^{-11}	.96683 10^{-12}	0.26337
5.1	.114173 10^{-7}	.114012 10^{-7}	.125083 10^{-11}	.498125 10^{-12}	0.18736
0.9	.355557 10^{-9}	.352724 10^{-9}	.395626 10^{-12}	.880052 10^{-13}	0.0758
0.015	.987657 10^{-13}	.685559 10^{-13}	.354803 10^{-13}	.157696 10^{-14}	0.0

for the sticky layer of condensed vapours that are always present in a utility boiler heat exchanger. For the aerodynamic lift force energy the value of the fluid shear stress was taken to be 2 kg/m s^2 while for the flow field that was studied typical values ranged from 0.3 at the front of the first cylinder to 12. at the 90 deg position and then to 0.1 at the 175–180 deg position. It is obvious that the aerodynamic lift force and the aerodynamic lift energy will follow the trend of the shear stress along the surface of the cylinders according to (2).

It is interesting to note that the plastic deformation energy is the main energy loss mechanism while the adhesion and aerodynamic lift energy are of the same order of magnitude. Allowing for the variation of the value of the fluid shear stress (which affects the aerodynamic lift force) along the tube surface it is obvious that the lift force is important to the adhesion process. The dependence of the aerodynamic lift energy on the impact velocity is due to δ in (3), which is the total approach between the particle and surface during impact. Another interesting observation is that the adhesion and aerodynamic lift energies become even more important at small impact velocities where plastic deformation is not as pronounced. The small velocities, at which the particles are shown to adhere to the surface, indicate that it is very difficult for a particle to adhere after a single impact. However, one or two bounces seem to be enough for the particle to lose a large amount of its initial energy and impact the surface with smaller velocities until it finally sticks. This was also observed for the TiO_2 particles impacting the Pt cylinder. Of course the mechanism is rather more complicated since the particle regains some of its energy from the surrounding flow field.

At this point, an attempt will be made to study the phenomenon in the same tube bundle arrangement but at a different scale. The utility boiler arrangement will be scaled down 3.8 times to give a tube diameter of 10 mm instead of the actual 38 mm and the Reynolds number will be kept constant by changing the inlet fluid velocity and keeping the fluid, particle, and surface properties the same. At first, the Stokes number of the particles will be kept constant, this being the parameter that actually defines the particle behavior with respect to the fluid flow. The Stokes number is defined:

$$\text{Stk} = \frac{\rho_p d_p^2 U_\infty}{D_{\text{cyl}} 18 \mu_g} \quad (10)$$

and for the four original particle sizes of Table 1 the Stokes numbers corresponding to the case being examined were $\text{Stk}(45$

$\mu\text{m}) = 1.73$, $\text{Stk}(145 \mu\text{m}) = 17.93$, $\text{Stk}(300 \mu\text{m}) = 76.77$, and $\text{Stk}(700 \mu\text{m}) = 418.01$.

In order to keep the Stokes number constant, the particles are reduced in size to compensate for the reduction in size of the tube diameter and to keep the fluid properties the same. The new particle diameters are related to the old ones by:

$$d_{p2} = (3.8)^{(-1)} d_{p1} \quad (11)$$

thus resulting in diameters 11.85, 38.16, 78.95, and 184.23 μm .

However, this change in the diameter of the particles and therefore their mass means that by keeping the Reynolds number and the mass flow rate of the particles per unit area constant, the kinetic energy at impact of the particles corresponding to the same Stokes number is $(3.8)^{-3}$ times smaller and therefore their behavior during impaction will be different as shown in Fig. 6.

The change in the deposit build up can be explained by the reduction in the kinetic energy of the impacting particles, which changes the impact conditions. This is evident from the deposition further away from the stagnation point where particles directly impact onto the tube and instead of rebounding and being carried away as in the full scale configuration, they adhere. The dimensional spacing also plays an important role since the mass flow rate per unit area is the same but there is less area available for the particles to bypass the tubes.

The next test on the scaled down configuration was to adjust the particle size so that the impaction kinetic energy of the particles (a function of d_p^{-3}), as well as the Reynolds number of the flow, remains constant. Of course, the Stokes number is not maintained. The particle diameters now result as:

$$d_{p2} = (3.8)^{(-2/3)} d_{p1} \quad (12)$$

meaning diameters equal to 18.5, 59.5, 123.2, and 287.5 μm .

The result is shown in Fig. 7 where again there is a big difference in the fouling behavior of the tube bundle. Although the fouling rate is much slower than when the Stokes number was maintained constant, it is still faster than in the original case of the actual dimensions. However, the shape of the deposits are much closer to the original shapes of the full scale arrangement (Fig. 5). A possible explanation to this could lie in the difference in Stokes number, which is responsible for the change in deposition behavior behind the tubes. The deposit formation in this region depends more on Stokes number since it results from particles that have already impacted and are led to this area by the flow. The deposit in front of the tubes, which is a result of

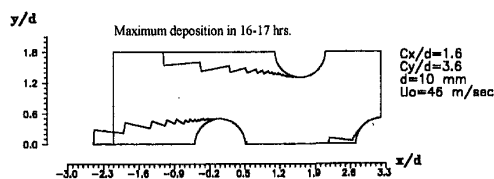


Fig. 6 Fouling in the staggered tube bundle of Fig. 5 scaled down 3.8 times but with the same Re number of the flow and the same particle Stokes numbers as for the actual dimensions

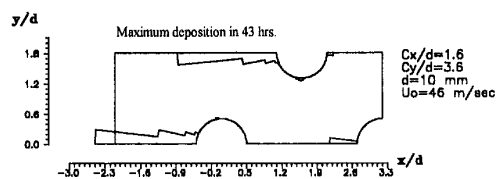


Fig. 7 Fouling in the staggered tube bundle of Fig. 5 scaled down 3.8 times but with the same Re number of the flow and the same kinetic energy of the particles as in the actual dimensions

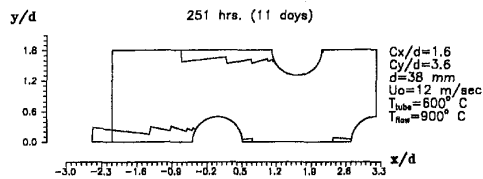


Fig. 8 Fouling in a full scale staggered tube bundle with temperature variation of the flow field. Fluid temperature: 900°C, Tube surface temperature: 600°C

more direct impaction, resembles closely the deposit in the full scale geometry. The shape of the deposit, as well as the time for maximum deposit, indicates that constant kinetic energy is more important than constant Stokes number as far as maximum deposition is concerned.

However, a definite similarity law could not be obtained. The conflicting mechanisms that are represented by the Reynolds number of the flow, the particle mass flow rate per unit area, the particle Stokes number, the kinetic energy of impaction of the particles, and the actual dimensions of the tube bundle could not be combined to give a reasonable method of scaling down the actual dimensions to study the fouling phenomenon.

Effect of the Tangential Impact Velocity. At this point, the initial condition that the tangential velocity during impaction is overlooked will be re-examined. The configuration of Fig. 5 is now solved accounting for the fluid density variation by solving the temperature equation for the flow. All other parameters regarding the flow and the particles are the same. The results are presented in Fig. 8 and it can be seen that the temperature difference leads to a faster fouling rate. There are two reasons directly related to this phenomenon. The first is the thermophoretic force that causes particles to be attracted to the colder tube surface, while the second is the larger density of the colder fluid in the vicinity of the tube, this will lead to increased drag on the particles (by about 30 percent) and cause them to impact onto the tube with smaller velocities thereby facilitating deposition.

Through a rather complex analysis of impact energies and material deformation, Bitter (1963b) reaches two expressions for the tangential rebound velocity. It actually turns out that this is dependant on the impact angle of the particle and that for an impact angle above a certain limit α_o , the tangential velocity becomes zero during impaction while below α_o the particle retains at least some of the tangential velocity all through the impact. The limiting impact angle α_o is most strongly influenced by the elastic yield limit Y and common values are between 12–25 degrees.

Depending on the impact angle the tangential rebound velocity V_{t2} is calculated and (8) becomes:

$$\tau_{er} = \frac{\Sigma [m_{imp}(V_{t2} - V_{t1})]}{A} \quad (13)$$

The tangential erosion calculated with (13) gives a curve which has a maximum at the α_o impact angle as opposed to (8) which has a maximum at 0 degrees. This represents the tangential erosion of a ductile material as opposed to a brittle material which would have maximum erosion at a normal impaction (90 degrees). The deposit material inside a heat exchanger which can be found at temperatures around 900 K can be assumed to be ductile and therefore the above expressions can be used.

The resulting calculation is presented in Fig. 9 where the tangential rebound velocity is now calculated based on Bitter (1963b) assuming $\alpha_o = 20$ deg for Al_2O_3 (the value for Aluminum is 15 deg). The deposits are now spread out over the surface of the tube due to the smaller tangential rebound velocities, which make it more difficult for the particles to escape and

causes them to stick further downstream and on the back of the cylinder. The overall result is an increase in the fouling rate by about 25 percent proving the importance of the tangential rebound velocity on the fouling process.

IV Conclusions

A numerical model has been developed to predict the deposition resulting from particles impacting a solid surface. The material properties of both particle and surface are taken into consideration as they affect the energy losses and the surface energies that will determine the sticking or rebound of the particle. The effect of the surrounding flow field is considered through a solution of the Navier Stokes equations and therefore the influence of local temperature on the material properties as well as the resulting thermophoretic force and the lift force are influential to the adhesion or rebound of the particle. The exact particle trajectory is also a result of the flow field calculation. The local shear stresses and the eroding particle impacts are considered as removal mechanisms and the evolution of the deposited mass in time is obtained. The model showed acceptable agreement with experimental measurements of particle mass flux deposited from high temperature particle laden gas flow around a circular cylinder. The effect of the thermophoretic force with regard to particle size was noted and the larger particles were found to deposit after one or two bounces. An analysis regarding the actual dimensions of a staggered arrangement heat exchanger tube bundle proved that the actual dimensions are extremely important to the fouling behaviour of the arrangement even though caution was taken to preserve various similarity laws for the flow and particles. Finally, the effect of the tangential velocity during impact and rebound was found to be an important factor in the fouling process.

The fouling phenomenon is undoubtedly very complex and can be found in many different situations. The numerical model presented in this paper is not intended to be a simulation of all the underlying mechanisms in fouling. Many of these, which could be of major importance in certain situations, have not been directly included in the model (e.g., chemical reactions, vapor condensation, vibration, flow field unsteadiness etc.). However, an attempt was made to include the most important mechanisms in utility boiler heat exchanger fouling without limiting the generality or expandability of the model. It is believed that the model can be a useful tool in heat exchanger design and as an operational guide for existing configurations. Investigation of the effect of different tube shapes and configurations on heat exchanger fouling and efficiency is the object of current research by the authors. Further study could be directed toward more exact values for the material properties, better flow field simulation through advanced turbulence modeling, and possibly a time resolved calculation to study the effect of unsteadiness and the deposit formation on the flow field.

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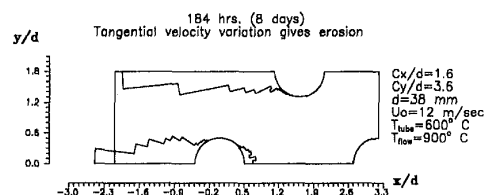


Fig. 9 As in Fig. 8 but with the tangential eroding shear stress of (13)

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