

THE EFFECT OF SILICA, TEMPERATURE, VELOCITY, AND PARTICULATES ON HEAT TRANSFER TO SPENT BAYER LIQUOR

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Abstract

Laboratory-scale investigations were performed to determine the effect of silica, temperature, velocity, and particulates on heat transfer to spent Bayer liquor. A method for simulating continuous process conditions was developed in the 60 L heat transfer test rig at the University of Auckland. Heat transfer was measured for conditions of 0.7 to 2.1 g/L $[\text{SiO}_2]$, 110 to 160°C surface temperature, 0.4 to 1.7 m/s flow velocity, and 0.0 to 0.3 g/L red mud concentration. A deposition model is proposed to determine the effect of silica and temperature on scaling rate. Similar to bulk desilication rate, the scaling rate was second order with respect to silica supersaturation, and had a similar Arrhenius temperature dependence. The effect of velocity on scaling rate was insignificant at heater surface temperatures below 150°C. The mild polishing effect on the heat transfer surface of the circulating red mud particulates was expected to have a beneficial effect on scaling. For liquors with high silica supersaturations, however, the presence of small amounts of red mud dramatically decreased the heat transfer coefficient.

Introduction

The eventual goal of this study is to formulate an accurate thermal model of spent liquor heaters. Heater fouling increases alumina production costs in a number of ways including additional capital expenditure for spare capacity, and increased maintenance and energy requirements. The driving force for formation of DSP and hence for heat exchanger fouling is the supersaturation of silica. The rate of fouling is a function of this supersaturation, liquor chemistry, heater surface condition, and temperature at the precipitation surface.

Review of Previous Fouling Investigations

Models predicting desilication behaviour as a function of temperature, liquor chemistry, and seed presence have been summarised recently (1). The majority of the kinetic models are second order with respect to silica supersaturation. Discrepancies in the rate behaviour have been observed. These discrepancies may result from neglecting the contribution to desilication from the seed surface area. The addition of seed particles increases the reaction rate approximately linearly with seed charge (1).

Previous experimental investigations of the effect of DSP scaling on heat transfer are described in (2-8). Investigations have been performed in both tubular (2,3) and double pipe (annular duct) configuration (5-8) heat transfer test rigs. The effects of operating parameters on scaling rates can be measured. Numerical models of shell and tube heaters have been proposed based on experimentally observed scale growth rates.

Cousineau and Fulford reported results of heat transfer measurements obtained using a steel tube as a resistance thermometer. Partial heat transfer coefficients (liquor to wall) can be obtained if the wall temperature is determined (2). Validation of the scaling rate model using the resistance thermometer indicated that increases in liquor velocity reduce equilibrium scale thickness. In (3), O'Neill reports results of 14 day experiments undertaken with Bayer liquor in a four pass horizontal tube heater assembly. A large effect of velocity on fouling was observed. Jamialahmadi and Muller-Steinhagen performed the most thorough experimental investigations using annular test sections similar to those used in this study (5-8). The laboratory investigations determined the effect of $[\text{SiO}_2]$, bulk temperature, wall temperature, velocity, and heat transfer mechanism on the heat transfer to spent Bayer liquor. Bulk temperature was found to have a negligible effect. Similarly, for liquors with relatively low silica supersaturations, the effect of velocity on fouling was negligible. At high silica supersaturations ($[\text{SiO}_2] > 1.2 \text{ g/L}$), however, increased velocity was associated with decreased fouling. This decrease in fouling was attributed to reduced particulate fouling at high shear rates. Increased surface temperature and increased $[\text{SiO}_2]$ were determined to have the greatest effects on fouling. A relationship was derived for the effect of $[\text{SiO}_2]$ on fouling.

Models of time dependent fouling resistances proposed in (3) and (5-8) are similar with differences due to different heat transfer test rig geometries. Both models assume a residence time for liquor calculated from the flow velocity and the incremental heat transfer volume of the tube or annular duct. The amount of silica precipitated on the heat transfer surface is calculated from batch desilication kinetics at the heater surface temperature for a volumetric flow rate. These models do not take into account that both bulk and surface precipitation of DSP occurs, and that only the surface precipitation reaction contributes to scaling. The experimental model derived in this study attempts to correct for these assumptions - bulk desilication kinetics are evaluated at the bulk liquor temperature, whereas surface desilication is evaluated at the temperature of the liquor / scale interface. This improved deposition model is presently being validated with refinery measurements. Numerical models for step-wise calculation of heat transfer through a multi-pass steam heated spent liquor heaters have been developed previously using the deposition models derived from the proposed time dependent fouling rate equations (3,4). These models combine local mass and energy balances with the kinetic behaviour of silica in the process stream to predict heat transfer. In (3) a model is presented averaging the heater performance as a function of time to scaling; however, the incremental heat transfer coefficients at each location in the heater are not calculated. In (4), the performance is calculated for each tube length increment; however, the results are for clean heat transfer only, and not as a function of time to scaling. The improved numerical heater model being developed in conjunction with this study predicts both time and length dependent scaling, $[\text{SiO}_2]$, and temperature profiles in each heater of the heater train for given operating conditions.

Effect of Silica Concentration

The effect of [SiO₂] on fouling can be determined only when constant [SiO₂] is maintained over the heaters. If [SiO₂] depletes during experiments, the fouling rate becomes a function of initial conditions and test rig configuration. The experimental approach in (5-7) did not simulate continuous conditions. Initial silica levels were not maintained for the remainder of each test and the liquor desilicated. The driving force for fouling is proportional to the square of the silica supersaturation. Therefore, all laboratory tests reported in (5-7) were scaling at rates where the driving force for precipitation was decreasing exponentially with time. The effect of [SiO₂] on fouling was determined from the initial slope of the fouling resistance vs time curve only. Satisfactory agreement of the model with plant measurements was obtained.

Bulk and wall temperatures, flow velocity, and heat transfer mechanism have been carefully controlled in previous studies. The investigation reported here reports fouling under controlled silica and particulate concentrations, conditions previously uncontrolled in other studies.

Effect of Particulates on Fouling

The particulate loadings in spent liquor have a significant effect on scaling rates. Results of analyses of scale samples removed from spent liquor heaters indicate a high particulate content, identified as slurry fines. A number of trials have been conducted to assess effect of particulates (generally mud or DSP) on heater performance. As shown in (1), the desilication rate in spent liquor is dramatically increased by addition of small amounts of mud (20 g/L). The increase in desilication rate was shown to be proportional to mud charge between 0 and 20 g/L. Decrease in spent liquor [SiO₂] and mild polishing of tubes would be beneficial aspects of mud addition. However, particulate fouling and decrease in heater scale thermal conductivity would be detrimental to heater performance. Benefits from fine mud overflow to spent liquor heaters are described briefly in a split stream digestion patent (10).

Fundamentals of Fouling in an Annular Test Section

Shell and tube liquor heaters transfer heat from a hot cylindrical tube wall to the process liquor inside. However, the experimental heat transfer test rig is an annulus with a heated core. A cross section of the heated core is shown in Figure 1. This system can be analysed as a cylindrical system experiencing temperature gradients in the radial direction only with overall heat transfer expressed in Equation [1].

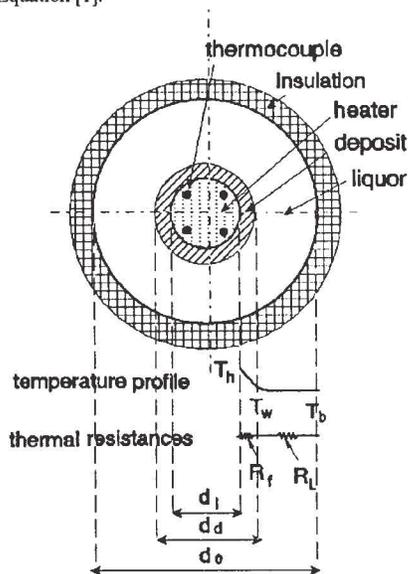


Figure 1 : Thermal Cross Section of Annular Probe Test Section

$$U = \frac{Q_r}{A_r \cdot (T_h - T_b)} \tag{1}$$

T_h is determined by adjustment for depth of average thermocouple temperatures, calculated using a Wilson Plot technique described in (12). The bulk temperature of the liquor is calculated from temperature measurements before and after the heater. The heat flow rate, Q_r, is obtained from measurement of electrical power input to the heaters. The two resistances to heat transfer from the wall to the bulk of the liquor shown in Figure 1 are:

1. liquid side resistance - R_L
2. fouling resistance due to the scale - R_f

For a clean heater, the initial heater wall temperature is a function of the heat flux, and the flow velocity. High velocities increase the heat transfer coefficient and therefore decrease the wall temperature. High q_r increase the wall temperature. In this study, q_r remains constant with time, and the heat transfer coefficient decreases due to scaling. For constant q_r operation, the heater wall temperature will increase with fouling; however, the liquor/scale interface temperature will remain constant at the initial (clean) wall temperature. This simplifies the kinetic analysis of fouling, as constant surface temperature at the scaling / liquor interface, T_w, is maintained. The constant heat flux operation of the test rig is similar to live steam heat exchangers.

The overall heat transfer coefficient based on the scaled heater diameter is determined by the Ohm's Law analogy for the additivity of thermal resistances according to Equation [2]:

$$U = \frac{1}{R_{tot}} = \frac{1}{R_L + R_f} \tag{2}$$

Assuming that the thickness of deposit is small in comparison to the heater rod diameter, the temperature distribution in the deposit is almost linear in the radial direction. Initially, the heater is clean and R_f(0) is zero. Therefore, the initial overall clean heat transfer coefficient, U(0), can be expressed in Equation [3]:

$$R_L(0) = \frac{1}{U(0)} \tag{3}$$

Assuming R_L to be independent of time, the time dependent thermal resistance due to the deposit can therefore be determined in Equation [4]. Thermal resistances (fouling resistances) are used to describe the fouling behaviour.

$$R_f(t) = \left(\frac{1}{U(t)} - \frac{1}{U(0)} \right) \tag{4}$$

Liquor Side Resistance

As described in (2,5), a large number of correlations for the prediction of forced convective heat transfer can be obtained in the literature. These express the Nusselt number as functions of Re, Pr, roughness, etc. Previous heat transfer investigations with spent liquor (5) indicate that the Gnielinski equation modified for annular flow is suitable (10). The liquid side heat transfer resistance is calculated from this Nusselt number similar to Equation [5].

$$N_{ub} = \frac{\frac{\xi}{8} ((Re_b - 1000) \cdot Pr_b)}{1 + 12.7 \sqrt{\frac{\xi}{8}} \cdot (Pr_b^{\frac{2}{3}} - 1)} \cdot \left(1 + \frac{1}{3} \left(\frac{d_h}{X_{th}} \right)^2 \right) \cdot 0.86 \left(\frac{d_d}{d_o} \right)^{-0.16} \cdot \left(\frac{Pr_b}{Pr_d} \right)^{0.11} \tag{5}$$

Equation [5] is a modification of the Nusselt Number for a tube (10). The characteristic length used to evaluate the dimensionless numbers Nu, and Re is the hydraulic diameter, d_h of the annular duct, as opposed to d_o for tubes. In the limiting case of clean heat transfer, d_d is equal to d_i.

$$d_h = d_o - d_d \quad [6]$$

$$Re_b = \frac{v \cdot d_h}{\nu_b} \quad [7]$$

$$Pr_b = \frac{\mu_b \cdot c_{p,b}}{\lambda_b} \quad [8]$$

The friction factor can be calculated for turbulent flow in technically smooth pipes from the Filonenko equation as in:

$$\xi = (1.82 \log_{10}(Re_b) - 1.64)^{-2} \quad [9]$$

In the second term in Equation [5], X_{th} represents the length of the heated section to the thermocouple location which is 8.26 cm for all experiments. In the third term, a correction factor of the form:

$$\Theta = 0.86 \cdot \left(\frac{d_d}{d_o}\right)^{-0.16} \quad [10]$$

is used. Heat transfer coefficients for flow in concentric annular ducts are dependent on the ratio dd/d_o in addition to Re , Pr , and d_i/X_{th} , since annuli are not geometrically similar unless the ratio d_d/d_o is identical. With annular flow, the maximum velocity is shifted to the inner wall as the ratio d_d/d_o decreases. The final term in Equation [5] is for the viscosity differences at the wall and bulk. Typical velocities of 0.9 m/s in 90°C spent liquor in the annular duct correspond to a Re_b of $2 \cdot 10^4$, which is in the turbulent region. Natural convection does not noticeably contribute to heat transfer in this region, and is neglected.

Resistance Due to Scale

The scale resistance is determined according to Equation [11]:

$$R_f = \frac{d_i \cdot \ln \frac{d_d}{d_i}}{2 \cdot \lambda_d} \quad [11]$$

Measurements of the thermal conductivity of plant heater scale have been performed with a thermal conductimeter to confirm the estimates of thermal conductivity. To determine the change in scaled diameter the thickness of scale deposited can be predicted from the following silica rate equation derived for an incremental cross-sectional volume in a heated section [12].

$$\begin{aligned} \frac{d}{dt}(M_{S,tot}) &= -\frac{d}{dt}(M_{S,wall}) - \frac{d}{dt}(M_{S,bulk}) \\ &= -A_i \cdot K_{wall} \left([SiO_2]_t - [SiO_2]_{eq} \right)^2 - V \cdot K_{bulk} \left([SiO_2]_t - [SiO_2]_{eq} \right)^2 \end{aligned} \quad [12]$$

where

- M_S = mass of silica precipitated [kg]
- $[SiO_2]_t$ = silica concentration [g/L] at time t
- $[SiO_2]_{eq}$ = silica concentration [g/L] in equilibrium with DSP
- K_{bulk} = bulk desilication rate constant [$m^3/kg \text{ min}$]
- K_{wall} = surface desilication rate constant [$m^3/kg \text{ min}$]
- V = incremental volume in heated section [m^3]
- A_i = heat transfer area in incremental volume [m^2]

The first term on the right hand side in Equation [12] models the desilication at the hot wall. The rate constant, K_{wall} , describes surface deposition [$m^3/kg \text{ min}$], and is evaluated at T_w . All silica precipitation described by this term is assumed to adhere onto the heated wall, and only precipitation contributed from this term results in fouling. The second term on the right hand side of equation [12] describes the volumetric desilication [$m^3/kg \text{ min}$], and is evaluated at the T_{bulk} . The rate constant, K_{bulk} , describes the bulk desilication. All silica precipitation predicted by this term is assumed to nucleate in the bulk and remain in the liquor. Allowing for a surface deposition reaction rate distinguishes this model from previous models (3,5-8). The rate of change of scaled diameter with time is described in Equation [13]. Since constant $[SiO_2]$ was maintained during all fouling runs, the time derivative of the supersaturation is zero.

$$\frac{d}{dt}(d_d) = \left(\frac{2}{A_i \cdot \rho f} \right) \cdot \frac{d}{dt}(M_{S,wall}) = \frac{2K_{wall}}{\rho f} \left([SiO_2] - [SiO_2]_{eq} \right)^2 \quad [13]$$

where ρ = DSP density (kg/m^3)
 f = mass fraction of silica in DSP (0.357)

Solving Equation [13] with initial boundary conditions ($d_d(0)=d_i$) yields the time dependence of the scaled diameter:

$$d_d(t) = \frac{2K_{wall}}{\rho f} \left([SiO_2] - [SiO_2]_{eq} \right)^2 t + d_i \quad [14]$$

Substitution of Equation [14] into Equation [11] gives the time dependent thermal resistance for scale in Equation [15].

$$R_f(t) = \frac{d_i \cdot \ln \left[\frac{2K_{wall} \left([SiO_2] - [SiO_2]_{eq} \right)^2 t + d_i}{d_i} \right]}{2\lambda_d} \quad [15]$$

Experimental

Fouling Rig

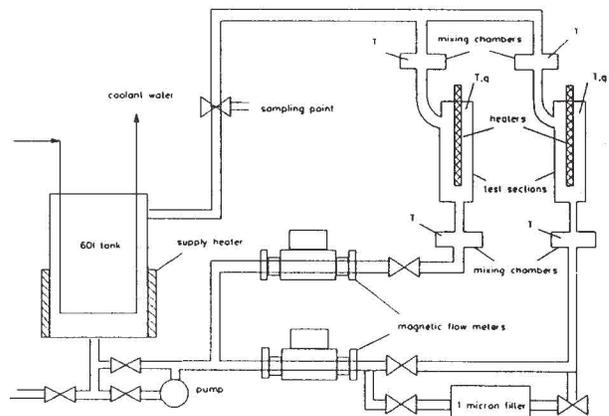


Figure 2 : Schematic of Heat Transfer Test Rig

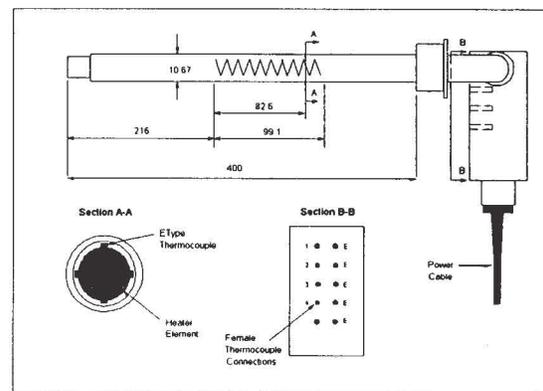


Figure 3 : Schematic of Heat Transfer Test Rig Heater Probe

A schematic of the heat transfer test rig used for the present investigation, is shown in Figure 2. All wetted parts in the test rig were stainless steel. The liquor flows in a closed loop consisting of a tank, pump, and annular test sections. The temperature in the 60 L tank is maintained at 90°C by 3 band heaters, a PID temperature controller, and a thermocouple immersed in the centre of the tank. Cooling water and 2 tank heaters prevent temperature oscillation in the tank ($\Delta T_{\text{bulk}} < 0.7^\circ\text{C}$). An APV 2 hp pump with water cooled seal is used to circulate the liquor. A 1 μm PTFE filter is used at temperature of 80°C for 30 minutes prior to each test to remove particulates that could act as seed. Flow velocities through the test section are independently controlled by 2 gate valves at the entrance to each section. The flow velocity is measured by 2 magnetic flowmeters with 15 mm bore size. Both flowmeters are calibrated frequently because the inset electrodes have a tendency to scale. The velocity was limited by the power of the pump. Sufficient liquor pressure had to be maintained in order to suppress boiling at the hot heater surface. The pressure in the test section was measured with gages, and could be adjusted with a downstream control valve. Fluid temperatures were measured with K-type thermocouples in 5" mixing chambers located before and after the test sections. The bulk temperature at the thermocouple plane in each loop was calculated from a function of the loop inlet and outlet temperatures. The heater consisted of an electrically heated cylindrical rod made of stainless steel, which is mounted concentrically within the surrounding pipe. A schematic of the heater is in Figure 3. Silica additions were performed using a peristaltic pump with either 1.0 or 3.2 mm diameter tubing.

Procedure

Sixty litres of liquor made up to aim concentrations were added to the test rig. The liquor was continuously filtered and heated to 90 °C. For mud containing trials, new mud was added at the start of each test after liquor filtering. The initial aim $[\text{SiO}_2]$ was attained by addition of diluted water glass. This concentration was maintained for the remainder of the test by continuous addition with a peristaltic pump of silica solution to the test rig. As expected, the dose rate required varied linearly with the square of the desired supersaturation. Typically, $[\text{SiO}_2]$ was monitored each hour for 2 to 3 hours to ensure a steady-state concentration analogous to a continuous process. Fouling trials commenced if fluctuations in $[\text{SiO}_2]$ were sufficiently low ($\Delta[\text{SiO}_2] / \text{hr} < \pm 0.05 \text{ g/L}$). When a stable $[\text{SiO}_2]$ was obtained, the liquor pump was stopped momentarily, and the heaters were installed into the annular test sections. After restarting the liquor pump, the flow velocity and liquor pressure in each section were adjusted. Initially, the electrical input to the heaters was adjusted to give a correct T_w . This initial heat flux, q , was maintained for the remainder of the experiment. Measurements of v , T_{bulk} , T_h , and q were made every 10 minutes, and the time dependent heat transfer coefficient, U , was calculated.

Liquor Analysis Techniques and Conditions

Monitoring of the $[\text{SiO}_2]$ and CS, A/C, and C/S was performed regularly using standard techniques described in (1). Synthetic liquor of similar composition to plant spent liquor was prepared with commercial grade NaOH and Na_2CO_3 , and Kwinanna trihydrate using standard liquor preparation techniques. Aim CS, A/C, and C/S of all liquors were 225 g/L, 0.38, and 0.85 respectively. Periodically, small amounts of liquor were removed from the rig and replaced with make-up liquor to maintain aim concentrations. $[\text{SiO}_2]$ in the liquor was adjusted with silica addition solution prepared from BDH water glass diluted to an optimal concentration (6.66 v% water glass, $[\text{SiO}_2]=30.1 \text{ g/L}$). This silica addition solution was sufficiently dilute to prevent precipitation on addition; however, the dilution was not so great as to sufficiently reduce the CS and alter DSP solubility in the test rig. Liquor dilution was prevented by controlled evaporation through the tank lid.

Last washer underflow mud was used in experiments investigating the effect of mud on heat transfer. The mud was added as a wet slurry to the spent liquor and circulated identically as in the mud-free case. Mud charges reported are from measurements of solids content of liquor withdrawn through

a pipette from the middle of the tank. Aliquots of liquor were centrifuged at 3000 rpm for 10 minutes, and the amount of mud recovered was measured.

Results

Control of Silica Concentration

In comparison to the effect of T_w , it has been shown that T_{bulk} has little effect on the fouling behaviour in spent liquor (6,7). Therefore, 90°C was selected as the bulk temperature for all trials. Typical ΔT values ($T_h - T_{\text{bulk}}$) are much larger in the laboratory experiments than in typical spent liquor heaters. However, for T_{bulk} greater than 90°C, the high silica dose rate required for constant $[\text{SiO}_2]$ would appreciably dilute the test rig liquor. Because even small mud charges have a large effect on desilication kinetics (1), mud-containing heat transfer experiments were performed in low silica concentration liquors (0.7 to 1.2 g/L). In mud containing liquors, the silica dose rate required to maintain constant silica concentrations above 1.2 g/L would cause significant uncontrollable dilution of the rig liquor. The desilication behaviour determined in batch desilication trials was used to predict the dose rate required to maintain constant silica.

An example of the time dependent $[\text{SiO}_2]$ is shown in Figure 6 for Run 6. The initial $[\text{SiO}_2]$ in the heat transfer test rig is approximately 1.5 g/L. However, after 1700 minutes with 0.46 ml/l silica additional solution dose rate, the $[\text{SiO}_2]$ stabilises to 1.1 g/L. Zero time in Figure 6 corresponds to the heaters switched on. Typical standard errors in silica were 0.01 to 0.02 g/L.

Fouling Experiments

Determination of Rate Constants

A typical heat transfer coefficient vs time plot is shown in Figure 7 for Run 5. The experimental conditions were $T_w=140^\circ\text{C}$, $[\text{SiO}_2]=1.89\text{g/L}$, and $v=0.90\text{m/s}$. The heat transfer coefficient decreases uniformly from 6000 $\text{W/m}^2\text{K}$ to just over half that value after 1600 min. The time-dependent fouling resistance calculated according to Equation [4] is shown as 'data' in Figure 8, and appears almost linear with time. Excellent agreement of the time dependent fouling resistance with the model obtained from linear regression is observed. The value of K_{wall} obtained by regression analysis for Run 5 was $8.27 \times 10^{-6} \text{ m}^4/\text{kgmin}$ with a standard error of $8.4 \times 10^{-7} \text{ m}^4/\text{kgmin}$. Similarly, fouling resistance vs time data for all subsequent trials were regressed to fit the model given in Equation [15]. Sensible values of scale thermal conductivity and silica scale fraction were assumed ($\lambda_d = 0.3 \text{ W/mK}$, $f=0.357$, $\rho = 2200 \text{ kg/m}^3$). This value of thermal conductivity has been confirmed using measurements using a thermoconductimeter. The standard error between the model and data was minimised for a value of K_{wall} . Similar analysis was performed for all subsequent experiments. In some trials, the heat transfer coefficients increased during the initial measurement period (30 to 100 minutes). This increase results from increased roughness as DSP nucleates on the surface. However, after sufficient time, the insulating effect of the growing deposit reduces the heat transfer coefficient below initially recorded values for the remainder of the test. This initial increase was not observed for heaters placed in high silica liquors ($[\text{SiO}_2] > 1.8\text{g/L}$) or in the flow stream for an hour before operation.

Effect of Temperature

The results of the regression analyses of all experiments with different $[\text{SiO}_2]$ and T_w are plotted in Figure 5. The K_{wall} values predicted by Equation [15] for all $[\text{SiO}_2]$ and T_w , converge to a single function of T_w (horizontal axis). This indicates that the proportionality factor ($[\text{SiO}_2] - [\text{SiO}_2]_{\text{eq}}$)² in Equation [15] is correct. As expected, both the bulk and surface rate constants have identical dependence on the square of the silica supersaturation. The natural logarithm of the rate constant was regressed as a function of $1/T_{w,K}$ (Figure 5) and the results of

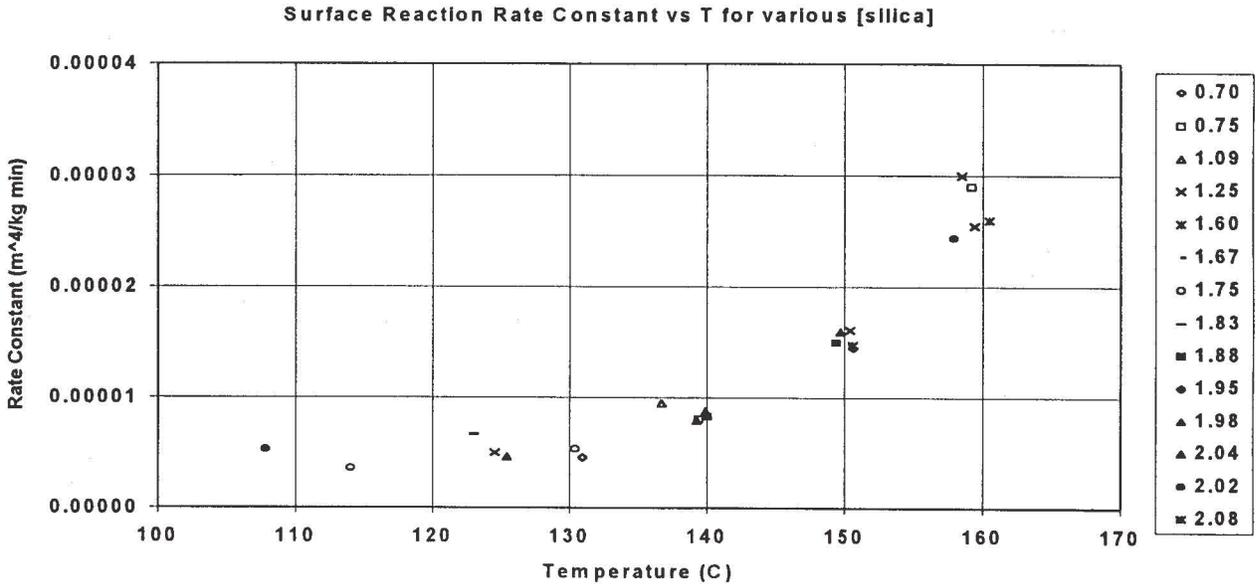


Figure 4: [SiO₂] denoted in legend

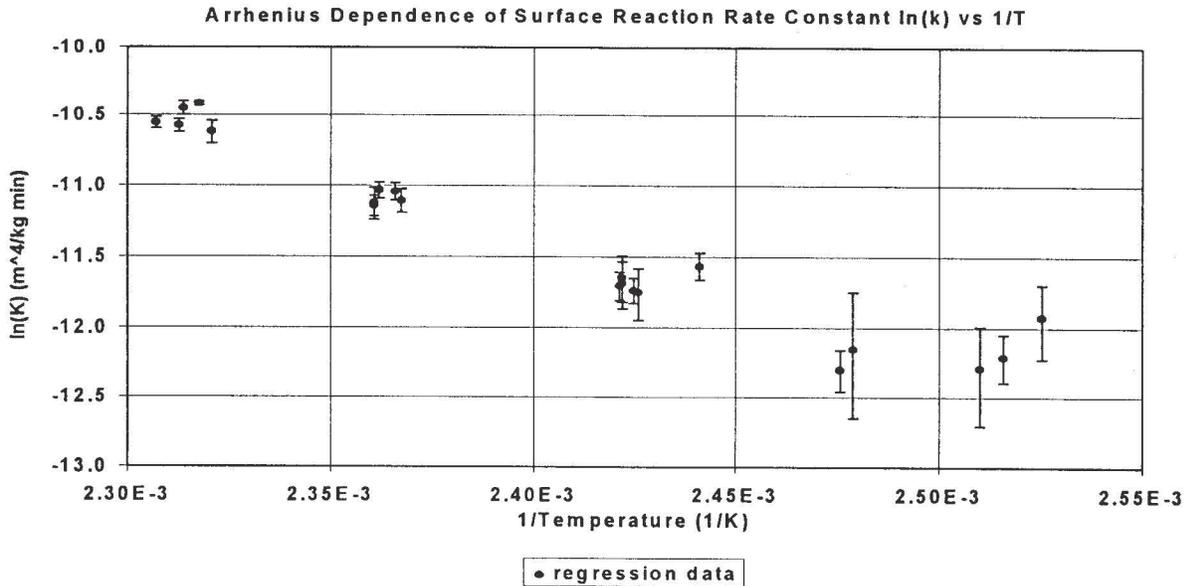


Figure 5 :Slope = -9262 ± 449, Intercept = 10.84 ± 1.08, r²=0.98, regression performed for 125°C-160°C T_{wall}

the regression are expressed in Equation [16]. The standard errors associated with the measurements are also shown in Figure 5.

$$K_{wall} = 51021e^{-\frac{9262}{T_w, K}} \quad [16]$$

The Arrhenius temperature dependency determined in this study for surface reaction desilication (9262K) is similar to the temperature dependencies determined in other studies (1,3,8,9) for bulk desilication (6766K to 10960K).

Effect Of Constant Silica Vs Desilicating Liquor On Fouling

Comparisons of the fouling behaviour in a desilicating and constant silica liquor were undertaken to assess the importance of maintaining constant

[SiO₂]. The [SiO₂] profiles for constant silica conditions (Run 7) and desilicating conditions (Run 8) are shown in Figure 9. For Run 7, the [SiO₂] was maintained at 1.75 g/L. However, for Run 8, the liquor desilicated from 2.2 g/L [SiO₂]. Identical operating conditions were maintained in both flow loops for Runs 7 and 8 (v=0.9 m/s, T_{bulk} =90°C for both loops, initial T_w of 112 and 125°C in loops 1 and 2 respectively). The scaling rates at both T_w's are reduced dramatically when the rig liquor is allowed to desilicate. The measurements performed in (5-8) were under similar desilicating conditions as in Run 8, although from lower initial silica concentrations (0.6 to 1.6 g/L, although modelled only to 1.2 g/L). The discrepancies observed in scaling rate results between Runs 7 and 8 indicate the importance of maintaining constant silica supersaturation.

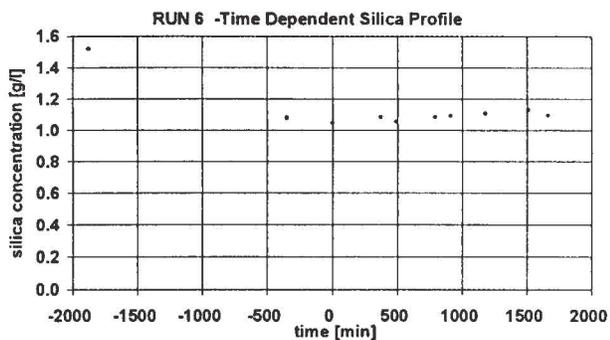


Figure 6

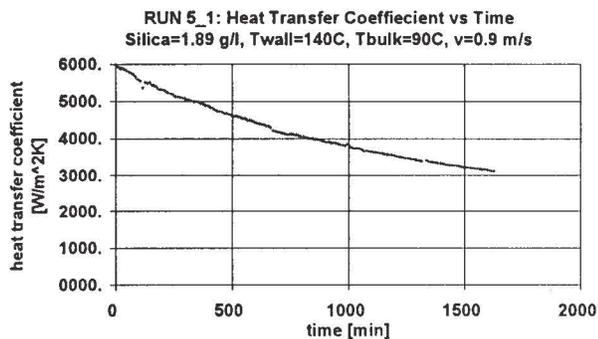


Figure 7

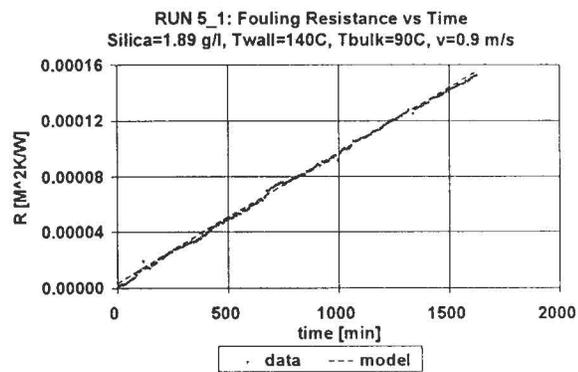


Figure 8

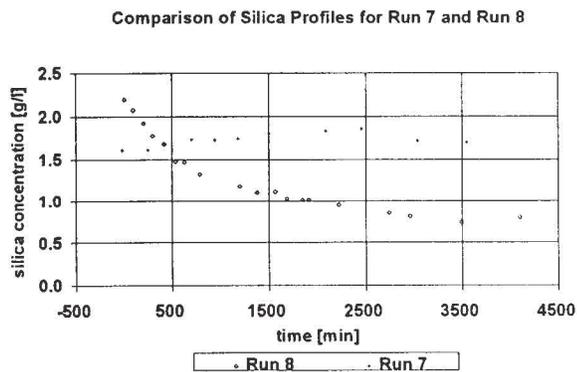


Figure 9

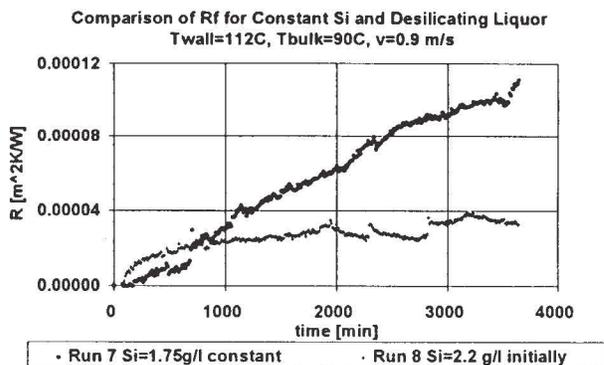


Figure 10: Loop 2

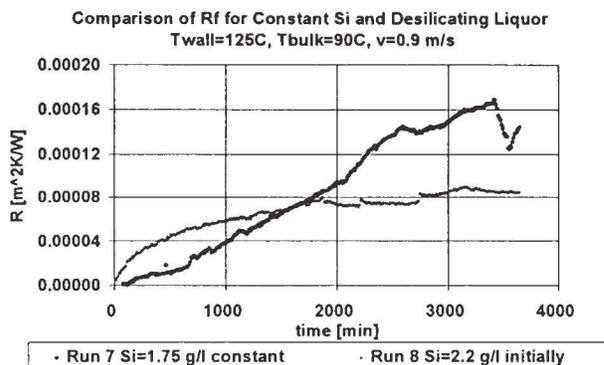


Figure 11: Loop 1

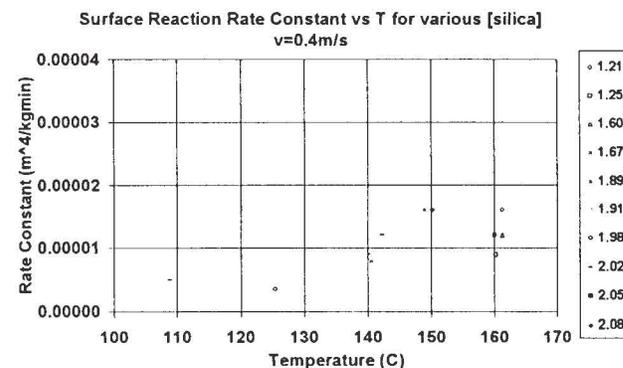


Figure 12

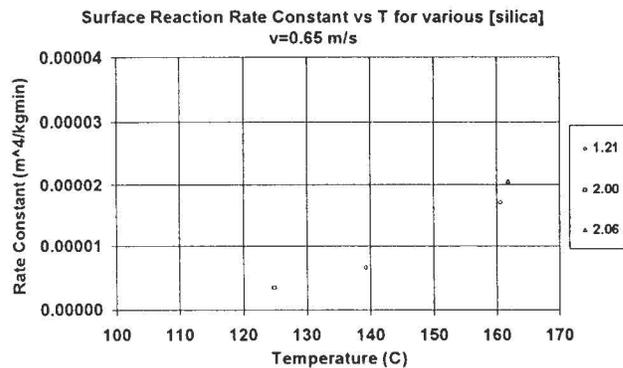


Figure 13

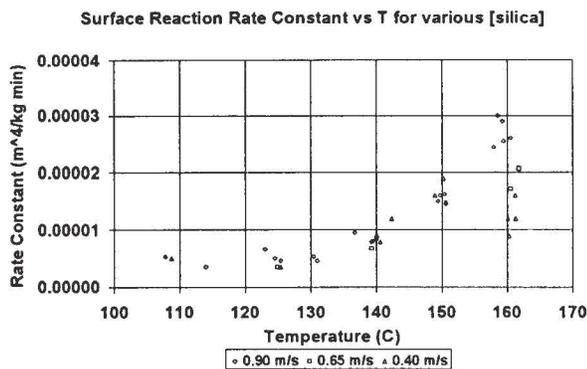


Figure 14

Effect of Velocity on Fouling

Results of fouling experiments performed at the lower velocities of 0.40 and 0.65 m/s are shown in Figure 12 and Figure 13 and a comparison of all velocities is shown in Figure 14. Scaling reaction rate constants at 0.40 and 0.65 m/s exhibit Arrhenius-type temperature dependence in the velocity range 0.40m/s to 0.90 m/s and temperature range 110 to 150°C with no significant effects of velocity on scaling rates. This indicates that the scaling is unlikely to be mass transfer controlled or the result of particulate fouling. This is contrary to the results in (6,7), where particulate fouling was observed at silica concentrations over 1.2 g/L [SiO₂]. The absence of particulate fouling in this study is consistent with adequate pre-filtering to 1µm, in contrast to no filtering in (6,7). DSP in solution precipitates in the bulk as 1-5 µm “wool-ball” structures. It is possible that some of these particles deposit on the heaters. However, the deposit on the heaters was hard and crystalline. In contrast to particulate fouling, there appears to be a consistent increase in scaling rate with increase in velocity at 160°C. This may be due to:

1. Fouling mechanism becomes dependent on the [SiO₂] gradient at high T_w. The diffusion of silica to the surface becomes the rate limiting step at high T_w. Because of the high [SiO₂] and small v range, it is unlikely this occurs to a significant extent.
2. More likely, the heat transfer mechanism at low v will have contributions of both convective heat transfer and subcooled nucleate boiling. The vapour pressure of spent liquor (TS=265 g/L) at 160°C is 491 kPa compared with test section pressures (470-500 kPa). Vapour bubble formation and detachment at the heat transfer surface increases heat transfer by generating additional turbulence in the thermal sublayer. Heater operation in the boiling region has been suggested as a scale mitigation scheme (8).

Effect of Mud on Heat Transfer

Figure 15 and Figure 16 show the effect of mud additions on heat transfer coefficient. These heat transfer measurements correspond to a fouling resistance profiles given in Figure 17 and 18 respectively. The mud has a significant effect on the heat transfer behaviour at both 125 and 150°C almost immediately upon addition. Initially, the effect of mud is to slightly increase the heat transfer coefficients from turbulence increases at the heater surface. However, the scaling rate increases dramatically at longer times.

Examination of heater deposits indicates that the mud is incorporated into the DSP deposit, thereby increasing the porosity and decreasing thermal conductivity. High pressure water washing of the heater probe removed only a small amount of surface mud, indicating that the mud was bound in the DSP. For constant silica concentration of [SiO₂]=1.0 g/L, the fouling rates at both 125 and 150°C were consistently reduced by increases in flow velocity from 0.40 to 1.7 m/s. However, at T_w=125°C, and [SiO₂]=1g/L, scaling rates measured at 1.7 m/s in solutions containing 0.3 g/L mud were consistently greater than rates measured at 0.40 m/s in particulate-free liquors.

For the conditions investigated here, the addition of 0.3 g/L mud was detrimental to heat transfer in the annular test sections. In a continuous process, the liquor in contact with mud circulating would desilicate as it proceeds through the heaters. In contrast, the [SiO₂] was maintained at a constant level in the heat transfer test rig. As a result, the beneficial desilicating effects of mud to reduce scaling rate were prevented from occurring. Therefore, only the ability of mud to act as a polishing agent on the heat transfer surface at specific operating conditions was evaluated here.

Conclusions

The rate of increase in heated element diameter due to scaling has been modelled. The rate of scale growth is proportional to the square of the silica supersaturation, and the surface reaction rate constant K_{wall}. The reaction rate constant is evaluated at the wall temperature. The fouling resistance given in Equation [15] is consistent with observed behaviour. The activation energy for the surface reaction is comparable to that determined in bulk desilication kinetics indicating similar temperature dependencies for bulk and surface scaling reaction. Scaling rate was velocity independent in the range 110 to 150°C, and 0.40 to 0.90 m/s. Mud charges of 0.3 g/L have been shown to have detrimental effects on heat transfer in the velocity range 0.4 to 1.7 m/s and silica concentration greater than 0.8 g/L. Mud contributes to increased scaling rates by particulate fouling, as well as being incorporated into the deposit.

Nomenclature

A _d	[m ²]	area scaled surface
A _i	[m ²]	area of heat transfer surface
c _{p,b}	[J/kg.K]	specific heat capacity
d _o	[m]	outer diameter of annular test section
d _h	[m]	hydraulic diameter of annular test section
d _i	[m]	outer diameter of clean heater probe
d _d	[m]	outer diameter of scaled heater probe
f		weight fraction of silica in DSP
K _{wall}	[m ⁴ /kg.min]	scaling rate constant, surface
K _{bulk}	[m ³ /kg.min]	desilication rate constant, bulk
m	[g/L]	mud charge
M _{S,tot}	[kg]	total mass of silica removed
M _{S,wall}	[kg]	total mass of silica removed at the heater
M _{S,bulk}	[kg]	total mass of silica removed in the bulk
Nu _b , Pr _b , Re _b		bulk Nusselt, Prandtl, Reynolds number
Pr _d		Prandtl number at the deposit
Q _r	[W]	heat flow rate
q _r	[W/m ²]	heat flux
R _r	[m ² .K/W]	thermal resistance of the scale
R _L	[m ² .K/W]	thermal resistance of the liquor
[SiO ₂] _t	[g/L]	silica concentration at time t
[SiO ₂] _{eq}	[g/L]	equilibrium silica concentration
[SiO ₂]	[g/L]	time independent silica concentration
T _h	[C]	temperature at heater wall
T _{bulk} , T _b	[C]	temperature in the bulk
T _{wall} , T _w [C], T _{w,k} [K]		temperature at scale/liquor interface
U	[W/m ² .K]	overall heat transfer coefficient
v	[m/s]	liquor flow velocity in annular test section
V	[m ³]	incremental volume
ξ		friction factor
X _{th}	[m]	thermal length in heaters
φ	[g/L.min]	silica additional solution flowrate
⊖		Nu number boundary condition for annulus
λ _d	[W/m.K]	thermal conductivity of DSP
ρ	[kg/m ³]	DSP density
μ _b	[kg/ m.s]	dynamic viscosity in the bulk
ν _b	[m ² /s]	kinetic viscosity

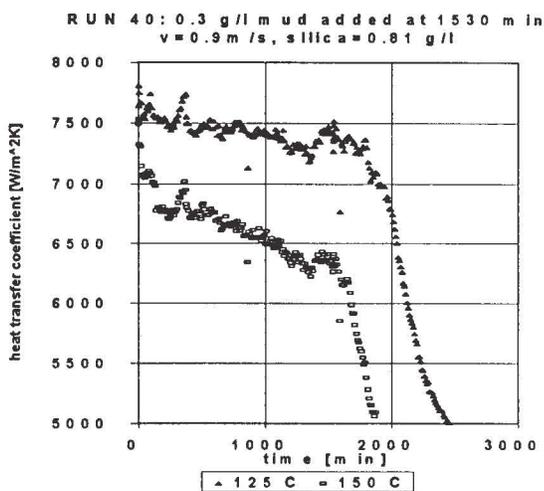


Figure 15

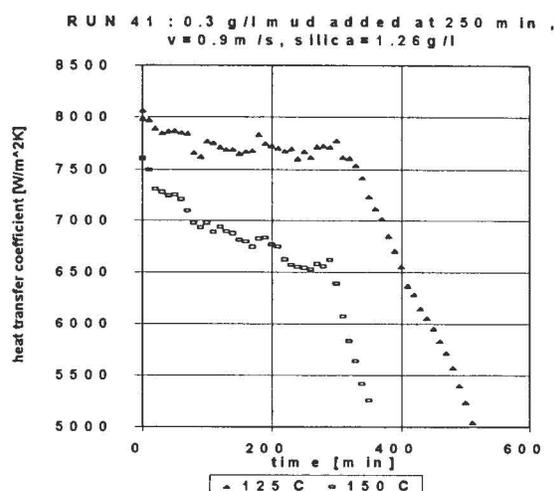


Figure 16

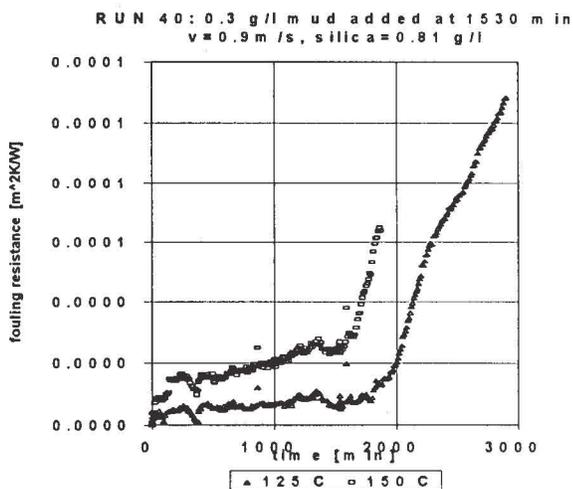


Figure 17

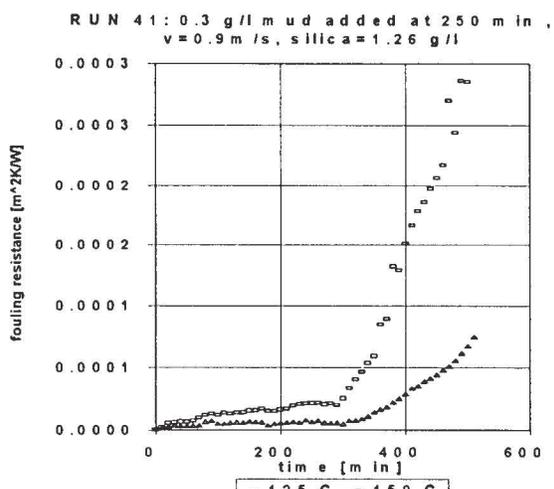


Figure 18

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