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THE APPLICABILITY OF FAN SPRAY NOZZLES TO STRIPPING
INSOLUBLE GASES FROM VISCOUS LIQUIDS

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THE APPLICABILITY OF FAN SPRAY NOZZLES TO STRIPPING
INSOLUBLE GASES FROM VISCOUS LIQUIDS

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ABSTRACT

Fan spray nozzle stripping appears to be a practical technique for separating dilute volatile solutes from nonvolatile solvents. In particular this technique can be used to strip molecular tritium and tritium fluoride at extremely small concentration (in the parts per million range) from molten salts used as blanket materials in a fusion reactor. Under adjusted operating conditions of the fan spray nozzle, a high percentage of the theoretically maximum-achievable stripping would take place from the expanding sheet of the fan spray as it leaves the nozzle and before it breaks up. Although the only available experimental data are for aqueous solutions, a new theoretical analysis of the fan spray sheet demonstrates the applicability of this technique to nonaqueous liquids. The equation derived from this analysis relates the theoretically achievable mass-transfer efficiency to the properties of the liquid flowing through the fan spray nozzle and to the operating conditions of the nozzle. Any fluid with viscosity higher than or equal to that of water would be expected to follow this equation as long as a fan-shaped sheet is formed under the operating conditions of the nozzle.

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I. INTRODUCTION

The stripping or absorption of sparingly soluble gases from or into nonaqueous solutions in fan spray nozzles is explored here. Although nonaqueous solutions like molten salts or mixtures of hydrocarbons are useful in industry, very little information has been published about their behavior in spray nozzles. However, the heat and mass transfer of sparingly soluble gases from aqueous solutions through spray nozzles has been studied by several authors. Hasson et al.¹ developed a theoretical analysis of vapor condensation on laminar water sheets produced by fan spray nozzles. This analysis was adapted to the study of the desorption of gases from aqueous solution sheets issuing from impingement nozzles by Simpson and Lynn² it was also adapted by Tamir and Rachmilev³ to the analysis of the absorption of carbon dioxide in a rapid laminar water sheet issuing from fan spray nozzles. This adaptation, which was by direct heat and mass transfer analogy, showed that more than 90% of the theoretical maximum achievable stripping (or absorption) would take place from the expanding aqueous solution sheet before breakup for both cases. Thus fan spray nozzles can provide an efficient and compact design for appropriate separations.

In order to evaluate this technique for nonaqueous as well as for aqueous solutions, we develop here a mathematical model derived directly from the general equation of convective mass transfer. The conditions for the validity of the analysis derived from the equation of motion identify the applicability of the technique for nonaqueous solutions. Since the control-volume balance approach is very difficult to apply in systems with curved streamlines, we start instead with the general equations for binary diffusion and motion and use tensor analysis.

II. MATHEMATICAL MODEL

The main features of the sheets produced from rectangular-orifice fan spray nozzles studied by Dombrowski et al.⁴ are: laminar behavior with straight streamlines in the spray sheet, constant liquid velocity along the sheet, and a continuously decreasing thickness in the direction of the flow caused by expansion of the sheet. The streamlines originate from a line source of high pressure behind the orifice, and the origin is taken at the center of this line source (see Fig. 1a). In the following analysis the sheet geometry may be described as a sectorlike shape from an attenuating disk where the angle between the sector radii is ϕ radians. In this shape the streamlines flow radially from the line source, which is the axis of the attenuating disk. A longitudinal section through the sheet is shown in Fig. 1b.

The assumptions made in developing the mathematical model for this study are:

1. Steady-state operation.
2. No chemical reaction in the sheet.
3. Laminar behavior in the fluid sheet.
4. Uniform liquid velocity V along the streamlines.
5. Very small ratio of the sheet thickness to radial distance along the central line of the sheet element, y/x .
6. Zero surface resistance to mass transfer, i.e., for the sparingly soluble gas the concentration of the dissolved gas at the surface of the sheet is in equilibrium with the partial pressure of the gas in the bulk of the gas phase outside the sheet.
7. Sheet is symmetric about its center planes (Fig. 1b).

8. Constant fluid properties.
9. Negligible x-direction diffusion as compared with diffusion in the y-direction.

A. Conservation of mass

Consider in Fig. 2 the central axes of two sheet elements subtending a very small angle $d\phi$ at the orifice. At a distance x from the origin O , the volume flow rate dQ through the section bounded by the sheet elements is given by

$$dQ = x \cdot d\phi \cdot 2h(x) \cdot V$$

where $2h(x)$ is spray thickness at x and V is the liquid velocity along the streamlines. Since the streamlines are straight, there is no mass flow across the streamlines and dQ through plane 1 is equal to that at plane 2. Therefore

$$x_1 \cdot d\phi \cdot 2h(x_1) \cdot V = x_2 \cdot d\phi \cdot 2h(x_2) \cdot V$$

$$x_1 \cdot 2h(x_1) = x_2 \cdot 2h(x_2) = \text{constant} = K \quad (1)$$

where K is the thickness parameter with a nonzero value. This relationship describes the geometry of the sheet where the thickness varies inversely with the distance x from the origin. However, this equation is indeterminate near the origin where $x = 0$. To avoid this difficulty, and since we are not

interested in the region very close to the origin, we assume mass transfer occurs only in the region $x > x_0$, where x_0 is chosen small enough to make the mass transfer from $x = 0$ up to $x = x_0$ negligible but large enough to allow for simplification in the derivations. One proper choice of x_0 is the distance from the origin to the orifice.

B. Diffusion equation

The binary diffusion equation for constant liquid density and diffusion coefficient is (Bird et al.)⁵:

$$\frac{\partial C}{\partial t} + \vec{v} \cdot \nabla C = D \nabla^2 C + R \quad (2)$$

where C is the local solute gas concentration in the liquid, D is the diffusion coefficient, and R is the chemical reaction term. Define a set of curvilinear coordinates in the sheet element as

$$u_1 = x^2 - y^2 \quad (3)$$

$$u_2 = 2xy \quad (4)$$

The streamlines along the direction u_2 are constant. At the boundary of the sheet $u_2 = 2xy = K$ and at the central line of the sheet element $u_2 = 0$. Following the assumptions made previously, differential vector operations lead to:

$$\frac{v}{\sqrt{g_{11}}} \frac{\partial C}{\partial u_2} = D g^{1/2} \frac{\partial}{\partial u_2} \left[\frac{g^{1/2}}{g_{22}} \frac{\partial C}{\partial u_2} \right] \quad (5)$$

where the metric tensors g_{ij} are defined as

$$g_{ij} \equiv \sum_{k=1}^3 \frac{\partial x^{k'}}{\partial x^i} \frac{\partial x^{k'}}{\partial x^j} .$$

For the present case,

$$g_{11} = g_{22} = \frac{1}{4u_1} \quad (6)$$

and the determinant of the matrix of metric tensors

$$g = \frac{1}{16u_1^2} . \quad (7)$$

Therefore, the convective mass transfer equation in u_1 and u_2 coordinates is:

$$\frac{\partial C}{\partial u_1^{3/2}} = \frac{4}{3} \frac{D}{v} \frac{\partial^2 C}{\partial u_2^2} . \quad (8)$$

Let

$$C^* = \frac{C - C_s}{C_o - C_s}$$

Initial Conditions:

$$u_1 = x_0^2 \quad C = C_o \quad (9)$$

Boundary Conditions:

$$u_2 = K \quad C = C_s \quad (10)$$

$$u_2 = 0 \quad \frac{\partial C}{\partial u_2} = 0 \quad (11)$$

Choosing this coordinate system converts the changing thickness problem into a uniform thickness problem. Let

$$\langle C^* \rangle = \frac{\langle C \rangle - C_s}{C_o - C_s} \quad (12)$$

where $\langle \quad \rangle$ stands for the quantity averaged over the u_2 -direction of the sheet element.

The solution of Eq. (8) with the above initial and boundary conditions is:

$$\langle C^* \rangle = \frac{8}{\pi^2} \sum_{n=1}^{\infty} \frac{1}{(2n-1)^2} \exp\left[-\frac{4\pi^2(2n-1)^2}{G_z}\right] \quad (13)$$

where G_z is the Graetz number defined as

$$G_z = \frac{12 \, v \, K^2}{D \, u_1^{3/2}} \quad (14)$$

Since $y \ll x$, $u_1 \sim x^2$ and G_z can be simplified to

$$G_z = \frac{12 \, v \, K^2}{D \, x^3} \quad (15)$$

Define

$$\alpha = 1 - \langle C^* \rangle = \frac{C_o - \langle C \rangle}{C_o - C_B} \quad (16)$$

= percentage of the theoretical maximum achievable mass transfer (stripping or absorption depending on the physical system).

The excellent agreement between the theoretical prediction by Eqs. (13), (15), and (16); and the experimental results of Tamir and Rachmilev³ over a wide range of sheet lengths for CO₂ absorbing into aqueous fluid sheet are shown in Fig. 3. It can be seen from the figure that about 90% of the theoretical maximum mass transfer ($\alpha \sim 0.9$) is achieved in the expanding sheet before breakup.

III. ANALYSIS LIMITATIONS

The two major assumptions made in this analysis are that the velocities along the streamlines are constant and that $y \ll x$ (or $u_2 \ll u_1$). From experimental observations, the assumption of $y \ll x$ is reasonable. For example, from the work of Tamir and Rachmillev³ y/x is of the order of 10^{-4} before sheet breakup. In order to check the validity of the assumption that V is constant, one may examine the equation of motion for this curvilinear coordinate system while holding the assumption of $y \ll x$.

In curvilinear coordinates the divergence of the stress tensor $\bar{\tau}$ is

$$\nabla \cdot \bar{\tau} = g_i^+ \tau_{ij}^{ji} \quad (17)$$

where g_i^+ are the base vectors, and τ_{ij}^{ji} is the covariant derivative with respect to u^j of the stress tensor τ^{ji} . The u_1 component of the equation of motion is

$$-\rho_L V_1 \frac{\partial V_1}{\partial u_1} = \frac{\partial p}{\partial u_1} + \frac{\partial \tau_{11}}{\partial u_1} + \frac{\partial \tau_{21}}{\partial u_2} - \frac{1}{2} u_1^{-1} \tau_{11} + \frac{1}{2} u_1^{-1} \tau_{22} \quad (18)$$

where the tensors appearing in this equation are written in terms of their physical components.

For incompressible liquids $\tau_{ij} = 2\mu d_{ij}$, where d_{ij} is the rate of strain tensor, given in terms of the velocity gradients as

$$d_{ij} = \frac{1}{2} \left(\frac{\partial v_i}{\partial x_j} + \frac{\partial v_j}{\partial x_i} \right) . \quad (19)$$

After substituting the expressions for τ_{ij} into Eq. (18), the u_1 component of the equation of motion becomes

$$-\rho_L v_1 \frac{\partial v_1}{\partial u_1} = \frac{\partial P}{\partial u_1} + 4\mu_1^{1/2} \frac{\partial^2 v_1}{\partial u_1^2} + 2\mu_1^{1/2} \frac{\partial^2 v_1}{\partial u_2^2} - \mu_1^{-3/2} v_1 . \quad (20)$$

Similarly, the u_2 component of the equation of motion becomes

$$0 = u_1^{1/2} \frac{\partial P}{\partial u_2} + 2\mu_1 \frac{\partial^2 v_1}{\partial u_1 \partial u_2} - 3\mu \frac{\partial v_1}{\partial u_2} . \quad (21)$$

From Eq. (21),

$$P = -2\mu_1^{1/2} \frac{\partial v_1}{\partial u_1} + 3\mu_1^{-1/2} v_1 + a(u_1) \quad (22)$$

where $a(u_1)$ is the integration constant. Substituting Eq. (22) into Eq. (20) gives

$$\begin{aligned} \rho_L u_1^{1/2} v_1 \frac{\partial v_1}{\partial u_1} + 2\mu_1 \frac{\partial^2 v_1}{\partial u_1^2} - \frac{5}{2} \mu u_1^{-1} v_1 + 2\mu \frac{\partial v_1}{\partial u_1} \\ + u_1^{1/2} \frac{\partial a(u_1)}{\partial u_1} + 2\mu_1 \frac{\partial^2 v_1}{\partial u_2^2} = 0 \end{aligned} \quad (23)$$

To scale this equation, we define the following terms. Let

$$\bar{v}_1 = \frac{v_1}{V_J}, \quad \bar{u}_1 = \frac{u_1}{X^2}, \quad \bar{u}_2 = \frac{u_2}{X^2}, \quad \bar{\delta} = \frac{\kappa}{X^2}, \quad \bar{a} = \frac{a}{\rho_L V_J^2}$$

where V_J is the injection velocity of the nozzle and X is the length of the sheet just before breakup. Equation (23) is transformed into the following dimensionless form:

$$\begin{aligned} Re \bar{u}_1^{1/2} \bar{v}_1 \frac{\partial \bar{v}_1}{\partial \bar{u}_1} + 2 \bar{u}_1 \frac{\partial^2 \bar{v}_1}{\partial \bar{u}_1^2} - \frac{5}{2} \bar{u}_1^{-1} \bar{v}_1 + 2 \frac{\partial \bar{v}_1}{\partial \bar{u}_1} \\ + Re \bar{u}_1^{1/2} \frac{\partial \bar{a}(\bar{u}_1)}{\partial \bar{u}_1} + 2\bar{u}_1 \frac{\partial^2 \bar{v}_1}{\partial \bar{u}_2^2} = 0 \end{aligned} \quad (24)$$

where the Reynolds number Re is defined as $Re \equiv (\rho_L V_J X)/\mu$.

From the order of magnitude method (e.g, see Ref.6), one may conclude that if

$$Re \ll \frac{1}{\delta^2}, \quad (25)$$

Eq. (24) will become

$$2\bar{u}_1 \frac{\partial^2 \bar{v}_1}{\partial \bar{u}_2^2} = 0, \quad (26)$$

which with the initial condition

$$\text{at } \bar{u}_1 \sim 0, \quad \bar{v}_1 \sim 1$$

requires that $\bar{v}_1 \sim 1$. Therefore $v_1 \sim v_j$, which is a constant under the conditions of operation. The condition for constant velocity [Eq. (25)] can be demonstrated by the experimental data of Tamir and Rachmilev.³ In their work Re is about 10^5 while $1/\delta^2$ is of the order of 10^7 .

IV. APPLICATION TO GAS STRIPPING FROM NONAQUEOUS

SOLUTION SHEET ISSUING FROM FAN SPRAY NOZZLE

Dombrowski et al.⁴ studied the effect of viscosity on the thickness parameter K of fan spray nozzles and found that at 15 psig injection pressure K increased from 14.3 to 16.5×10^{-4} cm² as the viscosity increased from 1 to 50 cp. At 50 psig K increased from 14.0 to 15.5×10^{-4} cm² over the same range of viscosity. Within the limits of experimental error there was no effect of the surface tension on K for injection pressure higher than 15 psig.

Dombrowski and Fraser⁷ chose water, 37% glycerine/water and 47.5% glycerine/water all at 20°C as the test liquids for their investigation of fan spray nozzles. The viscosities of these three liquids are 1 cp, 3.3 cp, and 5.3 cp, respectively, while the surface tension and density of these three

liquids are about the same ($\sigma = 73, 72, \text{ and } 70 \text{ dyne/cm, respectively,}$ and $\rho = 1.0, 1.1, \text{ and } 1.12 \text{ g/ml, respectively}$). The experiments, which employed the same injection pressures, resulted in sheet lengths for 47.5% glycerine/water about twice as long as those for water. They also found that X was longer for liquids of higher surface tension for the same viscosity and density. Their experiments showed that an increase of density from 0.89 to 2.28 g/ml resulted in only a small increase in the length of the sheet. From these results one may conclude that the radial distance before breakup of the sheet, X , is longer for liquids of higher viscosity, surface tension, and density as long as a sheet is formed. For liquids of very high viscosity and surface tension, the injection pressure must be increased to form a sheet.

The maximum value of y/x at the point of breakup of the sheet is

$$\frac{y}{x} = \frac{h(X)}{X} = \frac{k}{2X^2} . \quad (29)$$

From the above discussion Eq. (29) implies that for constant K , in general, the higher the viscosity, surface tension, and density of one fluid system, the greater the validity of the assumption of $y \ll x$.

Dombrowski et al.⁴ also showed that the injection velocity at the orifice is

$$V_J = C_Q \sqrt{2P/\rho_L} \text{ or } V_J \rho_L = C_Q \sqrt{2P\rho_L} \quad (30)$$

where C_Q is the orifice discharge coefficient and P is the injection pressure. For the same injection pressure and orifice discharge coefficient the product $V_J \rho_L$ varies with the square root of the liquid density. With

normal liquids the variation of density lies in the small range from 0.7 to 2.0 g/ml. It would be expected, therefore, that the effect of density on the V_J as well as on $V_J \rho_L$ would not be large.

The condition for constant liquid velocity Eq. (25) can be written as:

$$\text{Re} \ll \frac{1}{\delta^2} = \frac{X^4}{K^2}$$

This implies that

$$K^2 V_J \rho_L X^{-3} \mu^{-1} \ll 1. \quad (31)$$

In general, the values of K and $V_J \rho_L$ are about the same for most liquids under the same injection pressure. Therefore, the larger the X and μ , the smaller the value of $K^2 V_J \rho_L X^{-3} \mu^{-1}$ and the greater the validity of the assumption of constant velocity throughout the sheet.

Another important assumption made in this model is that the shape of the sheet can be described as a sector. In fact, the sheet shape is influenced by the contraction of the edges as a result of surface tension. In the experimental results of Tamir and Rachmilev, the open angle of the sector is about 120°. For this geometry the error which arises from the deviation of the actual sheet shape from the ideal sector shape is small. Since the contraction of the sheet edges is less pronounced at higher injection pressures, one can always increase the injection pressure of the nozzle (therefore increasing the open angle of the spray sheet) for liquids with high surface tension such that the shape of the sheet will be close to an ideal sector. Furthermore, by proper choice of nozzle type and operating conditions, the condition for constant velocity, $K^2 V_J \rho_L X^{-3} \mu^{-1} \ll 1$, can still

be fulfilled. For example, since different types of nozzles have different values of K (the range of the difference can be in the order of ten), one can choose a nozzle with a small enough value of K to compensate for large values of V_J .

For the specific case of recovering the bred tritium in a fusion reactor, one faces the problem of stripping the tritium fluoride from the blanket material at concentration levels in the parts per million range. A potential blanket material is the eutectic flibe (a mixture of F, Li, and Be) comprising a 47 to 53 molar mixture of lithium fluoride and beryllium fluoride. The viscosity of the eutectic flibe at 600°C is 40 cp (see Ref. 8) and its surface tension at 480°C is 198 dynes/cm (see Ref. 9). From the above analysis, the conditions $\mu_f = 40 \mu_w$, $\sigma_f = 3 \sigma_w$, and $\rho_f = 2\rho_w$ imply that $(y/x)_f$ is less than $(y/x)_w$, where the subscript f stands for the properties of flibe and the subscript w stands for the properties of water. The condition $\rho_f = 2\rho_w$ also implies $(V_J \rho_L)_f \sim \sqrt{2} (V_J \rho_L)_w$ for the same injection pressure. Although $(K^2)_f = 1.22 (K^2)_w$ at $p = 50$ psig and $(K^2)_f = 1.33 (K^2)_w$ at $p = 15$ psig, the product $K^2 V_J \rho_L \mu^{-1}$ for flibe is still much smaller than that of water even if $X_f = X_w$. Therefore the total product $K^2 V_J \rho_L X^{-3} \mu^{-1}$ for flibe is smaller than that for water. In fact, even if the nozzle injection pressure for the flibe system is 450 times higher than that for the water system, the total product $K^2 V_J \rho_L X^{-3} \mu^{-1}$ is still smaller than that for the water system. Although the surface tension of flibe is about three times higher than that of water, this wide range of allowance for adjustment of the injection pressure will make the flibe sheet geometry close to the ideal sector while preserving the other two conditions $y \ll x$ and $V = \text{constant}$.

A question remains as to whether the high predicted mass transfer efficiency for water systems can actually be achieved in other systems with

practical values for the system parameters. From Fig. 3 we notice that the higher the value of $4DX^3/3 V K^2$ the higher the value of the mass transfer efficiency α . For a liquid sheet with high surface tension, the increase of the injection velocity (V) to make the sheet geometry close to an ideal sector will have a tendency to decrease the value of $4DX^3/3 V K^2$. However, the length of the liquid sheet just before breakup (X) will be longer for the liquid with higher viscosity, therefore the high viscosity will have a tendency to increase the value of $4DX^3/3 V K^2$. The balance between the viscosity effect (longer X for liquid with high viscosity) and the surface tension effect (higher injection velocity is required for liquids with higher surface tension to achieve a shape closer to an ideal sector) will determine the resulting value of $4DX^3/3 V K^2$. We wish to know whether the mass transfer efficiency for flibe sheet is higher than that for water sheet issuing from the same type of nozzle when both sheets are sufficiently close to an ideal sector geometry, given that the relations between the fluid properties are:

$$\mu_f = 40 \mu_w, \sigma_f = 2.9 \sigma_w \text{ and } \rho_f = 1.53 \rho_w.$$

To investigate this case, consider the system 55.3% glycerine/water (with viscosity 8.15 cp, surface tension 69 dynes/cm, and density 1.14 g/c.c) and ethyl acetate (with viscosity 0.45 cp, surface tension 24 dynes/cm, and density 0.9 g/c.c), which has been studied experimentally by Dombrowski and Fraser (1954) for various operating conditions. Here we notice that $\mu_{GW} = 17.7 \mu_{EA}$, $\sigma_{GW} = 2.88 \sigma_{EA}$, and $\rho_{GW} = 1.27 \rho_{EA}$, where the subscripts GW and EA stand for the properties of 55.3% glycerine/water and ethyl acetate, respectively. Since $\sigma_{GW} = 2.88 \sigma_{EA}$ (notice that this is the same ratio as σ_f/σ_w), a higher injection pressure is needed for the GW system than that for

the EA system to make the shape of the GW sheet close to an ideal sector. Their work showed that the maximum required injection pressure for the GW system is three times higher than that required for the EA system, corresponding to $V_{GW} = \sqrt{3} V_{EA}$, to make the shapes of the two sheets close to ideal sectors. Under this condition, because $\mu_{GW} = 17.7 \mu_{EA}$, the length of the sheet before breakup for the GW system (X_{GW}) is about 1.8 times longer than that for the EA system. If the diffusion coefficients of the solute in the two solvents are about the same, the parameter $4DX^3/3 V K^2$ for the GW system will be about 3.3 times larger than that for the EA system. This result implies that the viscosity effect overcomes the surface tension effect for the GW system as compared with the EA system. From Fig. 3 we find that the larger the value of $4DX^3/3 V K^2$ the higher the value of α . Therefore the stripping efficiency for solute in the GW system before breakup is higher than that for the EA system. This result illustrates that for a liquid sheet with viscosity 17 times higher than the reference liquid system and surface tension 2.88 times higher than the reference liquid system, the mass transfer efficiency (α) would be at least as high as that for the reference liquid sheet just before breakup. We can extend this conclusion to the case of comparing flibe and water systems. Since $\mu_{flibe} = 40 \mu_{water}$ (which is even higher than the ratio of μ_{GW}/μ_{EA} , therefore making X_f/X_w even higher than X_{GW}/X_{EA}), $\sigma_{flibe} = 2.8 \sigma_{water}$ (which is similar to the ratio of σ_{GW}/σ_{EA}) and $\rho_{flibe} = 1.98 \rho_{water}$, based on the previous result of comparing GW and EA systems, for $X_f > 1.8 X_w$ and $V_{C,f} = \sqrt{3} V_{J,w}$, the value of the parameter $4DX^3/3 V K^2$ for a flibe system will be higher than that for a water system. One can therefore expect that the mass transfer efficiency α for a flibe sheet before breakup is at least as high as that for a water sheet.

V. CONCLUSIONS

In general, gas stripping from the fluid sheet issuing from a fan spray nozzle would be expected to follow Eq. (13) for any fluid with viscosity higher than or equal to that of the water as long as a fan-shaped sheet is formed under the operating conditions of the nozzle. With a careful adjustment of the operating conditions of the nozzle and choice of the type of the nozzle, the mass transfer efficiency, α , would be at least 0.9. In particular, the fluid properties of the flibe increase the validity of the two major assumptions, namely, $y \ll x$ and $V = \text{constant}$ in this analysis while keeping the shape of the spray sheet close to an ideal sector. Hence it may be expected that the mass transfer efficiency relation, Eq. (13), would be at least as accurately applicable for a flibe system for fan spray stripping as for an aqueous solution system. The stripping of sparingly soluble gas from molten fluoride salt (flibe) sheets issuing from fan spray nozzles is expected to be at least as expeditious as that observed in aqueous systems. Thus tritium fluoroide should be readily separable from molten fluoride salt using fan sprays.

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NOMENCLATURE

C	=	local solute gas concentration in the liquid.
$\langle C \rangle$	=	average local solute gas concentration in the liquid.
C_0	=	solute gas concentration in the entrance.
C_s	=	solute gas concentration at the interface.
C^*	=	dimensionless concentration.
$\langle C^* \rangle$	=	average dimensionless concentration.
d_{ij}	=	rate of strain tensor.
D	=	diffusion coefficient of solute gas in the liquid.
$\vec{g}_i$	=	basic vectors.
g_{ij}	=	metric tensors.
G_z	=	Graetz number.
$h(x)$	=	half sheet thickness at distance x .
K	=	thickness parameter, Eq. (1).
P	=	pressure.
Q	=	volumetric flow rate.
R	=	chemical reaction term in the general diffusion equation.
Re	=	Reynolds number.
u_1	=	curvilinear coordinate in the sheet element along the direction of streamline.
u_2	=	curvilinear coordinate in the sheet.
v	=	liquid velocity along the streamline.
v_1	=	liquid velocity in the u_1 coordinate.
v_j	=	injection velocity at the orifice.
x	=	radial distance from the sheet origin along the central line of the sheet element.

- x_0 = inlet length for initial condition.
 x = radial distance along the central line of the sheet element before breakup.
 y = y coordinate in the sheet element.

Greek symbols

- ρ = density of the liquid.
 σ = surface tension.
 μ = viscosity of the liquid.
 α = approach to equilibrium or efficiency of stripping Eq. (16).
 ϕ = angle subtended by fan spray sheet.
 δ = defined as K/x^2 .

Subscripts

- f = flibe
 L = liquid
 w = water

FIGURE CAPTIONS

- FIG. 1a. Geometry of the whole sheet.
- FIG. 1b. Geometry of the sheet element. Longitudinal section of sheet normal to line source.
- FIG. 2. Mass balance representation.
- FIG. 3. Theoretical prediction and experimental results for the mass transfer of CO_2 in water sheet. (Solid line represents the theoretical prediction; the experimental data are from Tamir and Bachmiev, 1972).

83E0058

Fig. 1

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$$\text{AREA 1} = x_1 d\phi \cdot 2h(x_1)$$

$$\text{AREA 2} = x_2 d\phi \cdot 2h(x_2)$$

Fig. 2

Fig. 3

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