Liquid Extraction in an Agitated Vessel

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This investigation was undertaken to show how the efficiency of solute transfer between two immiscible liquids is influenced by the type, size, and submergence and rotational speed of the impeller and the degree of baffling, residence time, and phase ratio. The system water–kerosene–*n*-butylamine solute was agitated in a continuous-flow 14¾-in. -diam. vessel using propellers, spiral turbines, and flat-blade turbines from 4 to 10 in. in diameter. The feed streams were introduced at the wall at the bottom of the vessel and left at the top of the vessel.

On the basis of the power required for a given level of stage efficiency, the best agitator design is any impeller that has a diameter about 40% of the vessel diameter and is centered in the unbaffled vessel. Without baffles the impeller type and depth of submergence have little effect on performance. The power increase required by the addition of baffles is small at the highest stage efficiencies, especially with the larger radial-flow impellers, but may be severalfold at efficiency levels of 70 to 90%. This presumably results from the lowered mass transfer driving force caused by the increase in end-to-end mixing due to wall baffles. Whether baffles have such a large adverse effect if the feed is introduced into the impeller rather than at the vessel wall is not fully established, but there is some evidence that baffling is also undesirable in this case. For baffled operation, impeller type and submergence do affect performance.

A correlation of stage efficiency is presented in terms of Reynolds number and power number. In addition, energy input per volume of total flow is correlated in terms of residence time and stage efficiency for one size of baffled propeller.

Spot samples taken from the vessel showed large, random-concentration fluctuations out to 20 residence times and perhaps indefinitely. Reproducible results were obtained by taking time-averaged samples.

Transfer of material between two immiscible liquid phases in an agitated vessel is a common operation in the chemical and petroleum industries. The power consumption of this operation has been well established by Rushton (*7*) and others as a function of impeller type, size, and speed; however, the effect of these variables on mass transfer has been largely ignored. A review of the literature through 1954 shows that the numerous agitation studies do not include a single investigation of mass transfer between liquid phases wherein the scale of study was large enough to ensure some confidence in the results. Consequently, this study was undertaken to determine how mass transfer efficiency is affected by two classes of variables: first, the impeller and its operation and, second, the liquid-feed rate and phase ratio. The variables pertaining to the impeller are the size, type, speed of rotation, its environment as determined by the location within the vessel, and the degree of baffling.

APPARATUS

The impellers tested may be seen in Figure 1 and are described in detail in Table 1. They included 4-, 6-, 8-, and 10-in. marine propellers, 4-, 6-, and 8½-in. spiral turbines, and 6-, 8-, and 10-in. flat-blade turbines, which were operated in a vessel of 14¾-in. diam. Operation of these impellers was studied with four wall baffles 1¼ in. wide and without baffles. In addition, the spiral turbines were operated within deflector rings to form Turbo-Mixers, and one size (4 in.) was studied with and without a shroud. The use of a confining cover plate was studied with one size of propeller, both baffled and unbaffled. The other unbaffled impellers were operated with the cover plate installed to prevent vortexing, which otherwise accompanies unbaffled operation The effects of impeller submergence (the fraction of the total liquid depth to which the impeller is submerged) and off-center mounting were also investigated. Liquid depth could not be varied conveniently, and so all the runs were made with the liquid depth slightly greater than the vessel diameter.

The feed streams were continuously extracted at total residence times of 1 to 5 min. Two methods of introducing the feed streams were considered. The streams could be injected directly into the impeller or they could be added at the vessel wall at some distance from the impeller. Wall addition was selected for most of the work in this study because it is a closer approach to the type of flow prevailing in multistage extractors such as the rotating-disk (RDC) (*5*) or the Scheibel contactor (*8*). However, since impeller feed addition may be more widely used in single-stage agitated vessels, a few measurements were made comparing the two types of feed addition.

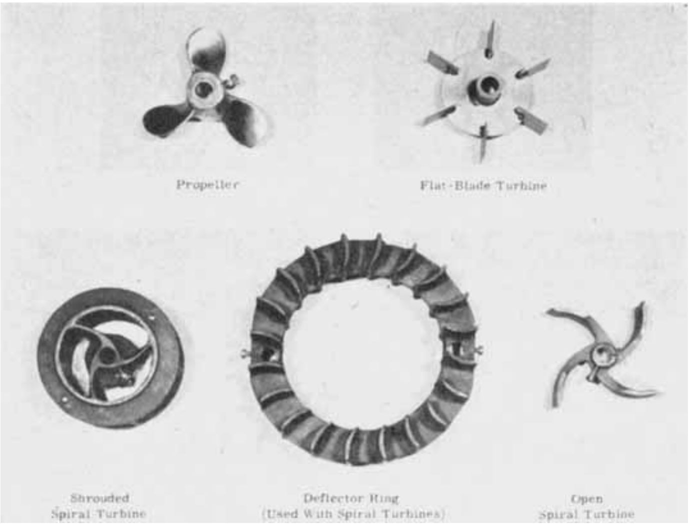

Fig. 1. Typical impellers.

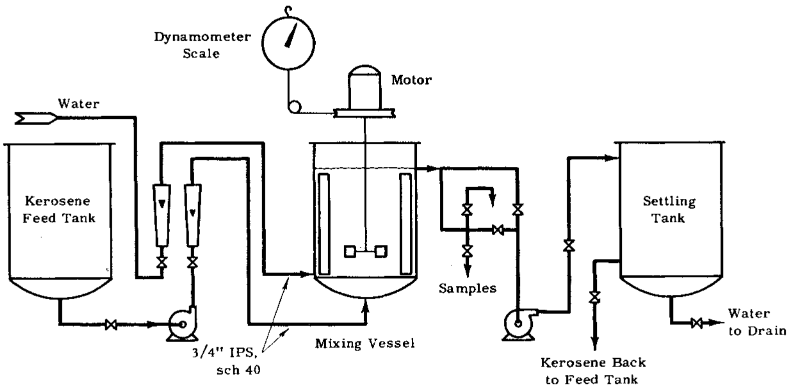

Fig. 2. Flow diagram.

EXPERIMENTAL TECHNIQUE

A diagram of the equipment used in this investigation is shown in Figure 2. The kerosene feed, containing 0.25 wt. % *n*-butylamine, was pumped from the feed-preparation tank through a rotameter to the mixing vessel, where it encountered a metered stream of city water, both streams entering the bottom of the vessel. The two-phase mixture flowed out of the vessel just below the liquid level to a centrifugal pump the discharge of which was throttled to maintain a constant level in the vessel. The mixture was then settled into clear phases, the water discarded, and the kerosene reused.

The mixing vessel was 14¾ in. I.D. and was operated with the liquid about 18¾ in. deep (including a 2-in. dished bottom), giving an operating volume of 13.3 gal. Four wall baffles, each 1¼-in. wide, were attached to a cage which could be removed from the vessel for unbaffled operation. The cover plate was flat with a sealing gasket around the edge and a chimney through which the impeller shaft entered. The plate was positioned at the normal air-liquid interface, thus maintaining the same operating volume. In order to ensure the complete absence of an air vortex within the vessel, the liquid was maintained in the chimney by control of the rate of withdrawal. Mass transfer samples were obtained by conducting a side stream of the two-phase mixture leaving the mixing vessel to a pipe cross of small volume which was packed with stainless steel matting of high void volume. Here a portion of the mixture was coalesced into two clear streams and the water phase withdrawn from the bottom of the cross and the kerosene phase from the top. By sampling these streams for 4 min., a time-averaged sample was obtained which was quite reproducible and in reasonable material balance. For the 187 runs in this study, the average recovery of solute was 96.7% of that present in the feed and the average and standard deviations from perfect material balance were 4.4 and 5.5%, respectively.

Previously samples had been obtained by scooping a mixture from the vessel through its open top or collecting a sample from the exit line and quickly separating the two phases. These runs, all at 1-min. residence time, indicated that even though steady state was reached in about 6 min., repeat runs sampled after 11 min. showed poor reproducibility (a large percentage variation in $1 - \epsilon$). Many false leads were explored before it was finally postulated that at steady state only the average concentration level at a given point in the vessel is constant and that the instantaneous values fluctuate measurably about the average. To check this hypothesis, runs of 1-min. nominal residence time were continued for 20 min., during which many 400- to 600-cc. samples were scooped from the vessel. It can be seen from Figure 3 that the concentrations varied significantly out to 20 residence times and the stage efficiency varied randomly from 0.84 to 0.93 stage. The continuous sampler described above was then devised, and all the results reported here are based on time-averaged samples. The majority of the runs were made for 10 min., which was a sufficient time to reach a steady state. The continuous sampler was operated throughout the run but samples were collected during only the last 4 min.

An analysis of the effects of small errors on the calculated stage efficiency showed that it was necessary to measure the exit concentrations and the vessel temperature accurately. Most of the runs, therefore, were made with water supplied at 68°F. so as to minimize temperature effects.

The power required to operate the various impellers was calculated from the motor speed and the torque required to restrain the motor on its ball-bearing torque table. The motor speed was controlled by a Thymotrol drive and indicated by a calibrated electric tachometer; the force to prevent rotation was indicated on a sensitive calibrated dynamometer scale. Critical dimensions of each of the impellers are shown in Table 1. A comparison of the variously sized impellers shows that only the flat-blade turbines are geometrically similar throughout their size range. Among the other impellers there are differences as to the number of blades or size ratios. Nevertheless, it is believed that these differences do not affect the major conclusions of this work. The propellers were operated so that liquid was discharged downward, and the spiral turbines so that the convex side of the blade advanced. Mixing Equipment Company supplied the propellers and the design drawings for the flat-blade turbines. The Turbo-Mixer Division of General American Transportation Corporation supplied the spiral turbines and deflector rings.

SYSTEM PROPERTIES

The test system water–kerosene–*n*-butylamine was chosen because it was more difficult to disperse and extract than the system water–methyl isobutyl ketone–acetic acid, which had also been used to test several types of extractors. The system settled very quickly after agitation and no emulsion problems were encountered. At very low agitation intensities kerosene was the phase dispersed. When the agitation was increased to give efficiencies greater than about 0.75 stage, the system was composed of a fine dispersion of water and kerosene globules. Under these conditions it was impossible to tell by visual inspection which, if either, phase was truly continuous. This could presumably be resolved by conductivity measurements, but these were not done. Holdup measurements were made in many of the tests without a cover plate (Table 3).† No holdup measurements sene as well as interfacial tensions for the liquid system are found in Table 2.

The system n-butylamine–kerosene–tap water, while satisfactory, is not ideal for mass transfer measurements because of the possibility of minor errors in material balance and stage count. This is caused by where b and m are constants and x and y are the weight fractions of amine in the water and kerosene phases, respectively. The slope m is a function of temperature. The intercept b, due in part to dissolved carbonate, changed slowly with time or usage and required an occasional redeter-

TABLE 1. IMPELLER CHARACTERISTICS

Propellers

Diam., in.	True tip radius, in.	Vertical projected blade height, in.	Calculated pitch/ diam. ratio	Avg. power function, C Baffled	Unbaffled	Off center	Horizontal projected blade area, sq. in.
4	0.58	0.54	1.13	0.31	0.25	—	4.34
6	1.08	1.07	1.33	0.42	0.28	0.37	11.0
8	1.68	2.10	1.45	0.62	0.33	—	21.7
10	1.82	1.07	0.90	0.25	0.18	—	33.7

Spiral turbines

Nominal size, in.	Actual O.D., in.	Blades	Blade height, in.	Avg. power function, C 4 Wall baffles	Deflector ring	Unbaffled	Deflector ring I.D., in.	O.D., in.	Blade height, in.	Blades
4 shrouded	3.9	3 full length +3 at periph.	Mouth 0.8 Max. 1.5	—	1.43	—	4.45	6.5	1.8	20
4 open	3.92	4	0.75	—	1.75	0.95	4.45	6.5	1.8	20
6 open	5.75	6	1.0	1.5	1.5	0.6	6.95	9.7	2.1	24
8-1/2 open	8.6	6	1.72	1.86	—	0.5				

Flat-blade turbines

Diam., in.	Blades	Blade width (radially), in.	Blade height, in.	Disk diam., in.	Avg. power function, C Baffled	Unbaffled
6	6	1.50	1.20	4.00	5.1	1.35
8	6	2.00	1.60	5.375	5.1	0.95
10	6	2.50	2.00	6.75	4.9	

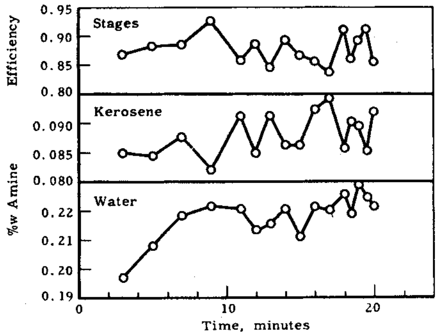

Fig. 3. Test of steady state; 6-in. propeller, one-half submergence, four wall baffles, θ = 1.08 min., Q_K/Q_W = 1.57.

were made when the cover plate was used.

The kerosene used in this investigation was Shell Solvent 140, which has a narrow boiling range and a low aromatic content. The solute, n-butylamine, was purchased from the Sharples Chemical Company. It has a minimum butylamine content of 96.5%, a minimum initial boiling point of 73.0°C., and a maximum final boiling point of 85°C. Additional properties of the kero-

the presence of small amounts of soluble bicarbonates in the tap water. When amine is added to the system, a loss of alkaline constituents (due to partial precipitation of the bicarbonates) occurs which is not compensated for by a blank water titration. Since the water used contained about 12 p.p.m. $Mg(HCO_3)_2$ and 94 p.p.m. $Ca(HCO_3)_2$, the apparent amine balance should be about 97% and indeed the average balance for 187 runs was 96.7%. The effect on stage count is caused by the unprecipitated carbonate ion reacting and rendering a small portion of the amine nondistributable. This results in a slight overestimation of the distributable amine content of the water-layer samples, which displaces the equilibrium line to give a positive intercept on the water-layer axis. While this is not desirable, its effect on stage count is negligible (less than 0.005 stage at the 0.9-stage level) as the water-extract end of the operating line is displaced the same amount as the equilibrium line. There is also a possible danger with this system of absorption of carbon dioxide from the air. However, calculations showed that there was essentially no chance for this to take place sufficiently to affect the material balance or stage count.

At the low amine concentrations used, the distribution ratio for the system is represented by the equation

$$x^* = my + b$$

TABLE 2. PROPERTIES OF SHELL SOLVENT 140

Typical properties

Gravity, °A.P.I.	45.1
Flash, tag OC	150°F.
Aromatics (Stoddard)	2.0%
Kauri butanol value	34.5
Aniline point	147°F.
A.S.T.M. distillation	
IBP	378°F.
FBP	404°F.

Measured physical properties

Temp., °F.	Density, g./cc.	Viscosity, cp.
60	0.8048	1.488
70	0.8009	1.352
80	0.7970	1.232

Interfacial tension (ring method)

Kerosene-Water-amine

68°F.

% wt. Amine	Dynes/cm.
0	50
0.25	39
0.5	31

mination. A typical value of the equilibrium line at 68°F. is

$$x^* = 2.78y + 0.000150 \qquad (1)$$

†Tabular material has been deposited as document 5052 with the American Documentation Institute, Photoduplication Service, Library of Congress, Washington 25, D. C., and may be obtained for $1.25 for photoprints or 35-mm. microfilm.

CALCULATION OF EFFICIENCY

The conversion of stream-concentration data to some index of extractor performance requires the use of an efficiency expression. Since the solvents are immiscible in the region of very low amine concentrations and the equilibrium line is straight, the problem is simplified; nevertheless, a choice must be made among at least the following three alternatives.

1. The system is considered countercurrent and the number of equilibrium stages calculated from the following equation (*4*), which gives the countercurrent stage efficiency, ϵ_{cc}

$$\epsilon_{cc} = \frac{\log \dfrac{my_2 - x_2 + b}{my_1 - x_1 + b}}{\log \dfrac{m(y_2 - y_1)}{x_2 - x_1}} \qquad (2)$$

where m is the slope of the equilibrium line and x_1 and x_2 are the amine concentrations in the inlet and exit water and y_2 and y_1 are the amine concentrations in the inlet and exit kerosene, respectively.

2. The system is considered cocurrent and the efficiency defined as the actual change in concentration of one stream divided by the change which would have occurred if the system had reached equilibrium at the intersection of the equilibrium line with the extension of the operating line. This results in the following equation

$$\epsilon_p = \frac{x_2 - x_1 + m(y_2 - y_1)}{my_2 + b - x_1} \qquad (3)$$

where ϵ_p is the cocurrent stage efficiency.

3. The system is considered cocurrent and the efficiency defined as the actual change in the concentration of one stream divided by the change which would have occurred if that stream had come to equilibrium with the other stream actually leaving the mixer. This results in the following Murphree-type equations for each phase

$$(\epsilon_M)_W = \frac{x_2 - x_1}{my_1 + b - x_1} \qquad (4)$$

$$(\epsilon_M)_K = \frac{m(y_2 - y_1)}{my_2 + b - x_2} \qquad (5)$$

For the case where $x_1 = 0$ (inlet water contains no amine) it can be shown that the various efficiencies are related as follows:

$$(\epsilon_M)_W = \frac{x_2 \lambda^{\epsilon_{cc}}}{my_2 - x_2 + b} \qquad (6)$$

and

$$\epsilon_p = \frac{1 + \lambda}{1 + \left(\dfrac{my_1 + b}{x_2}\right)\lambda^{\epsilon_{cc}}} \qquad (7)$$

or

$$\epsilon_p = \frac{1 + \lambda}{1 + \lambda^{\epsilon_{cc}}/(\epsilon_M)_W} \qquad (8)$$

where

$$\lambda = \frac{m(y_2 - y_1)}{(x_2 - x_1)} = \frac{mW_W}{W_K}$$

The choice of equation is somewhat arbitrary, as the mixer is neither purely countercurrent nor cocurrent; it approaches cocurrent flow in the unbaffled case but it should be considered as a well-mixed stage when operated with baffles. The choice was finally made in favor of the countercurrent case [Equation (2)], partly because the experimental mixer was usually thought of as part of a countercurrent multistage extraction process and partly because of undesirable features in the cocurrent cases. In case 3 the undesirable feature is the significant efficiency difference, depending on which phase was chosen; in case 2 it is the poor resolution of runs near one theoretical stage. Runs which calculate out to efficiencies of 0.80 to 1.0 stage by use of Equation (2) generally lie between 0.9 and 1.0 stage when Equation (3) is used. From a practical standpoint, the choice makes little difference since the major conclusions of this work result from the rather large differences in power required to reach a certain efficiency level for a given impeller. Changing the numerical values of efficiency will not affect the conclusions.

EXPERIMENTAL RESULTS

A general tabulation of experimental results showing agitator speed and power, holdup, stage count, and solute material balance is contained in Table 3.* Of the various methods of interpreting the data, the use of plots of stage efficiency vs. the impeller power was most satisfactory. These allow a direct comparison to be made between the various impellers and methods of operation on the basis of the power required to produce a given level of efficiency. They also show the increases in power occasioned by raising the level of efficiency. The power basis was chosen, as it determines part of the operating and capital equipment cost for a given operation. The power plotted in these figures is the impeller power, which is essentially the total extraction power as the kinetic energy of the entering liquid streams is negligible in this case. The mass transfer is that due to agitation by the impeller plus that due to liquid flow through the vessel, exit line, and sampler. The extraction due to factors other than impeller agitation is 0.18 of a theoretical stage at a residence time of 1.08 min.

For each new condition of operation (such as a new type and size of impeller), impeller speed was varied to define the horsepower-efficiency curve in the region of 0.7 to 0.95 stage. This generally required at least four experimental points, and usually more. Smooth curves were drawn through the experimental points. The conclusions discussed below are derived from an examination of such summary curves.

SIZE AND BAFFLING

Figures 4, 5, 6, and 7 summarize the power-efficiency relationships at 1.08 min. residence time and impeller submergence of one-half liquid depth. On the basis of the power required for a given level of mass transfer efficiency, both size and baffling have large effects. The data show that in all cases operation without baffles required less power to reach a given efficiency level than operation with baffles. The difference in power between baffled and unbaffled operation is greatest in the case of the marine propellers, Figures 4 and 5, and least in the flat-blade turbines, Figure 7. In each of the impeller types the power difference is largest with the 4- and 6-in.-diam. impellers and smallest in the 8- and 10-in. impellers. The difference in power is significant in the case of the small impellers. For example, at efficiency levels of 0.7 to 0.9 stage, the 4- and 6-in. marine propellers require only one-third to one-quarter as much power without baffles as with baffles. In the case of the 8- and 10-in. propellers the ratio is about one-half and in the 8-in. spiral turbine and flat-blade turbine it is essentially unity. Figure 8 shows the optimum impeller size for each impeller both with and without baffles. In the unbaffled state the coincidence of the curves for the 6-in.-propeller, flat-blade and spiral turbines shows the best design to be any impeller of about 0.4 tank diameter. In the baffled state the optimum sizes are larger than in the unbaffled case, and the impellers giving little axial flow (flat-blade and spiral turbines) are shown to be more effective than the marine propeller.

The good results obtained without wall baffles are surprising. Previously baffles were considered essential for most mixing operations (*1, 2*) because they improve top-to-bottom mixing and, by maintaining similitude, may aid in scaling up designs from small experiments (*6*). The superiority of unbaffled operation in this case is believed due to a better utilization of the mass transfer driving forces. When baffles are present, axial flow is increased, especially with the propellers, and the entering feed streams are diluted with material nearer equilibrium, thus reducing the average mass transfer driving force. In the absence of baffles, end-to-end mixing is diminished and a swirling plug-type flow results which better utilizes the driving force and permits mass transfer at lower

*See footnote on page 531.

agitation levels. Thus, if this postulate is correct, mass transfer in this case is a function both of the amount of agitation, which determines the drop size and turbulence inside and outside the drops, and of the degree to which concentration driving forces are utilized.

The adverse effect of wall baffles might not have been great if the feed streams had been introduced directly into the impeller rather than at the wall, as was usually done in this work. The high turbulence near the impeller might have accomplished much mass transfer before the feed streams were diluted by end-to-end mixing, particularly with a system having high mass transfer coefficients. The effect of changing feed-inlet location was checked in an early series of experiments with the baffled 6-in. marine propeller at one-half submergence. The feed streams in this case were injected coaxially upward at a velocity of about 10 ft./sec. at a point 2¾ in. below the impeller. These data, unreported since they are based on spot samples and a previous batch of kerosene, showed that for efficiencies of 0.80 to 0.95 stage in the baffled vessel, introducing the feed into the impeller lowered the total power requirement about 50% relative to the wall-addition case at the same efficiency. However, the later work reported here for the same propeller shows that when the baffles are removed the power required to reach a given efficiency is reduced to about 25% of that with baffles. Thus when compared to a common standard, unbaffled operation with wall feed addition is superior by about 2 to 1 to baffled operation with impeller feed addition. Since the power requirement without baffles might be reduced even further if the feed were introduced into the impeller, the indication becomes strengthened that baffles would be undesirable even with impeller feed addition. Actually the effects of impeller feed addition should be viewed as a separate, unresolved problem. It is possible that the best impeller size in the baffled case with impeller feed might be quite different from that indicated here with wall feed. However, there seems no reason to believe that at constant power the end-to-end mixing promoted by baffles would result in more efficient mass transfer than that obtained with cocurrent or countercurrent flowing phases.

CORRELATION OF EFFICIENCY

It will be noted that the preceding curves of power vs. efficiency are specific for a given impeller and cannot be used to predict the performance of an unknown impeller. Therefore it seemed desirable to attempt a general representation of the stage efficiency. The impeller Reynolds number was tried first. This is defined as

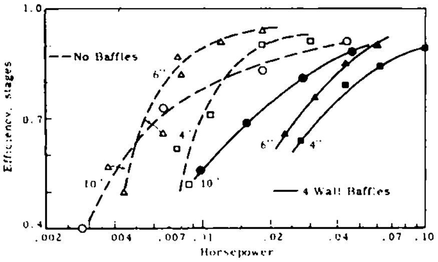

Fig. 4. Marine propellers; one-half submergence, θ = 1.08 min., Q_K/Q_W = 1.57.

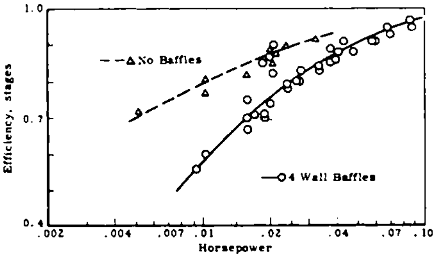

Fig. 5. Eight-inch marine propeller; one-half submergence, θ = 1.08 min., Q_K/Q_W = 1.57.

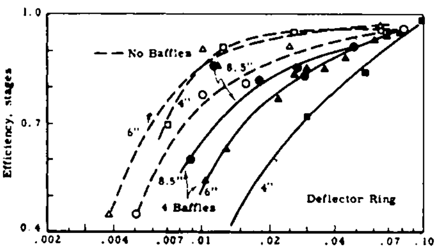

Fig. 6. Open spiral turbines; one-half submergence, θ = 1.08 min., Q_K/Q_W = 1.57.

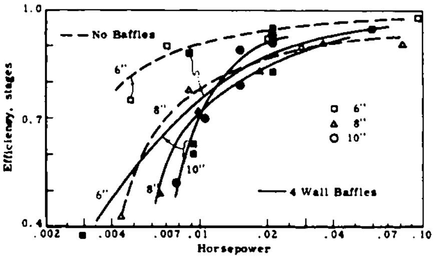

Fig. 7. Flat-blade turbines; one-half submergence, θ = 1.08 min., Q_K/Q_W = 1.57.

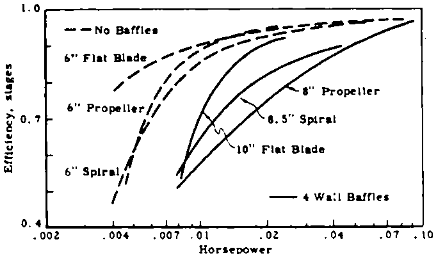

Fig. 8. Optimum impeller size; one-half submergence, θ = 1.08 min., Q_K/Q_W = 1.57.

$$Re = \frac{L^2 N \rho}{\mu}$$

Preliminary runs made without the benefit of temperature control showed that the Reynolds number affected the efficiency to the same degree whether the variable changed was viscosity (by a temperature change) or impeller speed. Thus the Reynolds number seemed to be a proper correlating variable although the density and viscosity were not varied intentionally in the runs reported here. A correlation of efficiency with Reynolds number was attempted, but, as the impeller size or baffling was changed, a series of curves was obtained whose sequence had no obvious relation to the impeller type or size.

However, the work of Mack and Marriner (3) suggested that the efficiency might be a function of the Reynolds number and the dimensionless power function, which is defined as

$$C = \frac{P g_c}{L^5 N^3 \rho}$$

The power number varied over a twenty-five-fold range as impeller type, diameter, and baffling were changed. In the turbulent region the power function is very nearly constant over a wide range of Reynolds number for baffled operation and is affected only slightly by Reynolds number for unbaffled operation. Average values of C were obtained from the power data for each size of impeller for baffled and unbaffled operation and these values are shown in Table 1. The following equation was then derived which represents all the data for unbaffled and baffled propellers, spiral turbines, and flat-blade turbines from 4 to 10 in. in diameter when operated at 1-min. residence time, one-half impeller submergence, and a kerosene to water volume ratio of 1.57:

$$1 - \epsilon = \frac{3.18 \times 10^{15}}{Re^{3.2}\, C^{1.37}} \left(\frac{L}{D}\right)^n \qquad (9)$$

where ϵ is fractional stage efficiency, D is the vessel diameter, and n is zero for baffled operation and 1.6 for unbaffled operation. Actually $(L/D)^n$ is an empirical adjustment required to fit the unbaffled data. The Reynolds numbers for Equation (9) were calculated for each run from the fluid properties at the run temperature (nominally 68°F.). The fluids were assumed present as a fifty-fifty volume mixture since the holdup was usually about 50% in runs without a cover plate. Holdup was not measured in the runs with a cover plate. An average power number was used for each impeller rather than the individual run values since even in the baffled vessel they showed some variation. The equation represents the data fairly well considering the wide range of conditions covered

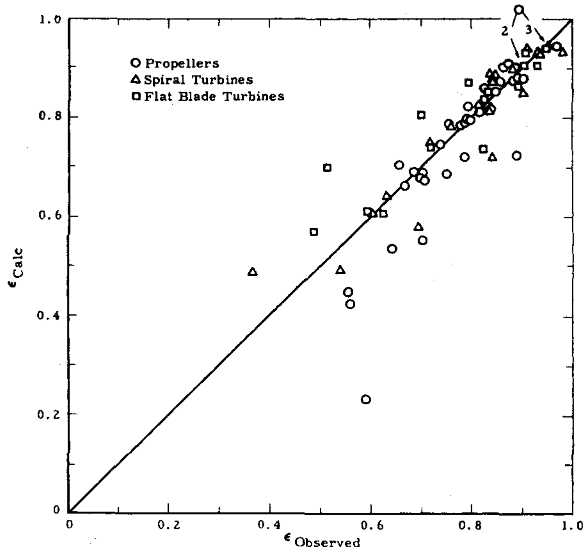

Fig. 9. Efficiency correlation: tests with four wall baffles; one-half submergence, θ = 1.08 min., Q_K/Q_W = 1.57;

$$1 - \varepsilon = \frac{3.18 \times 10^{15}}{Re^{3.2}\ C^{1.37}} \left(\frac{L}{D}\right)^n.$$

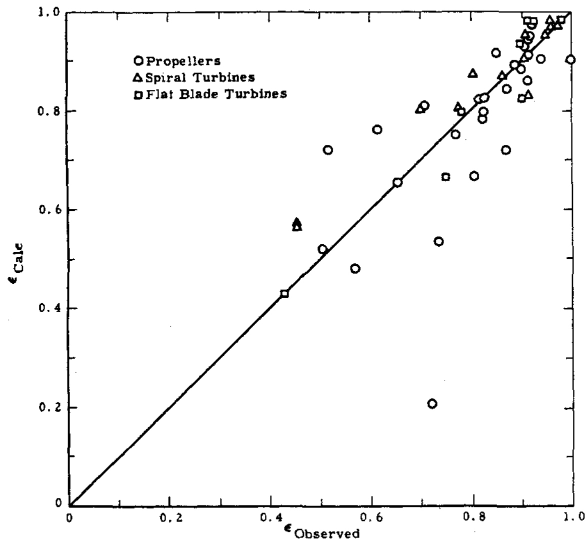

Fig. 10. Efficiency correlation: tests without baffles; one-half submergence, θ = 1.08 min., Q_K/Q_W = 1.57;

$$1 - \varepsilon = \frac{3.18 \times 10^{15}}{Re^{3.2}\ C^{1.37}} \left(\frac{L}{D}\right)^n.$$

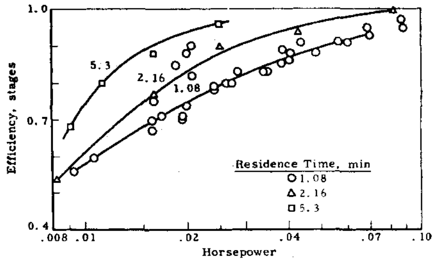

Fig. 11. Effect of residence time; 8-in. propeller, one-half submergence, four wall baffles, Q_K/Q_W = 1.57.

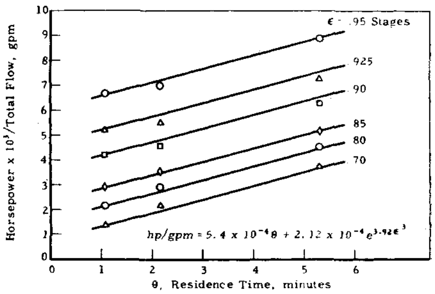

Fig. 12. Specific energy consumption; 8-in. propeller, one-half submergence, four baffles, Q_K/Q_W = 1.57.

and differing types of impellers. Of the total of 132 runs, six (*CF, CH, CJ, EO, FO,* and *HO*) are considered to be in error as they deviate badly from the original power-efficiency curve for the particular impeller. If these plus two others (runs *GA* and *DN*, which gave efficiencies of 0.4 stage or less) are omitted, the equation predicts the experimental runs with average and maximum deviations of 0.041 and 0.36 stage, respectively, for the baffled tests, and 0.067 and 0.51 stage, respectively, for the unbaffled tests. These deviations are shown graphically in Figures 9 and 10. The observed efficiencies cover the range from 0.40 to 1.0 stage although only 20% of the runs are below 0.7 stage and the deviation here is larger than the average. An attempt was made to correlate the runs using over-all mass transfer coefficients, but this was not so successful as the correlation in terms of Reynolds number and power number.

Residence Time

With the 8-in. propeller operating at one-half submergence with four wall baffles, the horsepower-efficiency curves were determined at three residence times, 1.08, 2.16, and 5.30 min. as shown in Figure 11. A cross plot of these data is shown in Figure 12 where specific energy (horsepower/total flow, gal./min.) is plotted against residence time. The points indicated are taken from the curves of Figure 11, and the straight lines are plots of the following empirical equation, which gives specific energy in terms of residence time θ(minutes) and stage efficiency ϵ.

$$\frac{\text{hp.}}{\text{gal./min.}} = 5.4 \times 10^{-4}\theta + 2.12 \times 10^{-4} e^{3.92\epsilon^3} \quad (10)$$

On a percentage basis, specific energy changes rapidly with residence time at low efficiencies but more slowly at high efficiencies. The average error resulting from the use of Equation (10) is 3.3% at efficiencies >0.7 stage. Whether this relationship could be used with other sizes and types of impellers is not known.

Submergence

The effect of the vertical location of the impeller along the central axis of the mixing tank was investigated for all three impeller types. (See Table 3.) When baffles are absent, the vertical placement of the impeller has no effect on the power-efficiency curve for any of the impeller types.

For baffled operation, impeller submergence of one-third liquid depth always gave the best results and three-fourth submergence the poorest. Although this effect is neither large nor incontestably established, it is consistent with the explanation given previously for the superiority of unbaffled operation in that the undesirable end-to-end mixing is most effective in the three-fourth submergence case (impeller near the feed inlet).

Phase Ratio

The kerosene to water volume ratio in the feed streams was varied from 1 to 2.5 while the 8-in.-propeller was operated at one-half submergence with four wall baffles and a 2-min. residence time. As seen in Figure 13, varying the feed phase ratio above and below the values used in most of this work produced only a minor change in stage efficiency. While this change in phase ratio is perhaps too small to justify a general conclusion (since the total flow was changed only from 50% kerosene to 70% kerosene), a wider variation was not attempted.

Off-center Mounting

One way to simulate the effect of baffles in an open unbaffled tank is to mount the impeller off center and thereby reduce swirl. This type of operation was tested with the 6-in. marine propeller mounted with its shaft vertical and with ½-in. clearance between the impeller tip and the vessel wall. (See Table 3.) The performance obtained was intermediate to that of the unbaffled and the baffled cases, but is closer to the former. This indicates that, although swirling is reduced, axial flow is not increased as in the case of wall baffles.

Deflector Ring

A spiral turbine may be baffled by the use of wall baffles or by a concentrically mounted deflector ring as in a Turbo-Mixer. The 6-in. spiral turbine was tested at one-half submergence and 1.08-min. residence time with both types of baffling. (See Table 3). The the power-efficiency curves were identical regardless of which method of baffling was used or whether both were used together.

Cover Plate

A cover plate was used to prevent the formation of a vortex in the mixing vessel when it was operating without baffles. The effect of the cover plate upon efficiency was determined with the 8-in. propeller at one-half submergence and 1.08-min. residence time both with and without baffles. (See Table 3.) As would be expected, no effect of the cover plate was noted during operation with baffles. In the case of unbaffled operation, only two determinations were made without a cover plate. These showed a smaller decrease in efficiency than would be expected in view of the decreased liquid residence time resulting from the deep vortex formed. However, operation with a vortex seemed so unlikely that this matter was pursued no further.

CONCLUSIONS

It has been established that in a continuous-flow, agitated vessel (14¾-in. diameter) with feed introduction at the wall, mass transfer at a given power level is a function of impeller type and baffling as well as size and speed. Optimum results are achieved in the unbaffled vessel with impeller diameter equal to about 40% of the vessel diameter, with the three impeller types giving almost identical performance. The reduction in power requirement with unbaffled operation is largest at efficiency levels of 0.7 to 0.9 stage and is significant in the smaller size propellers but becomes insignificant in the larger size spiral and flat-blade turbines. It is believed to result from better utilization of the concentration differences existing in the feed streams. Whether baffles would have an adverse effect if the feed were introduced directly into the impeller is not established. However, there is some evidence that baffling would also be undesirable in this case.

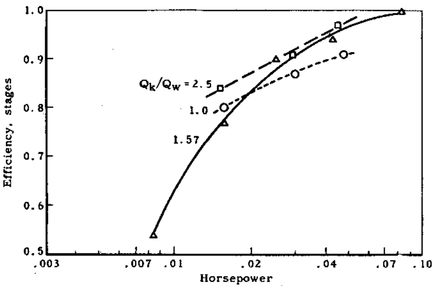

Fig. 13. Effect of phase ratio; 8-in. propeller, one-half submergence, four wall baffles, $\theta = 2.16$ min.

For operation with baffles, the flat-blade and spiral turbines required less power to reach a given efficiency level than did the propellers.

Mass transfer has been correlated in terms of Reynolds number and power number for all runs at 1-min. residence time, both baffled and unbaffled. In addition, horsepower/total flow was correlated for one baffled impeller in terms of residence time and stage efficiency. The effects of phase ratio and the use of a cover plate are shown to be minor.

With the spiral turbine, no difference was found among wall baffles, deflector ring, or both. Off-center, unbaffled location of a propeller is better than centered, baffled operation but not quite so good as centered, unbaffled operation. The vertical location of the impeller has no effect upon the power-efficiency relation in the unbaffled case, but when baffles are present there is a small effect, with the impeller locations farther away from the feed inlet being generally preferable to those nearer.

Naturally, it is not known how the effects of baffling and impeller submergence found in this study will be affected by changes in vessel size and arrangement or in liquid system.

NOTATION

b = intercept of equilibrium line
C = power number, $Pg_c/L^5N^3\rho$, dimensionless
D = vessel diameter, ft.
g_c = conversion factor, 32.17 (lb. mass) (ft.)/(lb. force)(sec.^{2})
h = volume fraction of kerosene to total liquid in the mixing vessel
L = impeller diameter, ft.
m = slope of equilibrium line
N = impeller rotational speed, revolutions/sec.
n = exponent in Equation (3); $n = 0$ for operation with baffles, $n = 1.6$ for operation without baffles
P = impeller power, ft.-lb./sec.
Q = volumetric feed rate, cu. ft./min.
Re = Reynolds number, $L^2N\rho/\mu$, dimensionless
V = volume of vessel, cu. ft.
W = weight feed rate, lb./min.
x = solute concentration in water phase, wt. fraction
y = solute concentration in kerosene phase, wt. fraction
ϵ = contactor efficiency, fraction of equilibrium stage
θ = nominal residence time, $V/(Q_K + Q_W)$, min.
λ = ratio of slopes of equilibrium and operating lines, $m(y_2 - y_1)/(x_2 - x_1) = mW_W/W_K$
μ = geometric mean viscosity of liquid mixture, $\mu_K{}^h \times \mu_W{}^{1-h}$, lb. mass/(ft.)(sec.)
ρ = arithmetic mean density of liquid mixture, $h\rho_K + (1 - h)\rho_W$, lb./cu. ft.

Subscripts

K = kerosene phase
W = water phase
1 = water phase in, kerosene out
2 = water phase out, kerosene in

Superscript

$*$ = equilibrium value

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