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A STUDY OF THE EROSIONAL/CORROSIONAL VELOCITY CRITERION FOR SIZING MULTI-PHASE FLOW LINES

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EXECUTIVE SUMMARY

The current recommended practice (API RP14E) for sizing two-phase flow lines in offshore production piping is to limit the flow velocity to a value less than the fluid erosional velocity. A simple erosional velocity criterion based on a single empirical constant and the two-phase mixture density is provided as guidance to determine the velocity above which erosion may occur. This criterion is for clean service (non-corrosive and sand-free) and it is noted that the limits should be reduced if sand or corrosive conditions are present. However, no guidelines are provided for these corrections. It is widely accepted in the industry that this simple criterion is inadequate. The objective of this project is to upgrade this practice for more realistic conditions. This report presents the results of Phase I of this multi-phase project. This initial phase is strictly an analysis of the open literature, therefore, a complete upgrade of the criteria is not possible. A general form is presented with specific details to be added in subsequent phases of this project.

The general oil and gas production literature was reviewed, which indicated that no measurable erosive damage was documented for clean service. While there has been significant wear problems, all are accompanied by corrosive conditions and/or sand production. The primary location of these problems has been in fittings with elbows experiencing twice as much damage as tees, long radius bends and straight runs downstream of chokes. To determine if there is a physical basis for these observations, we reviewed the fundamental erosion research literature.

The gas-liquid droplet erosion literature indicated that erosive wear due to droplet impact is proportional to the difference between the droplet velocity and a threshold velocity raised to a power. The significance of the threshold is that no erosive damage due to liquid impact occurs at velocities below the threshold. This threshold is about 400 feet per second. Since this is an order of magnitude above practical operating velocities, this confirms the assertion that no erosive damage has been experienced in the field for clean service.

The solid particle erosion literature indicates that solid particle erosion is proportional to the kinetic energy of individual particles. Erosive wear can occur at all velocities, so that for solid particle impacts a small acceptable wear rate is defined. With this wear limit, the kinetic energy models can be solved for velocity, so that an "'erosional velocity" is proportional to the square root of the mass of the particles. However, this "erosional velocity" is based on the particle velocity. In order to develop a relationship for flow velocity, the literature on wear in pneumatic conveyor systems was reviewed.

The pneumatic conveyor literature identified elbows as the primary area of concern and presented models for wear rates in elbows as a function of density ratio of the solid particles to conveyor gas. The erosional velocity from these models is in the form of an empirical constant divided by a root of the density ratio. This root varied between 2 and 3 for practical velocities and particles loadings.

The erosion-corrosion literature indicates that in the absence of solid particles, wear rate increases with flow velocity. This accelerated corrosion, however, is not due to erosion and should be limited by controlling chemistry and not flow velocity. Corrosion in the presence of solid particle impacts follows the same general models as solid impact erosion, except that the target properties are those of the brittle corrosive scale and not the base metal.

The conclusions from this work are:

(1) No erosional velocity limit is required for non-corrosive sand-free service.

(2) Erosional velocity (V_e) for sand producing service is a function of an empirical constant (K) and the density ratio (Z) of sand density to gas density:

$$V_e = \frac{K}{\sqrt[n]{Z}}$$

(3) The values of K and n are functions of corrosive conditions, fitting geometry and material properties of the fittings.

The guideline presented above gives the general form of the equation for erosional velocity. The empirical constants, however, have yet to be determined. The next phase of this research should be to develop values for these constants based on actual field operational experience and a computational fluid dynamics analysis to determine the effect of different fittings on erosional damage in the absence of corrosion as a function of flow velocity, flow regime, and sand production.

A subsequent third phase of the research should be to include the effect of corrosion and to experimentally verify the values of these constants for sand producing service and to perform a systematic test program to develop values for the corrosive environments of CO_2 and H_2S service with sand production.

I. INTRODUCTION

The American Petroleum Institute (API) currently publishes Recommended Practices for various aspects of petroleum production to facilitate the broad availability of proven, sound, engineering and operating practices. One of these Recommended Practices is for the "Design and Installation of Offshore Production Platform Piping Systems" and is designated as API RP 14E [3][1]. The scope of this document is to recommend minimum requirements and guidelines for the design and installation of new piping systems on production platforms located offshore. The conditions covered in this document are pressures up to 10,000 psig and temperatures from -20°F to 650°F. The recommendations presented are based on years of experience in oil and gas development. Almost all of this experience has been for hydrocarbon service free of hydrogen sulfide.

The approach is to provide a standard procedure for sizing the required piping. For single-phase liquid lines, the primary basis for sizing is flow velocity. For single-phase gas lines, pressure drop is the primary basis. Most wells, however, are not single-phase and include two-phase flow including gas and liquid. At some point in the life of some wells, solids in the form of sand are also present. The primary sizing criterion for gas/liquid two-phase flow lines is erosional velocity.

API RP 14E provides the following simple design criterion for erosional velocity (V_e) for avoiding fluid erosive wear:

$$V_e = \frac{C}{\sqrt{\rho_m}} \tag{1}$$

where

ρ_m = mixture density,
C = 100 for continuous service, and,
C = 125 for non-continuous service.

API RP 14E states that fluid velocity should be kept at least below the fluid erosional velocity. If sand production or corrosive fluids are anticipated, fluid velocities should be reduced accordingly. However, a quantitative correction for sand and presence of corrosion is not provided.

API RP 14E recognizes that the values recommended for the empirical constant "C" are conservative in the absence of sand or corrosive environment, and permits using higher values of "C" where specific application studies have shown them to be appropriate. However, when sand production and/or a corrosive environment is present, this value for the empirical constant "C" may not be conservative.

Under conditions when this simple criterion is conservative for platform piping, it can lead to the over design of tubing and platform piping systems. The criterion, when first adopted, represented an average of the existing criteria of several companies which utilized similar formulas with constants varying from 80 to 170. The "consensus" decision of utilizing a constant of 100 was

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1 Numbers in brackets are references cited at the end of this report.

not critical to cost when adopted, as most flowlines in the Gulf of Mexico were low flow rate (1 to 5 MMSCFD). For most instances, the criterion resulted in installing 2-inch to 3-inch flowlines which could be fabricated out of standard pipe wall thickness.

Now that gas wells are being completed in the Gulf of Mexico at flow rates of 50 MMSCFD and higher, and the criterion in RP 14E is being applied to prolific oil wells in North Sea and Middle East locations, the economic impact of this decision is much greater. For example, the criterion, as written, restricts velocity for a 25 MMSCFD, 5000 psig, 0.6 SG, 100 Bbl/MMSCF stream to 21.2 ft/sec, requiring an internal diameter of 2.9 inch for piping upstream of a choke. This requires a 4-inch XXS line. If a constant of 160 were allowable, an erosional velocity of 33.9 ft/sec would be calculated and a 3-inch XXS line would be acceptable. Recent work indicates the even higher constants may be appropriate [2,6,11,13,15].

With any two-phase (gas/liquid) flow, different flow regimes can exist. The resultant erosional conditions are, therefore, extremely dependent on the specific flow regime. If sand production is added, the situation is even more complex. If significant corrosion conditions exist, the effect of erosion on the corrosive scale can be quite severe. The combined erosion/corrosion phenomena can lead to significant metal loss under flow conditions that appear mild from a purely erosional viewpoint.

In summary, the sizing of piping for two-phase flow is significantly more complex than for single-phase flow and a simple velocity criterion is inappropriate. A more realistic method for accounting for erosional effects is required.

The approach for the present study was to review the general literature documenting oil and gas production problems related to erosive damage. Based on these results, additional literature into more fundamental damage mechanisms was reviewed. The summary provided in this report is limited to the literature relevant to developing an appropriate erosional velocity criterion even though a large list of references were reviewed. The reference list includes the total number reviewed, however, only the relevant references are cited.

II. REVIEW OF THE PROBLEM

As indicated above, the application of API's erosional velocity equation may lead to very conservative piping designs for sand free, non-corrosive service; or non-conservative piping designs when these conditions exist. In an effort to better design piping systems to meet the demands of the service, many companies have conducted field or laboratory tests to study pipe erosion as a function of downhole conditions. A limited amount of this data, or recommendations resulting from these studies, is available in the open literature. The available information will be summarized in this section. While reviewing the information presented below, it is important to note that although these practices were reported in the literature as mentioned above, this may no longer reflect the current practices of that company.

A. Company Practices

Phillips Petroleum [11] performed inspections on several wells with average fluid velocities up to 60 ft/sec. In those wells, corrosion was minimized by continuous injection of inhibitors or, when inhibition fails, the use of stainless steel tubulars. Maximum velocities were noted to be in excess of 300 ft/sec immediately upstream of check valves. These lines were replaced every three months. It was reported that Phillips does not use the current API RP 14E erosional velocity criterion.

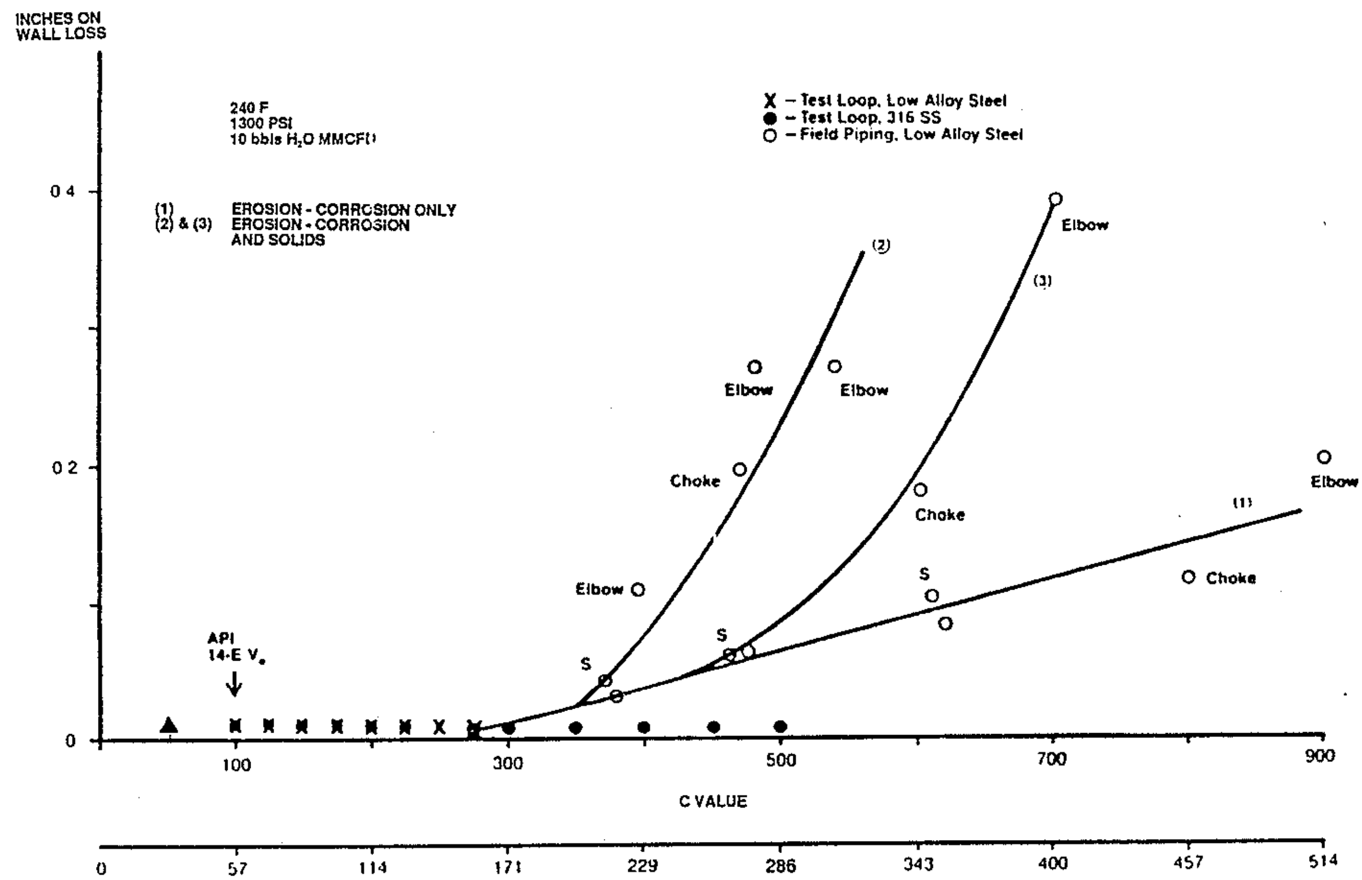
Mobil Oil [11] has conducted field tests and sponsored research on erosion and wear. If sand production can be avoided, Mobil does not seem to limit flow velocities. In one gas field Mobil has avoided flow-enhanced corrosion by increasing production rates so that water condensation does not occur within the production string. Therefore, it seems that under some conditions Mobil does not adhere to the guidelines presented in API RP 14E.

Based on privately funded flow and wear research, Conoco has recommended that the erosional velocity limits be increased. Although Conoco has published a paper on this subject [13], experimental data was not available. As was the case for Phillips and Mobil, Conoco has noted circumstances under which they do not use the API guidelines.

Arco [15] has conducted research in the field using special, horizontal piping systems located above the wellhead. Their experiments included changing the pipe diameters and materials in an effort to investigate flow rates at and well above the API RP 14E limiting velocity. The test report on these experiments was obtained from Arco. The data obtained from this study are presented in Figure 1. The data along the bottom, zero metal loss line, of this plot are for straight pipe sections. This includes data for low alloy steel with an inhibitor, at "C" values of 100 to 275, and 316 SS sections, at "C" values of 100 to 500. Curve 1 represents the metal loss due to erosion-corrosion only with no sand production. Curves 2 and 3 represent the metal loss due to erosion-corrosion and solids.

The data in Figure 1 also indicated where the metal loss was measured. Note that for each curve the metal loss was worst in the elbows followed by the piping downstream of chokes.

Based on this study, Arco indicates "that the API RP 14E is overly conservative for straight tubing with "clean" fluids, but does not establish an upper limit for design purposes that includes bends, solids or corrosive fluids" [15]. They have developed a set of recommended practices for applying API RP 14E to their gas and gas condensate operations. With several precautions and conditions, their procedures are based on using "C" values of 100 for continuous service and 150



Velocity ft/sec

Figure 1. Arco East Seven Sisters Erosion Test Results [15]

service and C=250 for intermittent service when corrosion is controlled or prevented by dehydrating the fluids, using a corrosion inhibitor, or employing higher alloy metallurgy. As mentioned earlier, there are several cautions that accompany these recommendations including the routine use of ultrasonic testing to ensure that the wall loss rate is acceptable.

Farshad [8, 9, 10] published results of a number of field studies. One study for Tenneco [9] presented results for nine Gulf Coast gas wells. During this 18 month study, production rates were controlled and monitored to ensure that four wells were produced below the erosional velocity and five were produced above this velocity. The tubing wall loss in each of these wells was monitored. The results of these tests are presented in Table I. Farshad concluded that "velocities above the erosional velocity accelerated the corrosion rate by a factor of 3.6." Therefore, it seems that Tenneco is pleased with the velocity limits suggested by API RP 14E.

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TABLE I. EFFECT OF VELOCITY ON TUBING WALL LOSS [9]

Well No.	Tubing Velocity *	Wall Loss Mils	Average Loss Mils
1		20	
2	$V_A < V_E$	60	45
3		60	
4		40	
5		110	
6		180	
7	$V_A > V_E$	280	160
8		110	
9		120	

*V_A = actual velocity, ft/sec
V_E = erosional velocity, ft/sec

Heidersbach [11] studied Tenneco's data in more detail. He obtained, and added to the above table, the calculated erosion velocity and actual well velocities. The resulting table as compiled by Heidersbach is shown in Table II. The calculated and actual fluid velocity conditions for wells 3, 4 and 7 were all very similar, that is approximately 30 ft/sec. However, this includes a well with high wall loss and two wells with very little wall loss. Therefore, any conclusions about general practices based on this data are highly questionable.

TABLE II. EFFECT OF VELOCITY ON TUBING WALL LOSS INCLUDING TENNECO DATA [11]

	Tubing Velocity (ft/sec)				
Well Number	Calculated Erosion Velocity	Actual Well Velocity	Comparison	Wall Loss (Mils)	Average Loss (Mils)
1	27	11		20	
2	39	27		60	
3*	31	28	$V_A < V_E$	60	45
4*	30	29		40	
5	30	36		110	
6	30	33		280	
7*	29	30	$V_A > V_E$	110	160
9	36	37		120	

*Wells with similar fluid velocities

B. Summary

In general, the reviewed information indicates that for sand-free flows, an erosional velocity was not required. Additionally, the information indicates that for sand laden gas flows, a new form of the erosional velocity equation is required. The form of the equation should be extended to include factors such as sand production rate, fitting geometry, material properties, and corrosion rate since each of these factors has a significant impact on the wall loss rate.

Information relative to fitting geometry was available from the literature which was discussed above. The critical erosion areas in oil field service piping were identified to be the fittings. These fittings include elbows, tees, long radius bends, chokes, straight runs downstream of chokes, valves, and tube ends and u-bends in heat exchangers. The straight piping just downstream of the fittings was also identified as critical erosion areas. In each of these cases the areas of concern are the points of contacts for impacts. It was reported [7,15] that (1) the maximum erosion occurred in elbows and (2) the erosion in tees, long radius bends, and straight runs downstream of chokes for similar conditions was about 50% of that observed in elbows. This information highlights the fact that the geometries of the fittings has a major impact on metal loss and that any equation used to calculate allowable velocities based on metal loss should include consideration of the fittings in the flow system.

The appropriate parameters, relative to each of these four factors must be developed. That is, the influence of a given parameter on the wall loss, and therefore erosional velocity, must be determined. Therefore, literature documenting basic research was reviewed in the areas of: (1) liquid droplet erosion, (2) solid particle erosion, (3) solid particle erosion in fittings and (4) erosion-corrosion. The body of research published in these areas is extremely extensive. The reviews presented in the following sections are limited to demonstrating the influence of the importance of parameters and not to presenting a comprehensive thesis on fundamental erosion research.

III. LIQUID DROPLET EROSION

Liquid droplets impinging on solid surfaces at high speeds can cause significant damage. This damage can be in the form of permanent deformation and fracture. The bulk of the research into this process has been for steam turbine blade erosion by water drops and the rain erosion of aircraft.

The American Society for Testing and Materials (ASTM) has developed a standard practice for Liquid Impingement Erosion Testing (G73-82) [23]. This practice provides a test procedure for evaluating erosion resistance of solid materials to repeated discrete liquid droplet impact. The cumulative effect of repeated impacts versus time is illustrated in Figure 2(a) and the instantaneous rate versus time is illustrated in Figure 2(b). This shows the complexity in predicting erosion of materials to liquid impact. Three distinct phases occur in this process: (1) incubation period, in which no detectable wear occurs, (2) accumulation period in which erosion rate increases to a maximum and (3) steady state period, in which the erosion rate remains constant at a terminal rate. Adler [16] further subdivided these phases and came up with six distinct phases, by dividing the accumulation phase into an acceleration period, maximum rate period and deceleration period. He also added a catastrophic period following the steady state or terminal period.

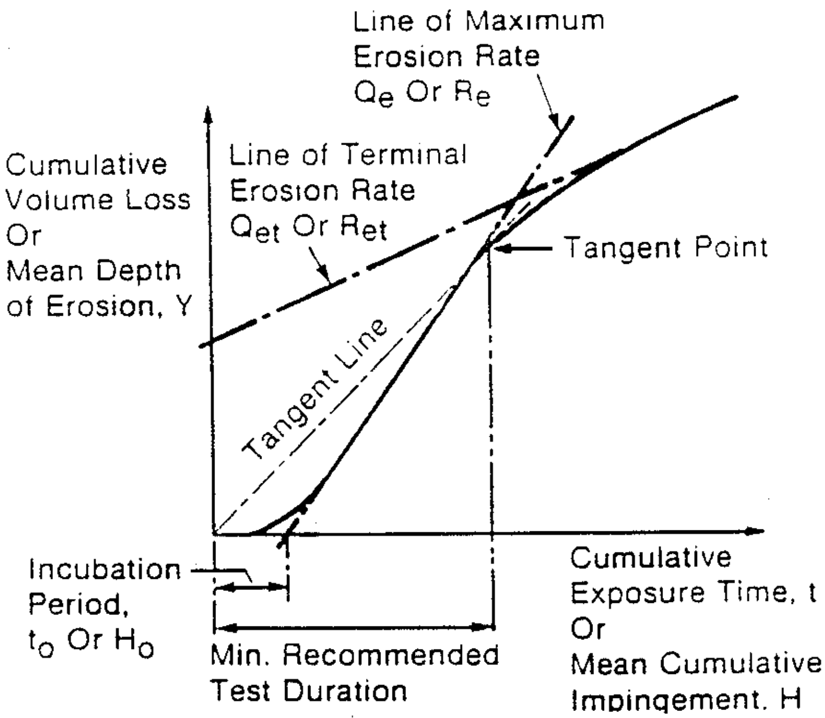
In a study for turbine blade erosion, Baker, Jolliffee and Pearson (17) developed a model for the maximum erosion rate. For the turbine application they were interested in conditions that defined the on-set of erosion in terms of maximum erosion rate (W_{max}), impact angle (θ) and droplet impact velocity (V) so that:

$$W_{max} = \frac{K(V \sin\theta - V_c)^n}{\sin\theta} \tag{2}$$

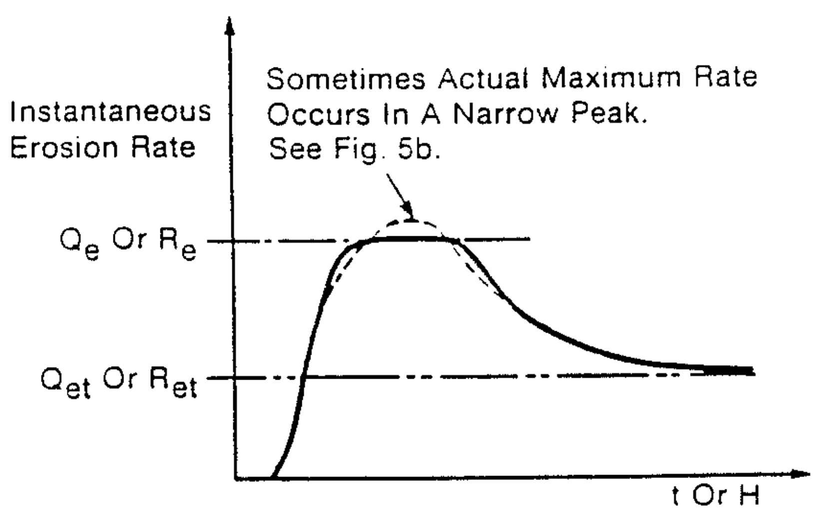
For the materials they investigated which were chrome steel,

n = 2.6,
V_C = 390 fps,
K = material constant.

The significance of V_c is that it is the value of impact velocity below which erosion will not occur.



(a). Cumulative Erosion - Time Curve



(b). Erosion Rate - Time Curve
(Derivative of Cumulative Erosion - Time Curve)

Figure 2. Typical Erosion - Time Pattern and Parameters Used to Quantify It [23]

Brunton and Rochester [18] reviewed droplet erosion and presented similar models as Baker et Al. with the values of n and V_c shown in Table III.

TABLE III. CRITICAL VELOCITY FOR DROPLET IMPACT EROSION

Investigator	n	V_c	Material
	2		
Honegger [18]	2	410 fps	High strength alloys
de Haller [18]	2	295 fps	-
Fyall [22]	3.37	682 fps	Polymethylmethacrylat
Heymann [20]	2.5	-	-
Baker [17]	2.6	390 fps	12% chrome steel

Thiruvengadam, Rudy and Gunasekaran [24] presented their experimental results in terms of:

$$W = f(V^n) \tag{3}$$

where the exponent was empirically determined to be n = 5. A summary of investigation results for this form of a model is shown in Table IV.

TABLE IV. POWERS OF VELOCITY FOR DROPLET IMPACT EROSION

Investigator	n	Material
Heymann [20]	5	Wide range
Hoff [18]	5-7	Wide range
Springer [22]	5	Wide range
Thiruvengadam [24]	5	Wide range
Hanlox [19]	4.5	Polymethylmethacrylate
Rochester [21]	4.8	Nickel

In summary, the majority of the water droplet research is presented in terms of high impact velocities. The threshold velocities below which no measurable erosion was observed is approximately 400 fps. This value is higher than realistic values of impact velocities expected in off-shore piping applications. Therefore, it appears that no significant erosion damage by pure droplet impacts is expected for these applications. However, no specific experimental data was presented for the exact conditions and piping materials that occur in off-shore piping.

IV. SOLID PARTICLE EROSION

A significant amount of research has been conducted in the area of flow-induced erosion of small, high-velocity particles in gas streams. The flow characteristics used in most of these studies involved either simple, single particle tests, or uniform, dilute jets of particles, where particle velocity, particle size, and impingement angle were fixed during a test.

This literature can be roughly divided into two categories: model development and investigation of erosion parameters. Several approaches have been taken to developing a model for erosion: theoretical, statistical, and phenomenological. Each of the significant models will be reviewed to provide a basis for understanding of the relevant parameters involved. In order to simplify the investigation of erosion and/or its modeling, many investigators have taken to determining the effects of a wide variety of parameters on erosion.

Since the practical field experience in off-shore piping systems indicates that the presence of sand is the major contributor to piping damage, this review of solid particle erosion is more extensive than the droplet erosion which is not a major contributor to piping damage.

A. Model Development

The first analytical investigation of the erosion phenomenon was undertaken by Finnie [31]. This study modeled a single particle as it struck a ductile flat surface or target and caused scratches. It was observed that these scratches appeared similar to microscopic "machine grooves". The quantity of material displaced was proportional to the kinetic energy of the impacting particle and inversely proportional to a property of the target defined as "flow stress". Therefore, the higher the flow stress the greater the resistance to the micromachining forces and the smaller the chip formed. It was also observed that the angle at which the particle struck the surface had a significant effect. The model for an individual particle collision was given in terms of weight loss:

$$W = cf(\alpha)dMV^2/2p \tag{4}$$

where

W	=	weight loss from target,
c	=	constant for specific erosion system,
$f(\alpha)$	=	function of impingement angle (α),
d	=	density of target,
M	=	mass of particle,
V	=	velocity of particle,
p	=	flow stress of target.

The constant c was used to account for the fact that not all particles would transmit all of the available kinetic energy and was determined experimentally to be 10%. The function of impingement angle was proposed to be zero at an angle of zero degrees, and zero at an angle of 90 degrees measured from the surface (i.e. 90 degrees is perpendicular to the surface and 0 degrees is parallel to the surface) with a maximum of about 18.5 degrees. It was observed, however, that the experimental data resulted in a finite weight loss at 90 degrees. Thus, a second term, ε, was added that had a magnitude of zero at 18.5 degrees and increased linearly up to 90 degrees. The results of this technique are illustrated in Figure 3.

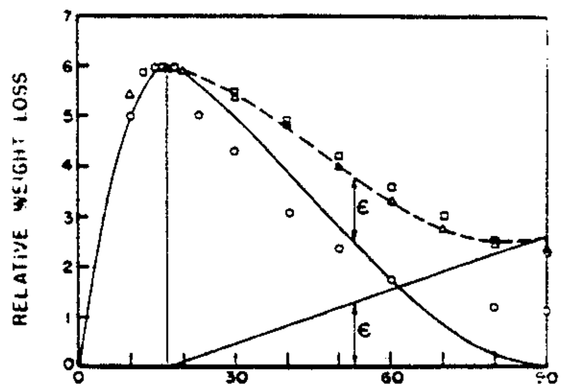


Figure 3. Predicted and Measured Relative Weight Loss, Maximum Weight Loss Taken as the Same in all Cases, Experimental Points Δ Cooper o Aluminum, □ SAE 1020 Steel [31]

Finnie [32] then attempted to apply this same technique to brittle targets and concluded that it was not possible at that time. Bitter [28] took an even more basic approach and assumed that there are two types of erosive wear, cutting wear and deformation wear. The cutting wear is similar to that defined by Finnie. Deformation wear occurs when the target surface is repeatedly deformed by impacting particles causing the surface to work-harden and crack. Propagation and spreading of the cracks result in material removal. Both types of wear occur in any material but cutting wear is dominant for ductile targets and deformation wear is dominant for brittle targets. The form of the equations for predicting the total erosive wear includes both components as:

$$W = W_d + W_c \tag{5}$$

$$W_d = \frac{dM(V\sin\alpha - K)^2}{2\varepsilon} \tag{6}$$

$$W_c = W_{c1} \quad at \quad \alpha \le \alpha_o$$

$$W_c = W_{c2} \quad at \quad \alpha \ge \alpha_o$$

$$W_{c1} = \frac{2Mcd(V\sin\alpha - K)^2}{\sqrt{V\sin\alpha}} \left[V\cos\alpha - \frac{c(V\sin\alpha - K)^2\rho}{\sqrt{V\sin\alpha}} \right] \tag{7}$$

$$W_{c2} = \frac{dM[V^2\cos^2\alpha - K_1(V\sin\alpha - K)^{3/2}]}{2\rho} \tag{8}$$

$$d^{1/4}V^{1/2}y^{-1/4} = \frac{\cos\alpha_o}{\sin^{3/2}\alpha_o} \quad (9)$$

$$K = \frac{\pi^2 y^{5/2}}{2\ \ 10}\sqrt{\frac{1}{d}}\ \left[\frac{1-q_1^2}{E_1}+\frac{1-q_2^2}{E_2}\right]^2 \quad (10)$$

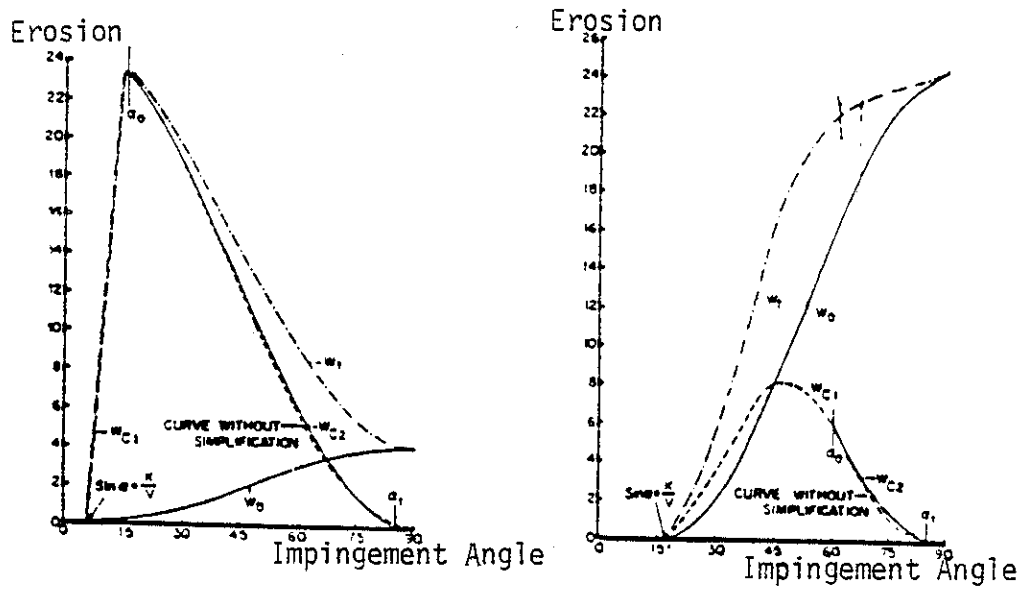
$$K_1 = 0.82y^2\sqrt{\frac{y}{d}}\ \left[\frac{1-q_1^2}{E_1}+\frac{1-q_2^2}{E_2}\right]^2 \quad (11)$$

where

W	=	total weight loss,
W_d	=	deformation wear,
W_c	=	cutting wear,
W_{c1}	=	cutting wear at $\alpha \le \alpha_o$,
W_{c2}	=	cutting wear at $\alpha \ge \alpha_o$,
d	=	density,
M	=	total mass of impinging particles,
V	=	velocity of impinging particle,
α	=	angle of impingement,
α_o	=	angle of impingement when horizontal velocity becomes zero,
K	=	material constant per eq (10),
K_1	=	material constant per eq (11),
ε	=	deformation factor,
c	=	constant = $(0.288/y)(d/y)^{-1/4}$,
ρ	=	cutting wear factor,
y	=	elastic load limit,
q_1	=	Poisson's ratio for particle,
q_2	=	Poisson's ratio for target,
E_1	=	Young's modulus for particle,
E_2	=	Young's modulus for target.

While this model is somewhat more complex than Finnie's model, it provides a better description of the erosion of real materials and predicts the wear on both ductile and brittle materials as shown in Figure 4. Note the similarity between the deformation wear curve in Figure 4(a) and the correction used by Finnie in Figure 3.

The complexity of this model lead Neilson and Gilchrist [56] to try to develop a simpler model to describe the erosion of materials. These investigators realized the conflict between using the simple model of Finnie and the complex model of Bitter. They, therefore, developed a model that combined various aspects of each, which results in the following:



(a). Ductile Material **(b). Brittle Materials**

Figure 4. Predicted Erosion as a Function of Impingement Angle

$$W = \frac{dMV^2\cos^2\alpha\sin\alpha\eta}{2\rho} + \frac{dM(V\sin\alpha - K)^2}{2\varepsilon} \qquad \alpha \le \alpha_o \tag{12}$$

$$W = \frac{dMV^2\cos^2\alpha}{2\rho} + \frac{dM(V\sin\alpha - K)^2}{2\varepsilon} \qquad \alpha \ge \alpha_o \tag{13}$$

where

α_o	=	$\pi/2n$,
η	=	empirical constant.

All other terms are as defined above. α_o Is the angle at which the component of the particle velocity parallel to the target surface becomes zero during impact, above which deformation wear predominates, and below which cutting wear becomes significant.

As a result of the work by Finnie, Bitter and Neilson/Gilchrist the following factors were identified as being important in predicting erosion damage:

1. The normal component of kinetic energy of the impinging particles is absorbed in the target and accounts for deformation wear.

2. For certain hard targets, subjected principally to deformation wear, there is a limiting component of velocity normal to the surface below which no erosion takes place. This value is dependent on particle shape.

3. The kinetic energy component parallel to the surface is associated with cutting wear.

4. For cutting wear and large angles of attack the particles may come to rest in the target and the parallel component of kinetic energy contributes to cutting wear. For small angles of attack, however, the particles may sweep into the target and finally leave again with a residual amount of parallel kinetic energy.

Two major improvements were provided by Bitter and Neilson/Gilchrist over that of Finnie. The first was the realization that there are both cutting wear and deformation wear in all real materials and their models quantify that effect. The second was the realization of the effect of kinetic energy in both cases. In cutting wear, which is the special case that Finnie investigated, the component of velocity parallel to the surface is used to define the kinetic energy of the impinging particles. The driving force for surface damage is the absorbed kinetic energy i.e. the difference between that energy that the particle has prior to impact and the energy the particle has after it leaves the surface. In deformation wear, the component of velocity perpendicular to the surface above a minimum threshold is used to define the kinetic energy, which is the driving force for surface damage.

Rabinowicz [57, 58, 60] postulated that solid particle erosion is a form of abrasive wear and is, therefore, governed by the standard abrasive wear model, suitably modified so that the load and displacement terms are replaced by terms representing the kinetic energy of the abrading particles.

$$W = \frac{MV^2 c\beta}{gp} \tag{14}$$

Sheldon and Kanhene [63] developed an empirical model based on observations of the crater formed by single particle impacts. They proposed that material is removed by a displacing action that results in fracture of removed material at sufficient strain. This process is comparable to single point cutting experiments. Their principle result is that the exponent of velocity and diameter is 3, which departs from the value based on kinetic energy models. So that:

$$W = \frac{MV^3 D^3 (\rho_p)^{3/2}}{H_v^{3/2}} \tag{15}$$

Two additional empirical models have been proposed that are more relevant to corrosive scales and are of similar form for the case of brittle materials. One was developed by Evans [29], which gave weight loss as:

$$W = cV^{19/6} D_p^{11/3} d_p^{19/12} K_c^{-4/3} H^{-1/4} \tag{16}$$

where

c	=	empirical constant,
K_c	=	target toughness,
H	=	hardness.

All of the other terms are as defined above. The other was by Ruff and Wierderhorn [61], which gave erosion as:

$$W = cV^{22/9} D_p^{11/3} d_p^{11/9} K_c^{-4/3} H^{1/9} \tag{17}$$

All of the terms are as defined above.

A theoretical analysis developed by Hutchings [39] for normal impingement on ductile metals employs a criteria of critical plastic strain to determine when material will be removed. This model incorporates particle velocity, mass of particles, dynamic hardness of the target and ductility of the target under eroding conditions. The functional relationship, however, is not directly in terms of kinetic energy but takes the following form:

$$W = cid_t d_p^{1/2} V^3 / ed^2 H^{3/2} \tag{18}$$

where

c	=	constant of 0.033,
i	=	indentation volume,
ed	=	erosion ductility.

All other items as defined above.

These investigators also obtained an energy balance for the initial incoming kinetic energy. The result was as follows:

particle rebound kinetic energy	=	5 to 9%
elastic wave energy	=	5 to 1%
dissipated in plastic work	=	~90%
(~80% as heat ~10% as stored energy)		

A more mechanistic approach to the problem was presented by Jennings, Head and Manning [42] for ductile erosion by applying dimensional analysis. They conducted an initial experimental program which observed that melting was a major operative mechanism. The following model was then proposed:

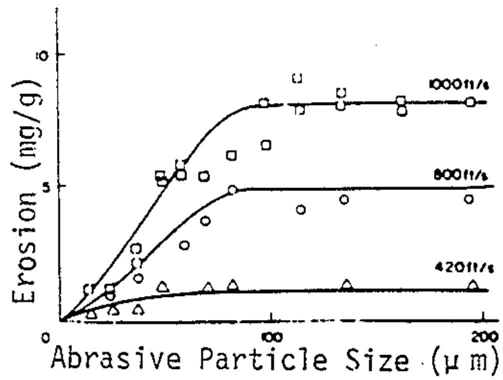
$$W = K_t^{5/2} M d^{2/3} G^{1/3} / RkT_m H_m \tag{19}$$

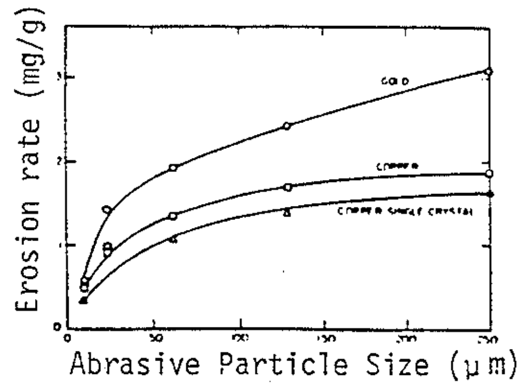
where

K_t	=	kinetic energy transferred per unit mass,
	=	$(V_i^2 - V_r^2)/2$,
V_i	=	impinging velocity,
V_r	=	rebound velocity,
G	=	target molecular weight,
R	=	particle roundness = SUM(c)/I N,
c	=	radius of curvature for individual corners,
I	=	radius of maximum inscribed circle,
N	=	number of particle corners measured,
k	=	target thermal conductivity,
H_m	=	target enthalpy of melting,
T_m	=	target melting temperature.

B. Investigation of Erosion Parameters

A number of independent materials parameters have been studied to determine the effect on erosion exclusive of any predictive models. A significant number of studies have been devoted to refining Finnie's [31] basic model by replacing his "flow stress" with different target properties. These efforts are summarized in Table V. One parameter whose effects are still somewhat controversial is the effect of particle size. Finnie, Wolak and Kabil [34] observed that the flow stress parameter in Finnie's original model is proportional to target hardness and that there was a particle size effect. Neilson and Gilchrist [56], however, concluded that particle size did not have a direct effect on erosion but did have a significant effect on the velocity obtained in a test rig and therefore appeared to have an effect. If the particle velocity is measured directly the particle size is not important. Misra and Finnie [55] reviewed the subject and concluded that the only explanation for the size effect which cannot be discounted is that shallow surface layers exhibit a higher flow stress than that of the bulk material when they are abraded or eroded. Figure 5 illustrates the relationship between erosion and particle size. As can be seen, once the particle size reaches a threshold there is no effect of particle size. This threshold is a function of a number of bulk material parameters and resulting surface layers or films.





(a). For 11% Cr Steel with 90° Impact (b). SiC at 20° Impact and 120 m/s

Figure 5. Erosion Rate as a Function of Abrasive Particle Size

TABLE V. PARAMETER DEVELOPMENT

Parameter to Replace Flow Stress in Finnie's Model	Author	Reference Number
Hardness	Finnie, Wolak and Kabil	34
Thermal Pressure Enthalphy for Melting	Ascarelli	26
Target Metal-Metal Bound Energies	Hutchings	40,41
Ultimates Resilience	Vijh	67
Vickers Hardness	Eyre	30
Ductility	Levy	45
Linear Coefficient of Thermal Expansion	Jones and Lewis	44
Specific Energy	Malkin	47
Strain Energy	Rickerly	60
Proof Resilience Ultimate Resilience Strain Energy	Rao and Buckley	59

Marshall, et Al. [50], investigated the effect of particle size distribution and the effect of different rigs in generating velocity as a function of size. The two rigs studied were a gas stream impingement rig where the particle velocity is a function of particle size and a slinger type rig where the particle velocity is independent of particle size. With both rigs experiments were conducted for four particle distributions with the same mean particle size. The distributions are illustrated in Figure 6(a) and the target mass loss as a function of impinging particle mass for each rig is shown in Figure 6(b). Figure 6(c) is a plot of erosion ratio versus size distribution where the erosion ratio is the erosion for a given condition divided by the erosion with only particles at the mean size (i.e. no size distribution).

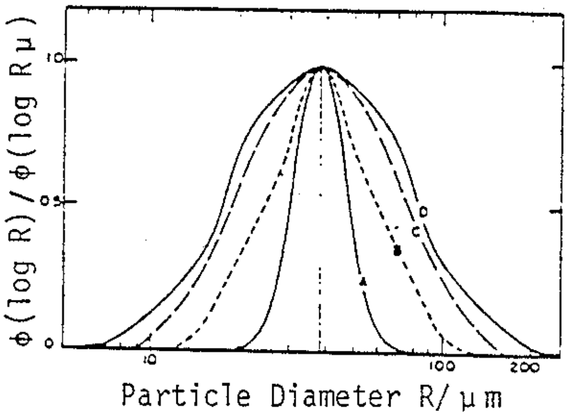
Two important observations were made by these investigators. The first was that most of the material removal was made by the larger particles. The second was that a controlled experiment using particles matched in median size to the in-service particles, but with a narrower distribution of sizes, will result in a serious underestimate of erosion rate. The concept of parameter distribution is extremely important for real applications where most impact parameters do have a distribution after a given range.

Another set of independent parameters that have been show to affect the rate of erosion are particle composition and shape. Levy and Chick [46] determined that there was a significant correlation between particle hardness and target mass loss. When the particles were strong enough not to break up on impact, however, the erosion rate became independent of particle hardness. It was also determined that angular particles of the same size resulted in four times the erosion of spherical particles.

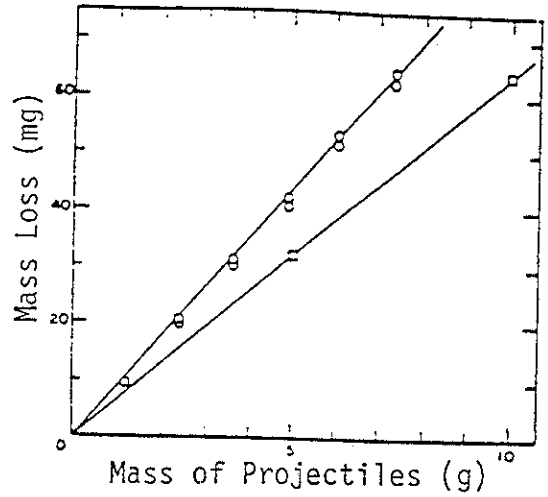
C. Summary of Erosion Parameters

In summary, the bulk of the solid particle erosion literature agrees on the primary parameters affecting erosion even though there are disagreements on some of the secondary parameters. These parameters can be divided into three groups: impact variables, impinging particle properties and target properties. Since the important quantity in erosion appears to be the total kinetic energy absorbed by the target, the general consensus is that the primary impact variables are particle velocity, impingement angle, and the mass flow of particles. In most experiments it has been assumed that all of these parameters have only a single value during a given period. However, if there is a range of values, i.e. a distribution, then the distribution is also important. The important particle parameters are shape, hardness, and maybe size. The important target properties appear to be dynamic hardness, yield strength, and perhaps thermal properties, but the literature includes a wide range of different target properties used to correlate the data. Note, however, that in terms of erosion-corrosion the primary surface properties may be governed by the corrosion process and will, therefore, be the properties of the outer, brittle scale.

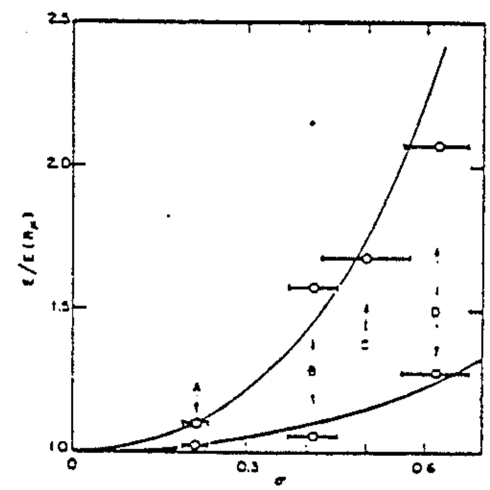
These models can be used to investigate the functional relationship of a velocity criterion by solving each for velocity, however, some models such as Neilson/Gilchrist, Bitters, Hutchings, and Jenny/Head/Manning are not directly solvable in terms of velocity. Table VI presents models for solid particle erosion on ductile surface and the solution in terms of velocity. Table VII presents models for solid particle erosion on brittle surfaces and the solution in terms of velocity. These tables indicate that the general form of all of the models is:



(a) SIZE DISTRIBUTIONS FOR THE BATCHES OF SiC PARTICLES USED IN THE EROSION EXPERIMENTS.



(b) TYPICAL MEASUREMENTS OF TARGET MASS LOSS AS A FUNCTION OF THE MASS OF THE PROJECTILES (BATCH A SiC PARTICLES; $= 100\ ms^{-1}$; NORMAL INCIDENCE; SILICON TARGET): ○, SLINGER; □, GAS STREAM.



(c) EROSION RATE AS A FUNCTION OF THE PARTICLE DISTRIBUTION WIDTH FOR TWO EROSION DEVICES (A-D REFER TO THE BATCHES OF SiC PARTICLES OF FIGURE 13a IMPACTING A SILICON TARGET AT NORMAL INCIDENCE; $= 100\ ms^{-1}$): ○, SLINGER; □, GAS STREAM.

Figure 6. Effect of Particle Size Distribution [65]

$$V_i = \frac{K}{\sqrt[n]{M_{sand}}} \qquad (20)$$

where

V_i	=	particle impact velocity,
n	=	2 to 3,
K	=	function of allowable erosion rate, target properties, impingement angle and surface chemistry,
M_{sand}	=	mass of sand.

However, this is not flow velocity it is particle impact velocity. A relationship between the flow velocity and impact velocity is, therefore, required.

TABLE VI. SOLID PARTICLE EROSION ON DUCTILE SURFACE

- **FINNIE MODEL**

$$W = \frac{MV^2 c d f(\alpha)}{2p} \quad \rightarrow \quad V_i = \frac{\sqrt{W2p/dCf(\alpha)}}{\sqrt{M}}$$

- **SHELDON AND KANHERE MODEL**

$$W = \frac{MV^3 D^3 (\rho_p)^{3/2}}{H_v^{3/2}} \quad \rightarrow V_i = \frac{\sqrt[3]{WH_v^{3/2}/\rho_p^{3/2}\rho_t}}{\sqrt[3]{M}}$$

- **RABINOWICZ MODEL**

$$W = \frac{MV^2 c\beta}{gp} \quad \rightarrow V_i = \frac{\sqrt{Wqp/c\beta}}{\sqrt{M}}$$

TABLE VII. SOLID PARTICLE EROSION ON BRITTLE SURFACE

- **EVANS MODEL**

$$W = \frac{cV^{19/6} D^{11/3} d_p^{19/12}}{K_c^{4/3} H^{1/4}} \rightarrow V_i = \frac{\sqrt[3.2]{W K_c^{4/3} H^{1/4} / C' d_p^{13/36}}}{\sqrt{M}}$$

- **RUFF AND WIEDERHORN MODEL**

$$W = \frac{cV^{22/9} D^{11/3} d_p^{11/9} H^{1/9}}{K_c^{4/3}} \rightarrow V_i = \frac{\sqrt[2.4]{W K_c^{4/3} / c' H^{1/9}}}{\sqrt{M}}$$

V. SOLID EROSION IN FITTINGS

Field experience has indicated that the primary erosion damage occurs due to solid particles in fittings with elbows being the worse case. Literature for pneumatic conveying applications was reviewed, which confirmed that elbows were the critical location.

Mason and Smith [51] performed experiments with pneumatically converged alumina particles in 1 inch and 2 inch square-section 90° bends. They analyzed their data in terms of maximum depth of wear at the primary wear point and derived equations for mean wear rate for both dense-phase

$$W_{mean} = c V_F^{2.25} Z^{-1.36} \tag{21}$$

and dilute-phase

$$W_{mean} = c V_F^{2.25} Z^{1.36} \tag{22}$$

where Z is the density ratio of the solids to the gas.

Shimoda and Yukama [64] conducted experiments in a 90° bend typical of pneumatic conveying system. They also conducted experiments in a standard jet test and compared damage in specimens for both techniques to back out the comparable conditions for the two. They used these data to generate a model for erosion where

$$W = c V_F^{2.8} Z^{-0.6} d H_v^{-0.4} \tag{23}$$

For values of V_F= 90 to 300 fps, Z = 0 to 20 lbs of particles to lbs of air, d = 23 to 500 microns, and H_V = 30 to 500 Vicker's hardness.

Yeung [69] performed a purely analytical study by combing Finnie's model for erosion rate and the calculation of flow patterns in an ideal toroidal geometry for a 90° bend with a pipe radius of 2 inches and a mean bend radius of ll inches. He made the following idealization:

1. The particle loading is sufficiently small that particle-particle interaction is negligible compared with particle-fluid interaction,

2. The presence of the particles does not influence the gas flow field,

3. Of all the forces that act on the particle, only the aerodynamic drag force caused by a velocity difference between the fluid and the particle is significant,

4. The drag force is assumed to obey Stokes Law throughout the analysis,

5. The gas-particle mixture enters the curved pipe with a uniform velocity.

He determined that the particle velocity approached the fluid velocity at low values of velocity, particle loading and particle size. For conditions where the particles impacted the wall, the model was divided into two regions. For the low velocity region, the particle trajectory closely followed the streamliner and impacted the wall at shallow angles. The impact velocity was much lower than the initial conditions provided as entrance conditions. For fluid velocity less than or equal to 8 fps, the maximum erosion rate can be calculated from:

$$W_{max} = c V_F^{3.96} Z \tag{24}$$

For fluid velocity greater than 8 fps,

$$W_{max} = c V_F^{3} Z \tag{25}$$

Since a number of these investigators determined that the particle velocity approached the fluid velocity as the velocity decreases, the phase density decreases or the particle size decreases; a simplified assumption is that the particle velocity is equal to the fluid velocity. This "simple" model can be added to the above models and the velocity criteria can be developed by solving for fluid velocity. Table VIII summarizes these velocity criterion along with the author and original erosion model. All of these models for velocity criterion are in the form of:

$$V_F = \frac{K}{\sqrt[n]{Z}} \tag{26}$$

where

V_F	=	fluid velocity,
n	=	positive value for dilute flow (2 to 3 above 8 fps),
n	=	negative value for dense flows,
K	=	a function of material properties, geometry and maximum allowed erosion rate,
Z	=	density ratio.

It is important to note that the velocity in these models is the fluid velocity and not the particle impact velocity used in the model presented for solid particle erosion. However, the form of the equation is similar except that the parameter describing sand production is in terms of density ratio. Both bodies of literature indicated that the erosional velocity is inversely proportional to some root of a parameter of sand production.

TABLE VIII. EROSIONAL VELOCITY CRITERION FOR 90° ELBOWS

ELBOW EROSION MODELS

o SIMPLEST ASSUMPTION

$$V_I = V_F \quad \rightarrow \quad V_F = \frac{K}{\sqrt{Z}}$$

$V_I \;\rightarrow\; V_F$ *AS* V_I *INCREASES*, Z *INCREASES*, d *INCREASES*

o **MASON AND SMITH MODEL (DILUTE LOADING)**

$$W_{mean} = c V_F^{2.25} Z^{1.36} \quad \rightarrow \quad V_F = \frac{\sqrt[2.25]{W_{mean}/c}}{\sqrt[1.65]{Z}}$$

o **MASON AND SMITH MODEL (DENSE LOADING)**

$$W_{mean} = c V_F^{2.25} Z^{-1.36} \quad \rightarrow \quad V_F = \sqrt[2.25]{W_{mean}/c} \quad \sqrt[1.65]{Z}$$

o **SHIMODA AND YUBAWA MODEL (DENSE LOADING)**

$$W = c V_F^{2.8} Z^{-0.6} d H_V{}^{-0.4} \quad \rightarrow \quad V_F = \sqrt[2.8]{W H_v / c d} \quad \sqrt[1.68]{Z}$$

o **YEUNG MODEL (DILUTE LOADING)**

FOR $V_F > 8$ Fps

$$W_{\max} = c V_F^3 Z \quad \rightarrow \quad V_F = \frac{\sqrt[3]{W_{\max}/c}}{\sqrt[3]{Z}}$$

FOR $V_F \leq 8$ Fps

$$E_{\max} = c V_F^{3.93} Z \quad \rightarrow \quad V_F = \frac{\sqrt[3.93]{W_{\max}/c}}{\sqrt[3.93]{Z}}$$

VI. EROSION-CORROSION

While the erosional velocity criteria in API RP 14E does not explicitly cover corrosion, it does note that piping systems that handle corrosive fluids may experience excessive metal loss due to erosion/corrosion at velocities lower than those indicated by the erosional velocity limit. This survey will therefore address this issue. However, the subject of corrosion in offshore two-phase flow lines is much broader than what can be covered in this small survey. The discussion is limited to the potential effect of fluid velocity on metal loss and the consequences on the erosional velocity.

Corrosion of offshore piping systems is a significantly well-documented problem [75-85]. Even in wells that are originally judged non-corrosive, cases of localized corrosion have been observed. The localized corrosion rate is a function of multiple variables, which include fluid chemistry, tube metallurgy, and accelerating factors of flow rate and sand production. As indicated in prior sections of this report, the effect of corrosion on solid particle erosion is to provide a brittle target for the solid particles. The combined process of erosion-corrosion has a number of potential effects depending on the relative rates of each of the individual processes.

In pure corrosion, the rate of metal loss may decrease with time and may eventually stifle itself due to the growth of a protective scale, as shown in Figure 7(a) [89]. If the mechanical action of erosion removes or reduces the thickness of this scale, then the stifling process is reduced or does not occur, as shown in Figure 7(b) and 7(c). This combined process is termed erosion-corrosion. From this simple explanation, it is clear that the rate of scale growth and the rate of scale thinning governs the total metal loss. When the rate of scale growth is significantly greater than scale thinning, the scale thickness will increase, the rate of metal loss decreases and the total metal loss is due to corrosive action. The mass transfer of the alloying element in the wall and reactive agent in the fluid combine to grow the scale and thin the base metal. When the rate of scale thinning is significantly greater than the scale growth, the effect is metal loss due to pure erosion. However, the composition of the metal surface has been altered due to the corrosive action and if the surface hardness is decreased due to the depletion of the alloying elements, then the erosional rate is greater than that which would be expected due to pure erosion. If the rates of scale growth and scale thinning are of similar magnitude, then the accelerated corrosion illustrated in Figure 7 will occur.

The prediction of corrosive damage is an extremely complex issue that does play a role in the erosion-corrosion process, but is far more involved than what can be presented here. The scale composition, scale mechanical properties, and resulting damage are specific to the chemistry of the flowing fluid and the metallurgy of the pipe wall. However, for the case with sand present the functional relationship for erosional velocity and sand production is similar to pure erosion even though the magnitude may be greater when corrosion is present. For the case of liquid impingement, it does not appear to be significantly affected by the scale-thinning process. The rate of metal loss is increased with an increase in velocity, but the process is somewhat different.

As illustrated above, the fluid velocities for this application are below those that cause erosional damage due to liquid droplet impacts. However, metal loss has been shown to increase with an increase in fluid velocity even when that velocity is below a pure erosional limit. Sanchez-Caldera, Griffith and Rabinowicz [86] studied erosion-corrosion in steam power plant piping and identified additional mechanism of erosion-corrosion. It was observed that there were two high wear regions in elbows: one on the outside of the bend and the other on the inside of the bend (as shown in Figure 8). The metal loss on the inside of the bend was definitely not due to droplet impact erosion, but metal loss in both regions increased with an increase in fluid velocity.

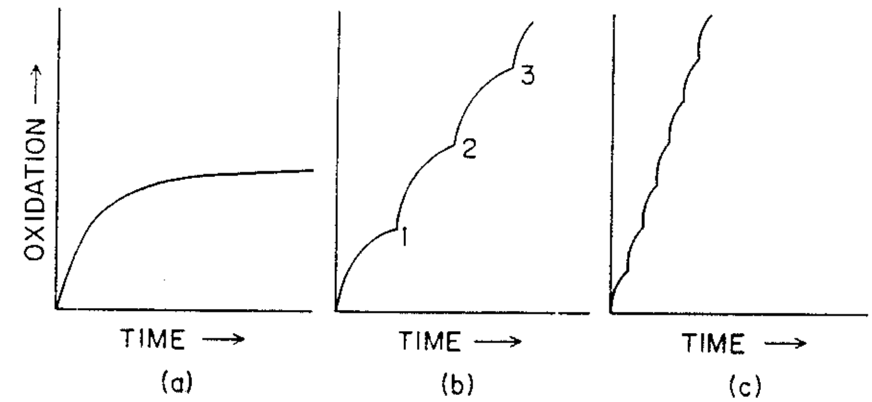


Figure 7. Oxidation versus time of exposure for (a) normal film growth; (b) film broken at 1, 2, and 3; and (c) film broken fequently, producing linear rate of oxidation. [89]

They determined that the secondary flow in the elbow caused significant flow of condensate from the outside of the bend to the inside of the bend and the wear patterns coincided with the secondary flow patterns. They experimentally investigated the process and developed the following model:

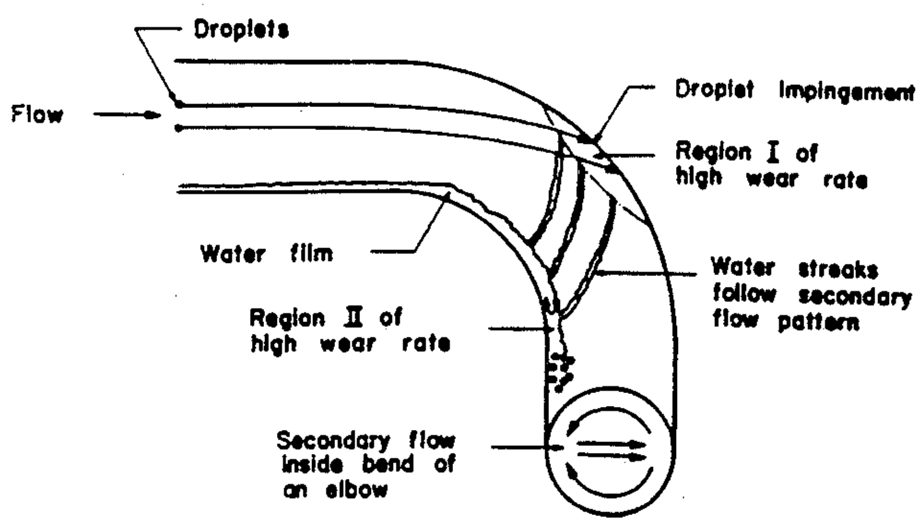


Figure 8. Wear Regions in an Elbow of a Steam Extraction Line [86]

$$W = \frac{\theta(C_e - C_\infty)}{(1/k) + (1-f)[(1/h_d) + (d/D)]} \tag{27}$$

where

W	=	wear rate,
C_e	=	equilibrium concentration of iron species (this concentration depends on temperature, hydrogen concentration, and pH),
θ	=	porosity of open area of metal,
h_d	=	mass transfer coefficient,
k	=	A exp (-E/RT), the reaction rate constant,
f	=	fraction of oxidized metal into magnetite at the metal - oxide interface,
D	=	diffusion coefficient,
d	=	oxide thickness,
C_∞	=	iron species concentration in the bulk of the fluid.

The effect of velocity was to increase the mass transfer coefficient. The conclusion is that the flow of condensate increases the rate of arrival of the corrosive species to the metal surface and the rate of removal of the corrosion product from the surface and, therefore, increases the rate of corrosion. While this mechanism is not erosional damage, the flow induced accelerated corrosion is referred to as erosion-corrosion and is a function of fluid velocity. This mechanism does not easily fit a simple relationship for erosional velocity and must, therefore, be investigated in more depth for the current application in future research. However, the approach to minimizing this form of damage should be to control chemistry instead of velocity.

VII. CONCLUSIONS AND RECOMMENDATIONS

A. Conclusions

Based on information in the open literature, sand-free and corrosion-free service does not require any erosional velocity limitation. Significant erosional damage does occur for service with sand production. The damage increases with an increase in flow velocity and an increase in sand production. This damage is even worse if corrosive fluids are present in production. While corrosive service without sand production does have accelerated damage with an increase in velocity, this damage is not erosional in nature and the method for limiting damage should be to control the chemistry not to limit velocity. Therefore, the following guideline is suggested:

2.5 Sizing Criteria for Gas/Liquid Two-Phase Lines.

a. Erosional Velocity. Flow lines, production manifolds, process headers and other lines transporting gas and liquid in two-phase flow should be sized primarily on the basis of flow velocity when sand is present. Flow velocity should be kept at least below fluid erosional velocity.

1. The flow velocity above which erosional damage may exceed an acceptable limit can be determined by the following empirical equation:

$$V_e = \frac{K}{\sqrt[n]{Z}} \tag{28}$$

where

V_e = fluid erosional velocity,
Z = density ratio of sand to fluid mixture,
K = empirical constant,
n = empirical root.

2. The values of the empirical constants K and n are given in the following table. The fittings are in descending order of severity. The worst case for each design should be used for sizing flow lines and setting production velocity. If sand production increases with the life of the well, then production rate should be lowered accordingly.

TABLE IX. EXAMPLE FOR A GIVEN FITTING MATERIAL COMPOSITION

	Sand Only		CO_2 Service With Sand		H_2S Service With Sand	
Fitting Type	**K**	**n**	**K**	**n**	**K**	**n**
Elbow, Short Radius	TBD	TBD	TBD	TBD	TBD	TBD
Elbow, Long Radius	TBD	TBD	TBD	TBD	TBD	TBD
Target Tees	TBD	TBD	TBD	TBD	TBD	TBD
Lines Downstream of Chokes	TBD	TBD	TBD	TBD	TBD	TBD

B. Future Research Needs

The guideline presented above gives the general form of the equation for erosional velocity. The empirical constants, however, have yet to be determined. The next phase of this research should be to develop values for these constants based on actual field operational experience and a computational fluid dynamics analysis to determine the effect of different fittings on erosional damage in the absence of corrosion as a function of flow velocity, flow regime, and sand production.

A subsequent third phase of the research should be to include the effect of corrosion and to experimentally verify the values of these constants for sand producing service and to perform a systematic test program to develop values for the corrosive environments of CO_2 and H_2S service with sand production.

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