MODELLING NITRIFICATION IN PLANT SCALE ACTIVATED SLUDGE

Peter J. Bliss and David Barnes

School of Civil Engineering, University of New South Wales, P.O. Box 1, Kensington, NSW 2033, Australia

ABSTRACT

Nitrification performance data from an activated sludge plant was simulated using a simple model based on growth requirements of nitrifiers only and a complete-mix flow pattern. The model best accounted for the plant data under high peak flows and ammonia loadings and confirmed the observation that high peak loadings cannot be fully compensated by increasing the safety factor applied to theoretical mean cell residence time. Difficulties in control of dissolved oxygen and MLSS concentration under high transient loads also adversely affected nitrification. Overall ammonia oxidation rates of the sludge, under the imposed nitrogen and carbon loadings, varied from 2.5 down to 1.3 mg.g\(^{-1}\).hr\(^{-1}\) at mean cell residence times of 3 to 4 and 9 to 10 days respectively. Adaptation of the model to predictive use requires modification to include overall biomass growth, oxygen relationships and sludge recycle arrangement.

KEYWORDS

Activated Sludge, Modelling, Nitrification, Plant scale, Transient loads.

INTRODUCTION

Nitrification of wastewaters has been extensively studied during the last three decades, both as a means of controlling ammonia toxicity and oxygen demand in surface waters and as the first step in the removal of nitrogen by biological denitrification. Since the major study of Downing et. al. (1964) led the way to an understanding of nitrification in the activated sludge process, a number of comprehensive reviews of nitrification theory have appeared (Painter, 1970, 1977; Sharma and Ahlert, 1977; USEPA, 1975; Barnes and Bliss, 1982). These reviews have presented the main requirements for nitrification as a microbial oxidation, carried out by a limited range of micro-organism species for which growth kinetics and environmental requirements have been established under a wide range of conditions. Consequently design principles for nitrification are well understood and design procedures have been widely publicised (USEPA, 1975; Barnes and Bliss, 1982), especially for the case of domestic, non-inhibitory, wastewater.

The greatest difficulties confronting the designer are in the selection of appropriate values for the kinetic co-efficients to account for any inhibitory characteristics of the wastewater to be treated; in provisions for process control, especially related to environmental factors; and in accommodation of the effects of transient flows and loadings (Poduska and Andrews, 1975; Gujer and Erni, 1978). Faced with the need to incorporate nitrification into amplifications of several of its inland water pollution control plants, the Metropolitan Water, Sewerage and Drainage Board in Sydney, Australia carried out a programme of field scale testing in order to elucidate some of these design factors. Interpretation of data obtained in this study (Bliss and Barnes, 1979, 1981; Bliss et. al., 1982, 1983) has been facilitated by the
use of a simple computer model of the process, based on the assumptions of complete mix flow pattern; that the process can be represented by factors affecting nitrifier growth only; and consequently that heterotrophic growth rate was held sensibly constant by the control of system mean cell residence time.

**DESIGN FOR NITRIFICATION**

**Theory**

The main factors governing nitrification in activated sludge are those affecting the rate of growth of the autotrophic nitrifying bacteria relative to those of the heterotrophic species which comprise the major part of the biomass. Thus, full nitrification requires the nitrifier growth rate, \( \mu_A / X_A \), to be at least equal to, and in practice potentially somewhat greater than, the sludge growth rate, \( \mu_H / X_H \). In terms of system mean cell residence time, \( \theta_c \), this condition is

\[
\frac{\mu_H}{X_H} > \frac{1}{\theta_c}
\]

(1)

Nitrification requirements are usually considered with respect to growth of the first stage ammonia oxidizers since under most practical conditions the second stage oxidation from nitrite to nitrate is not rate limiting (Knowles et. al. 1965; USEPA, 1975). Although there have been suggestions that this is sometimes not so, (Alleman 1984), nitrite accumulation is rare in practice.

Nitrosomonas growth rate is affected by concentrations of the substrates (ammonia nitrogen, carbon dioxide and oxygen) according to Monod kinetics; environmental factors - temperature according to the Arrhenius equation, and pH; and the type and concentration of inhibitory chemicals in the waste. Of these factors carbon dioxide is normally present in excess, no general formulation is feasible for inhibitory effects and pH effects are small in acclimated sludges and biofilms provided it does not fall much below 6.5 (Haug and McCarty, 1972). The remaining factors are expressed in terms of the design equation:

\[
\frac{\hat{\mu}_A}{X_A} = \frac{X_A}{S_{NH}} \cdot \frac{S_{NH}}{0.4e^{0.118(T-15)} + S_{NH}} \cdot \frac{S_O}{K_O + S_O} \cdot e^{0.095(T-15)}
\]

(2)

For modelling nitrification performance this can be converted to the rate of ammonia oxidation, \( \rho_A / X_A \), using the fact that \( \rho_A = \mu_A / Y_A \) (where for simplicity \( Y_A \) is taken as the net yield co-efficient). To apply this equation to mixed nitrifying activated sludges requires an estimate of the fraction of nitrifiers in the biomass from the relative amounts of carbonaceous substrate utilized and ammonia oxidized (USEPA, 1975). Nitrifier fraction can then be calculated from the nitrifier yield co-efficient \( Y_A \) and net daily mass of TKN removed (the difference between total TKN removed \( \Delta TKN_T \) and that removed in the daily excess biomass, as measured by MLVSS, \( \Delta X_v_T \), on the assumption that biomass has twelve per cent nitrogen content).

The resulting expression is:

\[
f_A = \frac{Y_A(\Delta TKN)_T - 0.12(\Delta X_v)_T}{(\Delta X_v)_T}
\]

(3)

Unit ammonia oxidation rate in the mixed sludge culture, called \( \rho_{A,H} / X_{A,H} \) to emphasize definition in terms of total biomass solids, is then given by

\[
\rho_{A,H} / X_{A,H} = f_A \cdot \frac{\rho_A}{X_A}
\]

(4)
Modelling nitrification

Total mass oxidation rate of ammonia per unit time ($\Delta t$) is then

$$\frac{\Delta S_{NH_2}}{\Delta t} = \frac{\rho_{A,H}}{X_{A,H}} \cdot V \cdot X_{A,H}$$  \hspace{1cm} (5)

In most cases the data and assumptions required in order to make separate estimates of the unit ammonia oxidation rate of nitrifiers and the nitrifier fraction in the biomass are not known with any certainty. Only their combined effect can readily be measured as the overall ammonia oxidation rate of the sludge ($\rho_{A,H}/X_{A,H}$). In the present modelling study, one objective was to determine the best fit value of this sludge oxidation rate, after allowance was made for temperature, oxygen, ammonia concentration and hydraulic effects. Expected values of ($\rho_{A,H}/X_{A,H}$), for conventional activated sludges (USEPA, 1975) are 1-3 (mg N oxidized) (g MLSS)$^{-1}$ . hr$^{-1}$.

In practice, allowance must also be made for the effects of transient loads, especially the ratio of peak to average ammonia mass load, $P_N$, and for problems which arise in process control as a consequence of general flow and pollutant load variations. Although the latter effects do not appear in design formulations, they must be taken into account in plant design and operation if plant performance specifications are to be met. In design, allowance is usually made for the effect of $P_N$ by applying a safety factor, $S.F.$, which varies with the flow pattern in the aeration tanks. For plug flow systems the required safety factor is relatively small but not mathematically simple. In complete-mix systems, however, it has been suggested (Gujer and Erni, 1978; EPA, 1975) that in order to satisfy a given effluent ammonia level at peak ammonia load, the design system steady state $\theta_c$ should be calculated from:

$$\theta_c = SF \cdot \theta_{c\text{ minimum}}$$  \hspace{1cm} (6)

where $SF = P_N$.

Problems in Design

The main problems in applying the above method involve the selection of appropriate values of the maximum nitrifier growth rate $\mu_A$, and the saturation constants especially that for oxygen, $K_O$. Many values of $\mu_A$ have been reported, under various conditions and temperatures - a representative value in the range 0.4-0.5 d$^{-1}$ at 15°C has been commonly adopted (USEPA, 1975), and corrected for temperature as in equation (2). The value of $K_O$ is poorly established, varying from 0.2 to 2 mg/L, and no relationship is available for its variation with temperature.

In addition, the extent to which a safety factor can accommodate peak ammonia loads is not well established in practice.

Field Studies into Nitrification

To obtain data on $\mu_A$, as well as dissolved oxygen and transient load effects for Sydney wastewater, field scale tests were carried out at Castle Hill Water Pollution Control Plant (Bliss and Barnes, 1981; Bliss et. al. 1982, 1983).

Main results of this study, as previously reported were: the winter critical $\theta_c$ was 4-5 days at 15°C, that higher dissolved oxygen concentration up to 4 mg/L enhanced nitrification, and that the system had limited ability to accommodate peak ammonia load beyond a value of 2-5 times the average.

There were many problems in the interpretation of the field data, however, because of process control difficulties, which resulted from the effects of transient loads. In order to better accommodate some of these process variations a more extensive analysis of the data using a simple process model has recently been carried out.
NITRIFICATION MODEL

General Model Requirements

Model construction, validation and use requires consideration of: Process Kinetics; Hydraulic Flow Regime; and Data Requirements on Process Loadings, Environmental Factors and Plant Operational Control Factors.

As a significant simplification, the model was constructed in terms of the growth requirements of the nitrifier population only. The complexities of detailed modelling of the response of the heterotrophic biomass were thus avoided, and it was assumed that sludge mass growth was effectively accounted for in the sludge wastage procedures by which the system was controlled. Ammonia oxidation was expressed simply in terms of the biomass as a whole, by equation (5), corrected for oxygen, ammonia and temperature according to equation (2). The major process variable then became $\frac{P_{A,H}}{X_{A,H}}$, and one objective of the model study was to determine by trial and error the value of $\frac{P_{A,H}}{X_{A,H}}$ for which the best fit could be obtained to the data, in each series of tests, and to assess the effects of the value of $K_O$ on the model fit.

Hydraulic flow regime in full scale systems rarely complies exactly with either complete-mix or plug flow models, but in the absence of elaborate precautions to prevent back mixing most commonly approaches the complete mix model. For the Castle Hill aeration tanks the calculated axial dispersion number was of the order of one under uniform flow conditions, indicating a closer approximation to complete-mix than plug flow. For the purpose of the model complete-mix was therefore assumed.

One of the major problems in plant scale studies is the cost of sampling and analysis - inevitably some compromise is necessary. In the present study intensive monitoring was confined to critical phases of the study, when data were obtained at one or two hourly intervals. At other times only skeletal data were obtained once-to-twice daily, to assist in plant control. Modelling efforts were confined to the periods of intensive monitoring, particularly to the study on transient load effects. Data were obtained on both the mixed liquor supernatant and the clarifier effluent; for the modelling exercise, data for the reactor supernatant were used.

Model Details

For a complete-mix model, effluent and reactor composition are identical, and the mass balance for ammonia nitrogen is:

\[
\text{INFLUENT} \quad \text{NH}_3 \text{ MASS} + \text{RECYCLE} \quad \text{NH}_3 \text{ MASS} = \text{MASS} \quad \text{NH}_3 \quad \text{OXIDIZED} + \text{CHANGE} \quad \text{IN MASS} + \text{EFFLUENT} \quad \text{NH}_3 \quad \text{STORED} \quad \text{NH}_3 \text{ MASS} \quad \text{NH}_3 \text{ MASS}
\]

The model, written in BASIC language carries out the above mass balance iteratively, on a one hour time step (consistent with data) using as input field data on influent and recycle flow rates, influent ammonia nitrogen concentration, reactor MLSS and DO concentrations, daily temperature and reactor volume, and calculates the effluent ammonia nitrogen concentration. Ammonia oxidation rate, $\frac{P_{A,H}}{X_{A,H}}$ and DO saturation constant, $K_O$ are also inputs and may be adjusted to obtain the best fit to the data.

Mixed liquor concentration was expressed in terms of suspended solids as measured rather than oxygen equivalent for which no measurements were taken. Measured MLSS value was also preferred to a calculated value based on recycle flow rate and concentration (for which far fewer determinations were made). Recycle ammonia concentration was generally taken as the average for the mixed liquor (effluent) over the preceding hour, except for the first hour of the day, when it was set as the value at the beginning of that hour.

Model output was in the form of an hour-by-hour tabulation and a graphical comparison of predicted and actual effluent ammonia nitrogen concentrations, weighted averages of both, and a simple statistical summary including co-efficient of variation. This facilitated comparison of results for different trial values of $\frac{P_{A,H}}{X_{A,H}}$ and $K_O$, on the basis of both peak and flow weighted average ammonia nitrogen concentrations and general fit as reflected in the co-efficient of variation.
The transient load study was set up to test the ability of safety factor (S.F.) to accommodate peak-to-average ammonia loading ($P_N$). The objective was to carry out three series of tests each at a different value of $S_C$, corresponding to safety factors of 1.2 to 1.5, 2 to 3 and 4 to 5. In each series, after several weeks (that is greater than $2B_c$) for equilibrium operation to be attained, plant performance was closely monitored for at least two days at three ratios of Peak-to-Average Flow, namely (a) one, (b) two and (c) three.

Because of diurnal variations in ammonia concentration, the value of $P_N$, even under equalized flow conditions was about 1.5. However, peak flows and ammonia concentrations were not quite coincident. Peak flow conditions were achieved simply by removing the flow diversion plates; such high peak flows were attainable because of the relatively small size of the treatment plant, (population 8000) and its consequently high diurnal peak flow.

In the event, difficulties in plant control led to a higher than intended value of $S_C$ (9-10 days) for the Series 2 tests, and hence conditions for Series 2 and Series 3 were quite similar. Table 1 summarises the main process and performance parameters for the three Series of tests.

### Table 1 Summary of Operating Conditions During Transient Load Tests

<table>
<thead>
<tr>
<th></th>
<th>Series 1</th>
<th>Series 2</th>
<th>Series 3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Operating $S_C$, d</td>
<td>3-4</td>
<td>9</td>
<td>9-12</td>
</tr>
<tr>
<td>Estimated Safety Factor (FS)</td>
<td>1.5-2</td>
<td>4</td>
<td>4-5</td>
</tr>
<tr>
<td>Operating Parameters</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>MLSS, mg/L$^{-1}$</td>
<td>2500-3000</td>
<td>3500-4500</td>
<td>5500-3000</td>
</tr>
<tr>
<td>DO, mg/L$^{-1}$</td>
<td>1.5-3.5</td>
<td>1.5-4.5</td>
<td>1-4.5</td>
</tr>
<tr>
<td>pH</td>
<td>6.2-6.6</td>
<td>6.2-6.6</td>
<td>6.3-6.8</td>
</tr>
<tr>
<td>Temperature, °C</td>
<td>21-23</td>
<td>23-24</td>
<td>23-24</td>
</tr>
<tr>
<td>Loading Data(1)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>- Peak/Ave. Flow</td>
<td>a 1.2</td>
<td>b 1.4</td>
<td>c 1.7</td>
</tr>
<tr>
<td>- Peak/Ave. Conc</td>
<td>a 1.4</td>
<td>b 1.4</td>
<td>c 1.5</td>
</tr>
<tr>
<td>- Peak/Ave. Load, $P_N$</td>
<td>a 1.7</td>
<td>b 1.6</td>
<td>c 2.3</td>
</tr>
<tr>
<td>Mixed Liquor Supernatant(1)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Ave, NH$_3$-N Conc, mg/L$^{-1}$</td>
<td>5 7 12</td>
<td>5 9 9</td>
<td>6 10 14</td>
</tr>
<tr>
<td>Peak NH$_3$-N Conc, mg/L$^{-1}$</td>
<td>10 12 25</td>
<td>9 17 17</td>
<td>10 16 25</td>
</tr>
</tbody>
</table>

Notes: 1. Loading and Performance Data are averages over two days operating results.

2. columns a, b, c refer to nominal peak-to-average flow ratios of 1, 2 and 3 respectively.

### RESULTS OF MODEL SIMULATION

Results of model simulations of the transient load tests are presented, with input data, for series 3 in Fig. 1-4. Results shown are for the most likely value of $P_{A,H}/X_{A,H}$ common to all tests in the series. This value is generally within 10-15 per cent of the best fit value. In each case the one value of $K_0 = 1$ mg/L was adopted for the purpose of illustration.

There is considerable variation in the fit of the individual model predictions to the measured values. Nevertheless, within this, as in the other series of tests, several trends are clear:

a. Model average ammonia nitrogen concentration is sensitive to the value of ($P_{A,H}/X_{A,H}$) assumed, as expected; however, the shape of the model output is similar for widely differing values of ($P_{A,H}/X_{A,H}$).
Fig. 1. Plant influent data for 11th March 1981, with comparison between actual and model predicted effluent ammonia concentration.

Fig. 2. Plant influent data for 25th March 1981, with comparison between actual and model predicted effluent ammonia concentration.

Fig. 3. Plant influent data for 31st March 1981, with comparison between actual and model predicted effluent ammonia concentration.

Fig. 4. Plant influent data for 1st April 1981, with comparison between actual and model predicted effluent ammonia concentration.

Legend: Unit ammonia oxidation rate used in the model simulation is shown bracketted in the top right hand corner of Fig. 1-4.
b. Best overall fit value of \((\rho_{A,H}/X_{A,H})\) for test series 3 was 1.6 mg\(\cdot\)g\(^{-1}\)\(\cdot\)hr\(^{-1}\) (For Series 1 and 2 the values were 2.5 and 1.3, respectively).

c. Model average effluent ammonia nitrogen concentration increases with increasing \(P_N\) for all series of tests, in accordance with expectations.

d. There is a general improvement in the model fit to the data as \(P_N\) values increase.

e. There is generally a better fit between model and data at periods of highest diurnal flow, and the fit at low flow periods is generally poor, with measured ammonia concentrations often significantly exceeding model predictions.

f. In one case (24/03/81) complete but temporary inhibition of nitrification occurred - plant performance was affected early on 25/03/81 (Fig. 2) but had apparently recovered by mid-morning.

g. Not shown on the plots of the results, but evident from the range of operating data shown in Table 1, is the fact that peak ammonia loading effects were frequently compounded by lower MLSS concentration (recycle problems) and lower D.O. concentrations (high oxygen demand and aeration system control problems).

h. Performance of the plant at the high value of \(\Theta_C\) in series 2 and 3 did not show the expected additional capacity to accommodate peak load effects compared with series 1 (Table 1).

i. As may be predicted from equation (2), other simulation runs using \(K_0\) values of 0.5 and 1.5 mg/L showed that in each case the effect on the model prediction was the same as that produced by a decrease or increase, respectively, of about 10 per cent, in the value of \(\rho_{A,H}/X_{A,H}\).

**DISCUSSION**

Results of the modelling exercise in this study can conveniently be reviewed with respect to the three areas of modelling raised above - kinetics, flow regime and analytical data and control problems.

**Mixing Regime**

Evidence for the practical acceptability of the complete-mix model in the present case may be adduced from several of the points set out in the results. Most importantly the fact that the model fit is best at highest peak loads (points c, d, and e) is in accord with studies which have shown that increased waste and recycle flows and higher aeration rates lead to a closer approximation to complete mixing. Further evidence is provided by the mode of increase in ammonia nitrogen on 24/03/81 (point f), which follows closely the pattern predicted for a continuous input into a complete-mix reactor, in the absence of any nitrification reaction. That mixing is not perfect, however, shows in the fact that the peak in the effluent ammonia concentration lags the influent peak by about one hour and by the significant variations in process variables throughout the aeration tank, as discussed below.

**Reaction Kinetics**

Although the available data did not allow rigorous separation of the effects of all the individual kinetic co-efficients, the ability of the model to account for the general trend in the variations of process variables supports the Monod kinetic assumption in terms of both ammonia nitrogen and oxygen. No conclusion as to the correct relative values of \((\rho_{A,H}/X_{A,H})\) and \(K_0\) was possible, however, because of the mutually inter-changeable effects of these factors on nitrification performance. However the commonly adopted value of \(K_0=1.0\) mg/L\(^{-1}\) appears to be reasonable. The marked decline in the unit nitrification rate from 2.5 mg g\(^{-1}\)hr\(^{-1}\) in series 1 to 1.3-1.6 mg g\(^{-1}\)hr\(^{-1}\) in series 2 and 3 is expected, because nitrifier fraction in the biomass is dependent on the total amount of ammonia nitrogen available to be oxidized - hence the daily mass of nitrifiers produced is constant, for a constant total daily nitrogen load. This means that as total MLSS held in the system is increased with increasing \(\Theta_C\) the nitrifier fraction decreases. Thus for an increase in MLSS from about 2700 to 4000 mg/L between series 1 and 2 tests, the effective value of \((\rho_{A,H}/X_{A,H})\) would be expected to decrease
Analytical and Process Control Factors

The practical limitations of sampling and analysis, in both time and space, impose limitations on the time step chosen for modelling. The need for, and spatial distribution of, sampling points decreases as the system approaches the ideal complete-mix model. Conversely it increases as the system departs from the complete-mix assumption. In the present study early investigations established that conditions in the aeration tanks with respect to most factors were reasonably uniform - for example DO sampling at three depths at each of six points along the aeration tanks gave surprisingly uniform results under equalized flow conditions. Nevertheless there was considerable variation in DO at and between each of the two DO sampling points, in the transient load study, (the mid-point and the outlet end of the tank), indicating the practical difficulty of maintaining a constant DO under transient load conditions. This, together with the large variation in MLSS concentration which occurred, in fact, provided the imperative to model the system in order to reflect actual rather than gross average conditions in the plant.

It should be noted further that the data for DO were only measured at two hour intervals - intermediate values were interpolated to enable modelling in the one hour time step, consistent with the other data. This may have led to an over estimation of the DO concentration during the most critical two hour period of the morning peak load condition, when DO depression was noticeable in almost every set of results.

It is possible, therefore, that more frequent sampling at a larger number of points in the aeration tank would have produced a better model fit and justified use of a more realistic hydraulic regime model. To an engineering approximation, however, the present model provides an acceptable account of the data.

Summary

The Monod based kinetic model provides a reasonable basis for the study of plant scale nitrification performance when coupled with a suitable hydraulic flow regime model. In the present study the complete-mix model was found to be increasingly satisfactory as flow and recycle rates and, with them, aeration rates and hence mixing intensity, increased. The commonly recommended value of 1 mg/L-1 for the saturation constant for oxygen appeared to reasonably account for the effects of oxygen on nitrification as given by equation (2) but a variation of ±50 per cent in£0 could be accommodated by a relatively small change of 10 per cent in the unit ammonia oxidation rate.

On the basis of the plant scale studies the effects of transient loads on nitrification efficiency could not be fully compensated by increasing the safety factor at high peak flow and ammonia nitrogen loadings. This results as much from other problems of operational control as from inherent biological effects. Control of recycle and oxygen supply rates are both critical, and most difficult to achieve, at peak waste flow rate. Dissolved oxygen control is especially difficult because at peak flows not only the ammonia but also the organic loads are at or approaching their peak. Under these conditions not only is the ambient DO likely to fall below the target concentration but the high metabolic rate induced in the biomass is likely to lead to a reduction in the proportion of the nitrifiers present which are active, as a result of limitations on oxygen diffusion into the biomass. Furthermore, in continuous plant operation under high peak loadings, the effects would be greater than in the present studies where the peak load was imposed for a few days only. Daily bleed-through of ammonia would eventually lead to a lower daily cell yield of nitrifiers, hence to a lower nitrifier fraction in the biomass and a lower unit ammonia oxidation rate. This effect is evident from the plant results, when the second day of each transient load study usually gave a lower best fit value of (PA/H/XA/H) than the first day.

Thus the simplified nitrification model is a useful tool for interpreting plant operating data, pointing up some difficulties in current design recommendations for handling high diurnal peak flows and ammonia loadings. It employs data on the process operating factors, biomass concentration and dissolved oxygen, and requires independent control of £0. To become fully predictive, however, the model requires the addition of means for calculating both the growth of the total biomass, and effective oxygen profiles (both demand and supply). Adequate representation of the clarifier thickening behaviour and sludge recycle system are also necessary in order to maintain control of the biomass concentration in the reactor.
CONCLUSION

A simple mathematical model based on the Monod kinetic equation in terms of ammonia nitrogen and dissolved oxygen, superimposed on a complete-mix flow regime, was used to study the effects of transient loads on plant scale nitrification. In spite of some instances where the fit was poor the model generally gave an acceptable account of the effects during the critical periods of peak loading. Limitations on the model were identified as associated with departures from complete mixing, and neglect of the dynamic growth of the heterotrophic biomass, both of which aggravated variations in environmental conditions, especially D.O. in the reactor, and associated sampling, analysis and control problems.

The study showed that peak ammonia nitrogen load effects could not be fully compensated by an increase in the safety factor applied to $\Theta_C$. This was also associated with difficulties in control of plant recycle and D.O. under peak loadings. These findings indicate that where peak ammonia loadings are likely to exceed 2 to 3 times the average, some form of flow or load equalization is necessary if high effluent peak ammonia concentrations are to be avoided.

Under loadings of ammonia nitrogen which were typical of domestic sewage, the unit ammonia oxidation rate of the activated sludge varied from 2.5 mg g$^{-1}$hr$^{-1}$ at a $\Theta_C$ of 3-4 days to 1.3 to 1.6 mg g$^{-1}$hr$^{-1}$ at a $\Theta_C$ of about 9 days, reflecting the relatively constant mass yield of nitrifiers and the reduced proportion of nitrifiers in the biomass at larger values of $\Theta_C$. Although the simple model, based on consideration of nitrifier growth only, is useful for the interpretation of plant operating data, for prediction of plant performance it must also account for overall biomass growth, oxygen demand and biomass recycle. Hence it must also reflect the level of plant operational control which is to be provided.

REFERENCES


Poduska, R.A. and Andrews, J.F., (1975), Dynamics of Nitrification in the Activated Sludge
NOTATION

The following symbols and units are used throughout this paper.

\[ \mu_{A,H} = \text{net sludge growth rate (g m}^{-3}\text{d}^{-1}) \]
\[ \mu_A = \text{net growth rate of nitrifying bacteria per unit mass (g m}^{-3}\text{d}^{-1}) \]
\[ \hat{\mu}_A = \text{maximum growth rate of nitrifying bacteria when substrate is not limiting (g m}^{-3}\text{d}^{-1}) \]
\[ \theta_c = \text{mean cell residence time (d).} \]
\[ S_{NH} = \text{NH}_4^+ - \text{N concentration (mg L}^{-1}) \]
\[ S_O = \text{dissolved oxygen concentration (mg L}^{-1}) \]
\[ f_A = \text{fraction of nitrifiers in the biomass} \]
\[ K_{NH} = \text{saturation constant for NH}_4^+ - \text{N (mg L}^{-1}) \]
\[ K_O = \text{oxygen saturation constant for nitrifiers (mg L}^{-1}) \]
\[ P_N = \frac{\text{Peak NH}_4^+ - \text{N flux}}{\text{Average NH}_4^+ - \text{N flux}} \]
\[ S_F = \text{Safety factor} \]
\[ T = \text{temperature (°C)} \]
\[ TKN = \text{Total Kjeldahl Nitrogen} \]
\[ V = \text{aeration tank volume (m}^3) \]
\[ X_A = \text{nitrifier concentration (mg L}^{-1}) \]
\[ X_{A,H} = \text{biomass concentration expressed as mixed liquor suspended solids (mg L}^{-1}) \]
\[ X_V = \text{mixed liquor volatile suspended solids concentration (mg L}^{-1}) \]
\[ Y_A = \text{net yield coefficient of nitrifiers (Nitrosomonas) (gg}^{-1}) \]
\[ Y_{A,H} = \text{net yield coefficient of activated sludge (gg}^{-1}) \]
\[ [\cdot]_T = \text{total mass (kg).} \]
\[ \rho_A = \text{ammonia oxidation rate of nitrifiers (g m}^{-3}\text{d}^{-1}) \]
\[ \hat{\rho}_A = \text{maximum oxidation rate of nitrifiers (g m}^{-3}\text{d}^{-1}) \]
\[ \rho_{A,H} = \text{ammonia oxidation rate of activated sludge (g m}^{-3}\text{d}^{-1}) \]